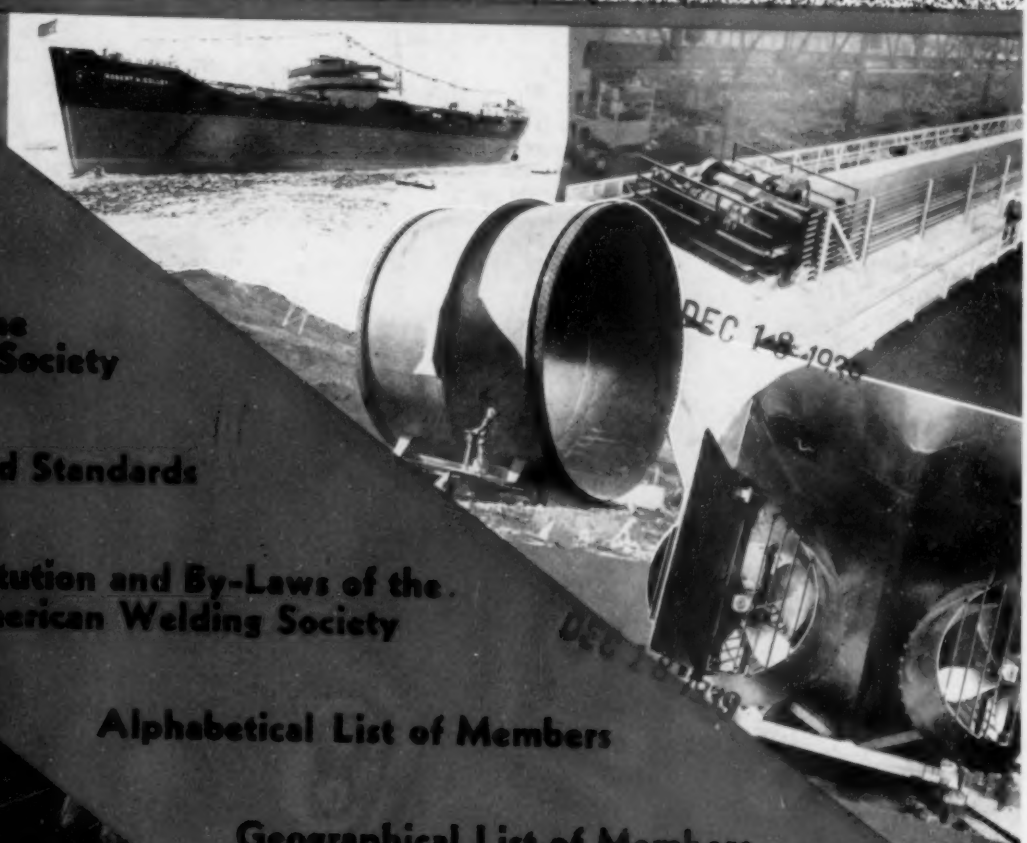
The Welding Journal

Year Book NUMBER



Activities of the American Welding Society

Codes and Standards

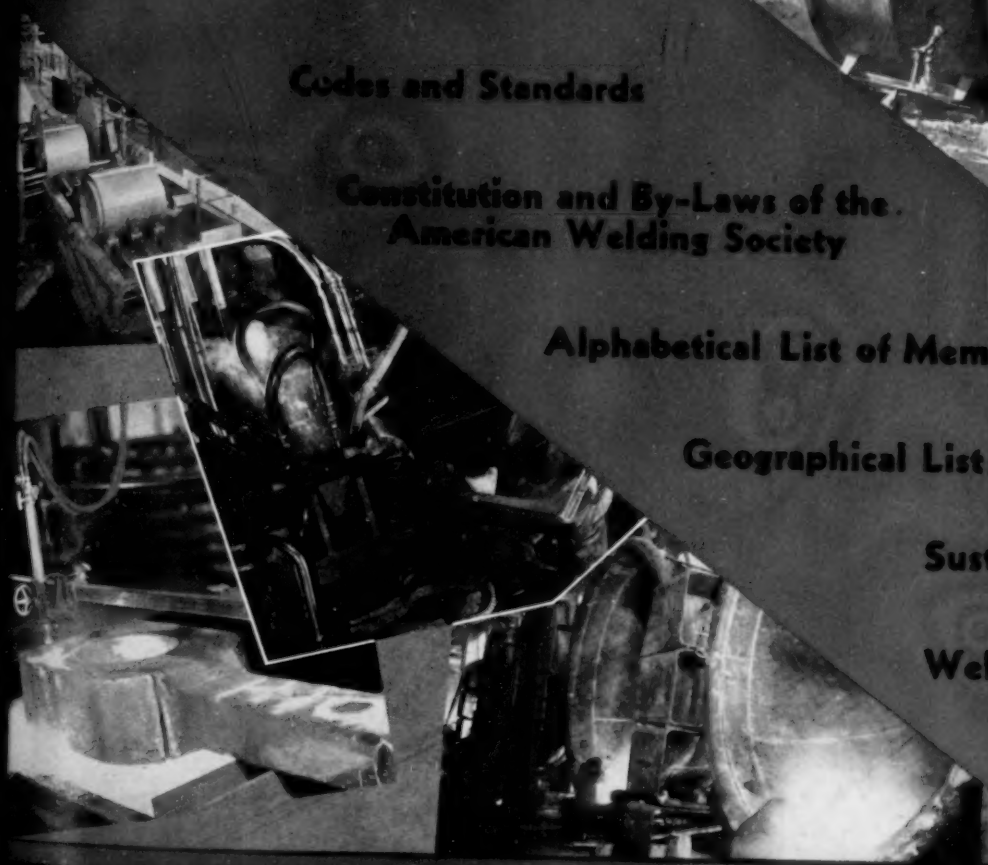
Constitution and By-Laws of the American Welding Society

Alphabetical List of Members

Geographical List of Members

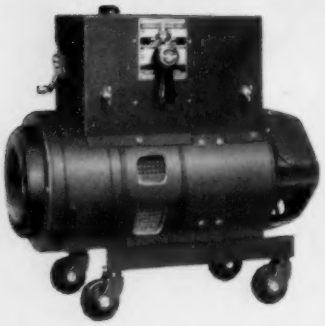
Sustaining Companies

Welding Research Committee

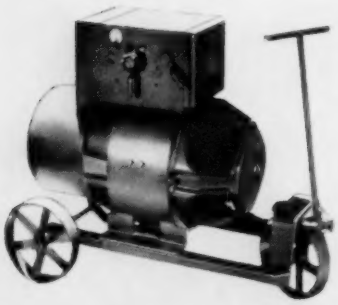


DECEMBER, 1939

The Journal of the American Welding Society



150-amp d-c arc welder



Single-operator d-c arc welders, 200- to 600-amp sizes; Multiple-operator d-c arc welders also available, sizes 500-amp and up

THE RIGHT ARC WELDER PAYS —*Most*

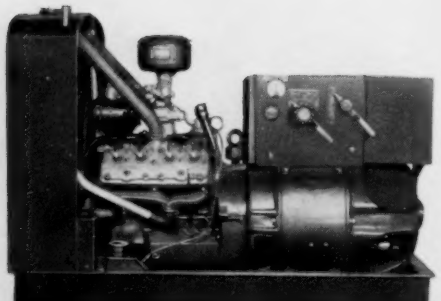
By changing from d-c arc welders to G-E alternating-current arc welders, a prominent crane manufacturer obtained 15 per cent faster speed, superior welds, and lower welding costs.

By changing from a 300-amp, 25-volt arc welder to a G-E 400-amp, 40-volt set, a Cicero, Illinois steel foundry reduced welding costs 50 per cent and obtained better results.

By changing to an atomic-hydrogen arc welder, a prominent die-casting company saved enough on the first two die-repair jobs to more than pay for the welder.

Scores of similar examples could be cited where a change to the right G-E arc welder has brought astonishing improvements.

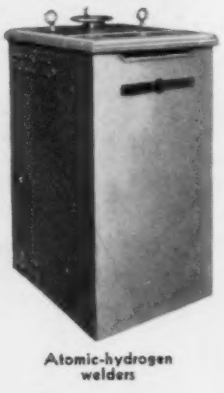
Why? Because G-E arc-welding distributors and specialists handle the widest variety of arc-welding equipment on the market. Their recommendations involve no compromise. Perhaps they can help you get better results in your own welding work. Why not call the local G-E distributor or G-E sales office and get started today? General Electric, Schenectady, N. Y.



Engine-driven arc welders, 200-, 300-, 400-, and 600-amp sizes

A-c arc welders, typical of 100- and 200-amp sizes

A-c arc welders, typical of 300-, 500-, 750-, and 1000-amp sizes



Atomic-hydrogen welders

Automatic arc welders; thyratron control for better performance

THE MOST COMPLETE LINE OF ARC WELDING EQUIPMENT IN THE WORLD

Complete line of welding accessories including the new G-E ventilated helmet

Glyptal No. 1294 for preventing adhesion of weld spatter

Heavily and lightly coated electrodes for manual or automatic operation

SEE THE G-E "HOUSE OF MAGIC" AT BOTH FAIRS

GENERAL ELECTRIC

140-94

The American Welding Society Journal

W. SPRARAGEN, Editor

Published for the Advancement of the Science and Art of Welding

OFFICERS—1939–1940
President, G. T. HORTON
First Vice-President, T. C. SMITH
Second Vice-Pres., L. S. MOISSEIFF
Treasurer, C. A. McCUNE
Secretary, M. M. KELLY
Technical Secretary, W. SPRARAGEN

VOLUME 18 NUMBER 12

DECEMBER 1939

Divisional Vice-Presidents
W. F. HESS, New York & New England
L. C. BIBBER, Middle Eastern Division
K. L. HANSEN, Middle Western Division
L. W. DELHI, Pacific Coast
A. J. MOSES, Southern

CONTENTS

The American Welding Society ... 773
Advantages to Be Gained Through Membership in the Society ... 773
Scope of Society Activities ... 773
Research ... 774
Publications ... 774
National and Section Meetings ... 774
Welding Handbook ... 774
Codes and Specifications ... 775
Standards ... 775
Membership Certificate ... 775
Classes of Membership ... 775
Membership Report ... 776
Report Activities Meetings and Papers Committee ... 777
Financial Report ... 778
Technical and Special Committees of the American Welding Society ... 780
Standing Committees ... 785
Special Committees ... 786
Award of Medals ... 787
Rules to Govern Organization, Functions and Operations of Committees ... 788
Officers and Standing Committees ... 790
Technical Committees Personnel ... 791
Special Committees Personnel ... 793
Representatives of A.W.S. on Other Societies ... 793
Map of Sections and Districts ... 794
Catalogue of Membership
Alphabetical List ... 795
Geographical List ... 835
Index to Advertisers ... 833
Section Chairmen, Secretaries and Regular Meeting Dates ... 834
Sustaining Companies ... 843
Constitution and By-Laws ... 860
Rulings Adopted by Board of Directors ... 870

WELDING RESEARCH SUPPLEMENT

The Welding Research Committee ... 441-s
Brief Progress Report for the Year 1939 ... 442-s
Literature Division ... 445-s
Fundamental Research Division ... 446-s
List of Researchers ... 447-s
Industrial Research Division ... 450-s

A Christmas Message

In these war-torn times the word WELD seems of special significance; and among the various definitions these two are outstanding: "to join indissolubly;" and "to make one."

Now, surely, the whole world needs a welding. One that will fuse all refractory elements into a true unity, held together forever by the eternal potency of the real welders—Faith, Loyalty and Honor.

These are the invincible forces that can make powerless the hate, cruelty, and greed that rend humanity today; and that will sometime bring back to us a real Christmas Season with lasting peace on earth and good will to all men.

Let us wish this world a Merry Christmas, in spite of all its turmoil; because these unhappy things must pass away.

"Careless seems the Great Avenger; history's pages but record
One death-grapple in the darkness 'twixt old systems and the Word.
Truth forever on the scaffold. Wrong forever on the throne,—
Yet that scaffold sways the future, and, behind the dim unknown,
Standeth God, within the shadow, keeping watch above his own."

—HAZEL HEATH HORTON

ISSUED MONTHLY

Copyright, 1939, by the American Welding Society

Subscription $5.00 per year in United States and possessions; $5.50 in Canada and Mexico; Foreign Countries $7.00. Single copies, non-members $.75; Special issues October and December $1.00; members 50 cents.

Entered as second-class matter January 15, 1932, at the post-office at Easton, Pa., under the Act of March 3, 1879.

PUBLICATION OFFICE
20th & Northampton Streets, Easton, Pa.

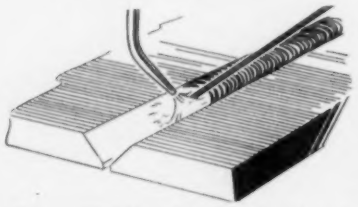
EDITORIAL AND GENERAL OFFICES
33 West 39th St., New York, N. Y.

The Society is not responsible for any statement made or opinion expressed in its publications. Permission is given to reprint any article after its date of publication provided proper credit is given.

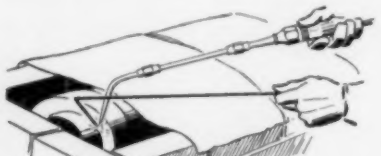
Features of the New Oxweld Welding Heads

Fewer Heads Do More Work because you can adjust for a variety of flame sizes over a much wider range—and get different types of flame for different kinds of work.

Streamlined Injector produces a flame that is not susceptible to flashback even over a broad range of adjustment, and that is far less sensitive to pressure fluctuations.



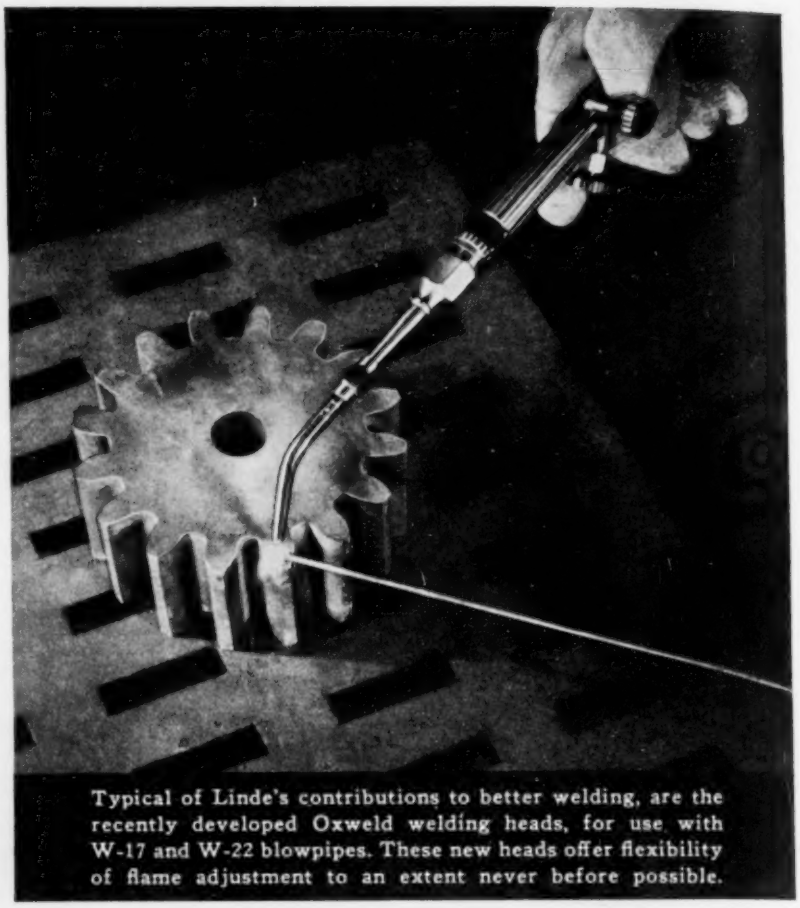
Better Welding Flame—A pointed, more penetrating flame that gets heat down faster to the base of the vee—yet still so soft there is no tendency to "blow" the molten metal.



Greater Operator Comfort on preheated work due to center coupling which permits use of extensions.

Lower Replacement Costs—Tips last longer because they contain more copper to dissipate heat, and because of chromium-plating. All parts replaceable at low cost.

Oxweld
TRADE MARK
WELDING AND CUTTING APPARATUS



Typical of Linde's contributions to better welding, are the recently developed Oxweld welding heads, for use with W-17 and W-22 blowpipes. These new heads offer flexibility of flame adjustment to an extent never before possible.

LINDE DEVELOPMENT
often begins in the field

THE Linde organization makes it an important part of its business to develop new oxy-acetylene apparatus as it is needed—apparatus that enables Linde customers to do a better job—and to do it faster, easier, or at less expense.

For this reason, Linde research frequently begins in the field. The Linde field organization keeps in close touch with the changing requirements of industry. In the Linde laboratories, development men design efficient new equipment to meet these requirements. A specialized factory organization manufactures this new equipment to perform well and last long.

Let the Linde representative who calls on you tell you more about the Oxweld line, and the process service which helps Linde customers use Linde products with more profitable results. The Linde Air Products Company, Unit of Union Carbide and Carbon Corporation. Offices in Principal Cities.

LINDE OXYGEN . . . PREST-O-LITE ACETYLENE . . . UNION CARBIDE
OXWELD, PREST-O-WELD, PUROX APPARATUS . . . OXWELD SUPPLIES

The words "Linde," "Prest-O-Lite," "Union," "Oxweld," "Prest-O-Weld" and "Purox" are trade-marks of Units of Union Carbide and Carbon Corporation.

Welding Journal

VOLUME 18, No. 11 DECEMBER 1939

THE AMERICAN WELDING SOCIETY

TWENTY-one years ago the AMERICAN WELDING SOCIETY was organized by a small group of men who appreciated the advantages of cooperative action in the solution of welding problems, in increasing the knowledge of welding and extending its applications. From these small beginnings the AMERICAN WELDING SOCIETY has grown steadily until it is now recognized as the leading scientific organization in the world devoted to the interests of welding. The Society is an organization of individuals and of various companies and corporations interested in welding by any process, either as users concerned with the efficient application of the art or as manufacturers of welding equipment and supplies interested in its acceptance.

THE WELDING INDUSTRY

The growth of the Society parallels the growth of the welding industry itself. Twenty-one years ago welding was considered in the light of a useful tool of repair. Today it is considered the greatest production tool of modern times.

Welding is now the standard method of joining metal parts in manufacturing, construction, production and maintenance work. Applications range from the welding of small articles, such as watch stems, to the largest metal fabricated structures—ships, locomotives, pipe lines, buildings, bridges, aircraft structures, pressure vessels and machinery of all kinds.

THE PLACE OF AN ENGINEERING SOCIETY

The development of the science and art of any branch of engineering involves the work of individuals and of associations, both professional and industrial. The allocation of credit to the several individuals or associations involved, is absolutely impossible in the quantitative sense, but there can be no doubt as to the very important part played by the Engineering Societies which render a service not rendered by any other type of organization. The benefits of this service accrue directly or indirectly to every individual and to every corporation connected with the corresponding industry. In a broader sense these benefits accrue also to the nation as a whole. For example, any increase in knowledge which makes possible the application of welding in a new field or makes applications in older fields safer as well as more efficient, means more business for both the manufacturer of welding equipment and the manufacturer who uses such equipment, economies for the users of the welded products, more demand for electric power, safer risks for insurance companies and more opportunities for employment, not only of welding operators, but also of welding engineers and many others who either are or should be members of the AMERICAN WELDING SOCIETY.

OPPORTUNITIES FOR COOPERATION IN THE WELDING INDUSTRY

With the complexity of modern industry it is necessary for people working in the same field to get together, as progress can be made best through such cooperative effort rather than by individual genius.

ADVANTAGES TO BE GAINED THROUGH MEMBERSHIP IN THE SOCIETY

The AMERICAN WELDING SOCIETY offers (1) An opportunity for personal association with the leaders of the industry and exchange of information and ideas. (2) Means to keep informed on the latest developments in the welding field. (3) Opportunity to assist through cooperative effort in increasing the knowledge of welding and extending its applications. (4) Assistance in the development and use of codes and standards. (5) An opportunity through cooperative action to secure material benefits at a minimum cost.

SCOPE OF SOCIETY ACTIVITIES

Some of the aims of the Society may be briefly summarized as follows:

1. To collect and make available authentic and up-to-date information on welding and cutting, and to be recognized as the authoritative source of such information by manufacturers in the welding field, users of the process, engineering societies and legislative bodies.
2. To provide a means for the interchange of knowledge and experience to aid in the solution of the problems of the welding art—technical, ethical and commercial—through cooperative effort.
3. To be the agent of the industry in cooperative research on important technical problems, and to serve as a common spokesman of the industry in matters pertaining to its welfare.
4. To provide opportunities for social intercourse, and thus to promote a better understanding among manufacturers, users and scientists in the welding field, and to foster a spirit of cooperation for the common good.

5. To encourage the development of welding through improvements in present applications and expansion of its uses into new fields, and to develop technical and ethical standards for the welding industry.

RESEARCH

Fortunately for the welding industry and for the AMERICAN WELDING SOCIETY research has always played an important part in the Society activities. As a matter of fact, the Society owes its very origin to the research work conducted by the Welding Committee of the Emergency Fleet Corporation. This research work was formerly conducted in a cooperative way by the AMERICAN WELDING SOCIETY through its research board, known as the American Bureau of Welding. More recently it is conducted through the Welding Research Committee which is affiliated also with Engineering Foundation and not only has the sponsorship of the AMERICAN WELDING SOCIETY but, also, of the American Institute of Electrical Engineers. Moreover, it enjoys the cooperation of many other engineering bodies.

Continuous research work has been carried on by individuals and private corporations. This research work has made possible the remarkable advance in welding in a technical way so that we may now have a welded joint whose integrity need not be questioned and whose strength as determined by physical tests, static and dynamic, is equal to or surpasses the base material which it joins. Research work has been carried on extensively in our leading universities so that young engineers have familiarized themselves with welding, thus carrying out one of the important educational activities of the Society. The supplement to this issue of the JOURNAL contains a complete Annual Report of the Welding Research Committee.

Reports of all research activities are made available to the members of the AMERICAN WELDING SOCIETY through publication in the JOURNAL of the Society.

PUBLICATIONS

The JOURNAL is the official publication of the AMERICAN WELDING SOCIETY. It has also been accepted as the official organ of the Welding Research Committee of Engineering Foundation. It is necessary for all those interested in welding to read it to keep abreast of the latest information on welding and to have available the latest codes, standards and specifications on all phases of Welding.

Not only does the JOURNAL carry, usually first among publications, all of the technical welding information available, but also a large proportion of its content consists of interestingly written practical articles of popular appeal. It carries departmentals and articles on "Society Activities," "News Items," "Current Welding Literature," "New Products," "How to Weld It," "Welding Abroad," "Safety Kinks," "Welding Surveys," "Communications" and "Positions Available."

The JOURNAL is published monthly and distributed to all members of the Society without further charge.

All of the important papers presented before the Society and National and Section meetings are published regularly. Each year a Subject and Authors' Index is prepared.

The various issues of the JOURNAL for a single year contain a veritable encyclopedia of up-to-date information on welding.

The Society also publishes a Membership Directory giving the names and addresses of all members.

NATIONAL AND SECTION MEETINGS

It is a human trait of civilized people to get together and exchange experiences and through such exchange to be mutually helpful in the furtherance of some important needed activity. The rapid growth of the welding processes in such a short space of time has been made possible only through the most liberal exchange of information by every one identified with the welding industry. Papers presented before Section meetings, national meetings or published in the technical press are common ways of bringing about this exchange of knowledge. There are at present some forty Sections of the Society in important cities throughout the country. Most of these hold monthly meetings in season.

The Annual Meeting is held in the fall in connection with the National Metal Congress and Exposition at which the latest developments in welding equipment, materials and applications are exhibited and demonstrated.

One new idea derived from discussion at these meetings may be worth the cost of membership for several years.

EDUCATIONAL EFFORTS

In bringing about an appreciation of the value and benefits of welding and the correct applications thereof involves education in many forms. The educational work of the Society is also conducted in a variety of ways, among which may be mentioned the Section and National meetings referred to above, THE WELDING JOURNAL, and, particularly, lecture courses organized by many of the Sections for this specific purpose. The new Welding Handbook of the AMERICAN WELDING SOCIETY is another useful medium. In previous years manuals served a similar purpose.

WELDING HANDBOOK

The AMERICAN WELDING SOCIETY has recently issued a comprehensive Handbook of more than 1200 pages devoted to every phase of welding prepared by some 90 authors and reviewed by 237 other experts. It represents a cross section of experience and knowledge of the leading welding talent in this country. The book is available free to members in the Sustaining Member and Member grades and at a special reduced price of $4.00 to the other grades. The price of the book to non-members is $6.00 in the United States and $6.50 elsewhere.

The first edition of the Welding Handbook of the AMERICAN WELDING SOCIETY, was prepared by the Society for the metal industries in response to a demand for practical up-to-date information, in concise form, on the subject of welding. In order to best serve its purpose the volume has been prepared to cover, first, the fundamentals of the various processes, second, the materials used and the testing methods involved, and third, the applications thereof. The above treatment of the broad subject of welding is supplemented by ample data on metal cutting and a number of processes allied to welding and cutting.

The aim has been to insure that statements of fact shall be accurate and the recommendations authoritative. To this end, the Committee selected outstanding authorities, from those available in each field, as authors for the respective chapters, and subsequently invited at least five other authorities to review each chapter. The desirability of an impartial treatment of subject matter was recognized by both authors and reviewers.

CODES AND SPECIFICATIONS

The AMERICAN WELDING SOCIETY is recognized as the authoritative spokesman in this country in the matter of welding codes. In many instances these codes are issued in the name of the AMERICAN WELDING SOCIETY and are known as AMERICAN WELDING SOCIETY Codes on Welding. In other instances it has appeared to the Board of Directors and Executive Committee advisable to cooperate with other engineering societies and legislative bodies in the issuance of codes relating to welding. In such instances, it has not always been advisable for the Society to claim undue credit in connection with these codes. However, in all cases the aim of the Society has been to promote the knowledge of welding and extend its applications with safety and at the same time prevent unjust restrictions being placed on the use of welding.

Codes in general are not permanent unchangeable documents. In welding, particularly, advancement is fairly rapid. It is necessary to keep these codes up to date and constantly revised.

Some of the Society Codes are Code for Fusion Welding and Gas Cutting in Building Construction, Code for Fusion Welding and Gas Cutting in Machinery Construction, Specifications for Welded Highway and Railway Bridges—Design, Construction and Repair, Marine Code for Welding and Gas Cutting, Part D, Standard Tests for Welds, Fusion Welding Symbols, Report on Welding or Cutting Certain Types of Containers Which Have Held Combustibles, Standard Qualification Procedure, Tentative Rules for the Fusion Welding of Gravity Tanks, Tank Risers & Towers and the Welding of Oil Storage Tanks.

Some of the Codes issued jointly with other Societies are: Filler Metal Specifications, Oil Storage Tank, Gravity Tanks, Tank Risers and Towers. Still other Codes in which the Society has taken a prominent part are the A.S.M.E. Unfired Pressure Vessel and Boiler Codes, and the A.S.A. Code for Pressure Piping.

In addition to these, there are a number of codes in preparation.

Members of the Society, therefore, have a voice in forming the codes under which they work—an advantage which is not to be overlooked.

STANDARDS

The Society has long realized the value of specifications and standards. For example, in the early days of the industry a wide variety of nomenclature and definitions was in vogue. Now, however, by means of standard symbols evolved by the Society, draftsmen are able to specify clearly the type, location and size of welding to be used, thereby eliminating confusion and expense. Other standards developed by the Society include standard tests for welds, standards for arc-welding and resistance-welding apparatus, specifications for welding wire and gages for measuring dimensions of welds.

The rapid growth of welding within the past few years may well be due to the maintenance of quality standards and procedure specifications which have eliminated doubt as to quality of workmanship, and brought industry in general to look upon welding with greater confidence.

MEMBERSHIP CERTIFICATE

Each member* of the Society is privileged to display, in office or shop, the certificate. The certificate states that the individual or company is a member of the AMERICAN WELDING SOCIETY—an organization devoted to the advancement of the science and art of welding.

* Except operating and student members.

HEADQUARTERS

The Society has its headquarters in the Engineering Societies Building, 25–33 West 39th Street, New York. This is also the headquarters of the major engineering societies of the country.

CLASSES OF MEMBERSHIP

Sustaining Members.—A Sustaining Member shall be an individual delegated by a corporation, firm or partnership interested in the science and art of welding, with full rights of membership.

Annual dues.....$100.00

Members.—A Member shall be an individual not less than 23 years of age who shall have been for at least three years engaged in work having a direct bearing on the art and science of welding and shall have made some contribution to the science and art of welding with full rights of membership. Corporate members in good standing of any major engineering society are eligible.

Annual dues..... $15.00

Associate Members.—An Associate Member shall be an individual interested in the science and art of welding with rights to vote but not to hold office except in Sections as may be provided for by the By-Laws of the Section.

Annual dues..... $10.00

Operating Members.—An operating member shall be an individual who, by occupation, is an operator of welding or cutting equipment, without the right to vote or to hold office excepting in the Section as may be provided for by the By-Laws of the Section. Beginning October 1, 1938, no *Operating Member* may continue in this status for more than a total period of 2 years (consecutive or otherwise). At the end of the two year period, the Operating Member shall automatically be moved up to the "Associate" grade.

Resident Operating Members are those residing in the United States, Canada or Mexico. Foreign Operating Members are those outside of the United States, Canada or Mexico.

Annual dues, United States and Canada.... $5 00

Other countries..... $10 00

Honorary Members.—An Honorary Member shall be an individual with full rights of membership. Honorary Members shall be persons of acknowledged eminence in the welding profession, or who may be accredited with exceptional accomplishments in the development of the welding art upon whom the AMERICAN WELDING SOCIETY may see fit to confer an honorary distinction.

Student Members.—A Student Member shall be an individual who is actually in attendance at a recognized college or university, taking a course leading to a degree, without the right to vote or to hold office excepting in Section as may be provided for by the By-Laws of the Section. At the termination of fiscal year of status as a student, affiliation as "Student Member" shall cease.

Annual dues.....$2.50

CLASSIFICATION OF MEMBERS INTERESTED IN AMERICAN WELDING SOCIETY

1. Engineers interested in metals, their fabrication, maintenance of plant equipment, or designers who must specify materials, sizes and must know unit strengths of various types of joints for various applications.
2. Production men concerned with lowering costs, improvement of products or speeding up time schedules.
3. Purchasing agents interested in the purchase of the best materials with due consideration to cost and other factors.
4. Draftsmen who need to know the latest symbols, types of welding and methods which are applicable to their work.
5. Presidents of companies concerned with broad economic and technical developments which may affect the operations of their companies.
6. Foremen and welders concerned with the best procedures, methods, techniques and latest developments in the art of welding.
7. Professors and students who desire fundamental training and knowledge in engineering principles and recent developments in welding which may affect design construction or production operations.
8. Research men desiring to keep abreast of the latest developments in science, metallurgy and other practical applications.
9. Inspectors whose duty it is to see that proper welding procedures are followed by qualified welders and in accordance with specifications.
10. Contractors interested in having accurate knowledge as to lowering costs of construction, safety, best materials and procedures and information as to codes, standards and specifications.

MEMBERSHIP REPORT—SEPTEMBER 30, 1939

DURING the year three new Sections of the Society were organized as follows:

Puget Sound with headquarters at Seattle, organized April 17, 1939;

Louisiana, with heaquarters at New Orleans, organized May 12, 1939;

Quad-Cities, comprising Davenport, Iowa; Rock Island, Moline and East Moline, Illinois, organized April 12, 1939.

Due to inactivity the Portland Section was disbanded by vote of the Executive Committee at its meeting of September 14th.

These Sections are in reasonably satisfactory condition. In addition to meetings, inspection tours and special gatherings, the activities of the local Sections include lecture courses and arrangement for welding conferences and round-table discussions. National headquarters with the cooperation of local Sections has compiled a guide for the operation of Sections which meets the procedure recommended for the conduct of affairs of existing Sections and those in process of formation. This guide also contains standard By-laws for Sections, prepared by the By-laws Committee and approved by the Executive Committee.

On October 1, 1939, our membership totaled 3693. On the same day in 1938 the figure was 3538, a gain of about 4½ per cent over last year. The percentage of membership in each classification is as follows:

Sustaining Members	2.4
Members	31.0
Associate Members	42.3
Operating Members	23.3
Student Members	1

Membership Status as of October 1, 1939

	A	B	C	D	E	F	Total
Total Membership, October 1, 1938	88	987	1586	786	1	90	3538
Increase during year	8	290	347	349	1	28	1023
	96	1277	1933	1135	2	118	4561
Decrease during year	9	131	370	275	..	83	869
Total Membership, October 1, 1939	87	1146	1563	860	2	35	3693
Net change during year	−1	159	−23	74	1	−55	155

Society Membership, October 1, 1939

	A	B	C	D	E	F	Total
Birmingham	1	13	26	18	.	..	58
Boston	1	30	45	36	.	..	112
Canada	..	25	21	18	.	1	65
Canton	2	5	13	3	.	..	23
Chattanooga	1	2	33	2	.	..	38
Chicago	9	71	81	77	.	..	238
Cincinnati	..	22	26	23	.	..	71
Cleveland	9	40	108	18	.	..	175
Colorado	1	12	1	12	.	..	26
Columbus	..	13	14	10	.	4	41
Connecticut	4	16	22	3	.	1	46
Detroit	2	30	53	13	.	2	100
Georgia	..	10	19	4	.	..	33
Hawaii	..	20	16	4	.	..	40
Indianapolis	1	19	10	15	.	1	46
Kansas City	..	28	45	21	.	..	94
Los Angeles	2	36	87	47	.	1	173
Louisiana	2	10	13	5	.	..	30
Maryland	1	18	25	18	.	..	62
Memphis	..	8	7	11	.	..	26
Milwaukee	2	19	39	29	.	..	89
Montana	..	2	8	17	.	1	28
New York	11	153	161	62	.	2	389
Northern N. Y.	1	10	34	32	.	..	77
Western N. Y.	..	15	23	10	.	..	48
Northwest	..	37	21	22	.	..	80
Oklahoma	..	15	24	15	.	12	66
Omaha	..	8	14	5	.	..	27
Peoria	1	9	35	22	.	..	67
Philadelphia	15	64	97	28	1	2	207
Pittsburgh	11	56	39	10	1	..	117
Puget Sound	..	21	14	5	.	..	40
Quad-Cities	..	7	8	2	.	..	17
Rochester	..	22	26	38	.	..	86
St. Louis	..	21	37	9	.	2	69
San Francisco	2	18	40	18	.	2	80
San Joaquin	..	1	11	22	.	..	34
South Texas	1	37	30	19	.	..	87
Tulsa	..	32	15	28	.	..	75
Washington	1	29	63	5	.	..	98
Wichita	..	9	15	29	.	..	53
Youngstown	4	23	17	17	.	..	61
Not in Sections	2	110	127	58	.	4	301
	87	1146	1563	860	2	35	3693

REPORT ACTIVITIES MEETINGS and PAPERS COMMITTEE

THE Meetings and Papers Committee has under its general jurisdiction (1) the publication of THE WELDING JOURNAL, and (2) the Technical Program of the Annual Meeting. Quite a number of improvements were made in the JOURNAL during the past year. The news items relating to the welding industry have been materially increased and so positioned in the JOURNAL as to allow every advertisement in the JOURNAL to have some reading matter next to it, thus making every advertisement preferred space. This step has assisted in securing additional advertising. The JOURNAL has received worldwide praise and it is believed that the instructive and interesting articles published make the JOURNAL compare favorably with other technical papers.

The Research Supplement of THE WELDING JOURNAL and the monthly bulletins of the Welding Research Committee continue to be one of the greatest assets to the JOURNAL. This material is a chronicle of the progress of systematic and organized research which is of benefit to the entire industry. The critical digests of literature in the opinion of the experts all over the world, make the JOURNAL a unique source of information which it would be impossible to obtain from any other source. The total number of pages published for the year ending September 1939 was 1316, which is a slight increase over the amount published last year—1290.

The principal activity of the Program Committee consists of arrangements for the Annual Meeting. When called upon it also assists in the preparation of programs for district and sectional meetings. This year some 56 papers were presented at 15 technical sessions. The program was by far the most outstanding in the history of the AMERICAN WELDING SOCIETY.

PUBLICATION OF JOURNAL RULES OF PROCEDURE

The Society shall publish and distribute free of charge to its members a monthly publication to be known as the JOURNAL OF THE AMERICAN WELDING SOCIETY, or in abbreviated form, THE WELDING JOURNAL.

The JOURNAL may include editorials, news items, technical reports and papers, employment service bulletin, bibliography of current welding literature, advertising and other items arranged, from time to time, by the Publications Committee. All papers presented at Section and National meetings shall be the property of the AMERICAN WELDING SOCIETY. All Sections and the National Program Committee are requested to send copies of all papers presented at monthly Section meetings or National meetings promptly to the Editor of the JOURNAL.

Reports of the Welding Research Committee, technical and other committees of the Society selected for publication shall be published in the JOURNAL. Such reports, however, shall receive the necessary approval of the Welding Research Committee and/or the AMERICAN WELDING SOCIETY before being released for publication.

Any member of the AMERICAN WELDING SOCIETY and others interested in welding shall have the privilege of writing technical papers and submitting them for consideration of the Publications Committee. Papers selected for publication shall be published at the earliest convenient time in the JOURNAL. Authors submitting papers for publication in the JOURNAL shall not submit them to other publications unless this is clearly indicated in the accompanying letter of transmittal.

The Editor shall advise within twenty days after receipt of papers whether or not they will be published in the JOURNAL. Failure of such notification will allow the author the privilege of submitting the paper to other publications.

Papers presented before National meetings, Sections of the Society, committee reports and papers dealing with results of research investigation, details as to technique and kindred papers will be favored as to time and space.

Papers presented before joint meetings with other societies shall be treated as special cases and the rights of cooperating organizations shall be fully recognized.

The immediate supervision of the work in handling of publication details shall rest in the hands of an Editor who shall, however, at all times be responsible to the Publications Committee. In case of doubt as to policy in regard to editorials, advertising and technical papers, the Editor shall call upon the Publications Committee, except that in the case of disagreement, it will be referred to the Executive Committee of the Society.

The Editor shall delete all advertising from papers and an author may not bring in the name of his company more than once in an article and preferably not at all. The title and connection of the author of any paper may be indicated by suitable footnote. All trade names not generally accepted by the Nomenclature Committee may be deleted at the option of the Editor. The author or his company may, however, use the advertising space in a particular issue in which his article appears to bring out advertising points, subject, however, to the general requirements for advertising matter.

Papers dealing with comparisons relating to competitive processes when originating with a manufacturer of equipment shall not be accepted for publication. When such papers originate with "users" they may be accepted if advancing the general aims and objectives of the Society.

The AMERICAN WELDING SOCIETY is primarily concerned with welding, cutting and allied processes. Inasmuch as there are other organizations and publications which cover other fields, articles shall in general deal with welding and cutting matters or their closely allied processes. Articles dealing with the use of welding and/or cutting equipment for other purposes than that indicated above shall in general not be published in the JOURNAL.

Each author may receive up to six copies of the JOURNAL in which his article appears without charge.

The Publications Committee may not incur any expense in connection with the publication unless provided

for in the annual budget or by the Executive Committee. The Editor of the JOURNAL shall be required to manage publication costs in such a way that the total for the year shall not vary more than 5% from the allotted budget. Modifications in the budget and other special arrangements may be made upon approval of the Finance and Executive Committees. The Editor shall be permitted to solicit cooperation from individuals, companies, committees and associations in defraying costs of cuts, drawings or extraordinary printing expenses.

FINANCIAL REPORT

October 4, 1939

Miss M. M. Kelly
American Welding Society
33 West 39th Street
New York, New York

Dear Miss Kelly:

We have conducted a Balance Sheet audit of the books and records of your Society for the fiscal year ended September 30, 1939 and present herewith our report comprising the following Exhibits and Schedules.

EXHIBIT "A"—Comparative Statements of Assets & Liabilities as at September 30, 1939 and September 30, 1938.

EXHIBIT "A"-1—Comparative Statement of Total Net Worth as at September 30, 1939 and September 30, 1938.

EXHIBIT "A"-2—Reconciliation of Net Worth for the fiscal year ended September 30, 1939.

EXHIBIT "B"—Operations for the Fiscal Year Ended September 30, 1939.

SCHEDULE 1—Miller Memorial Fund.

SCHEDULE 2—Special Technical Committee Activities.

SCHEDULE 3—Welding Handbook Account.

Very truly yours,
(Signed) DAVID JOSEPH
DAVID JOSEPH & COMPANY

AMERICAN WELDING SOCIETY
COMPARATIVE STATEMENTS OF OPERATING NET WORTH
SEPTEMBER 30, 1939 AND 1938

EXHIBIT "A"

	1939	1938
ASSETS:		
Current Assets:		
Cash on Hand	$ 75.00	$ 75.00
Cash in Bankers Trust Co.	4,297.02	1,185.19
Investment—$6000 Par Bonds of the St. Louis and San Francisco Ry. Co. 4's Due 1950 at Market Value	690.00	660.00
Accounts Receivable—Journal:		
Advertising	1,470.00	908.02
Supplements, Cuts and Journals	11.53	...
Sundry Accounts Receivable—Sales of Reprints, Bulletins, Emblems, Etc.	187.87	306.66
Deferred Charges:		
Stationery and Postage	167.18	244.41
Annual Meeting Expense	560.86	445.41
Cost of Journal Cuts	948.86	...
Fixed Assets:		
Inventory of Bulletins and Printed Matter (Estimated)	2,500.00	2,500.00
Office Equipment (Cost $4,029.13)—Less: Depreciation	1,792.49	1,546.19
Total Assets	12,700.81	7,870.88
LIABILITIES:		
Accounts Payable	1,404.44	2,616.87
Section Refunds on Paid Dues	481.63	1,015.50
New York City Sales Tax Payable	1.64	...
Dues Held in Suspense	63.90	40.00
Total Liabilities	1,951.61	3,672.37
OPERATING NET WORTH (Exhibit "A"-2)	$10,749.20	$4,198.51

AMERICAN WELDING SOCIETY
COMPARATIVE STATEMENT OF TOTAL NET WORTH
SEPTEMBER 30, 1939 AND 1938

EXHIBIT "A"-1

	1939	1938
OPERATING NET WORTH—EXHIBIT "A".	$10,749.20	$4,198.51
SPECIAL, EARMARKED AND RESERVE FUNDS:		
I. Miller Memorial Fund, on deposit with Union Dime Savings Bank	1,243.12	710.84
II. Special Technical Committee Activities Fund, on deposit with Bankers Trust Co.	150.98	93.17
III. Welding Handbook Account, on deposit with Corn Exchange Bank Trust Co.—reserved for working capital purposes as advances against cost of next issue.	1,025.39	349.68
IV. Permanent Funds Committee Account, on deposit with Emigrant Industrial Savings Bank, as authorized by the Board of Directors on April 27, 1939, to be administered by a "Committee on Permanent Funds," created out of Surplus in the Welding Handbook Account (III, above).	2,248.71	None
TOTAL NET WORTH—SEPTEMBER 30TH.	$15,417.40	$5,352.20

AMERICAN WELDING SOCIETY
RECONCILIATION OF NET WORTH
SEPTEMBER 30, 1939

EXHIBIT "A"-2

NET WORTH—OCTOBER 1, 1938		$ 4,198.51
ADD:		
Cancellation of Checks for 1938 Refunds to Canton Section—Section Inactive	$ 131.50	
Purchases of Office Equipment—Charged Off as Expenses during Year now Capitalized	603.33	

Increase in Market Value of Investment in Bonds of St. Louis and San Francisco Ry. Co. since October 1, 1938	30.00	
Receipt of Funds from Disbanded Portland Section, Said Funds Being the Balance Remaining in the Section's Treasury	57.36	
Refund of Part of Expenses Connected with the Extended Activity	2,475.60	
Net Excess of Income over Expenses for the year Ended September 30, 1939 (Exhibit "B")	3,759.93	7,057.72
		11,256.23
LESS:		
Audit Expense for the Six Month Period Ended September 30, 1938, Paid During Current Fiscal Year	150.00	
Provision for Depreciation of Office Equipment for the Fiscal Year	357.03	507.03
NET WORTH—SEPTEMBER 30, 1939		$10,749.20

AMERICAN WELDING SOCIETY
OPERATIONS FOR THE YEAR ENDED SEPTEMBER 30, 1939

	EXHIBIT "B"
INCOME:	
Membership Dues	$41,671.87
Advertising	15,409.20
Supplement	2,890.87
Journal Cuts	771.05
Subscriptions and Sales of Journals	6,712.91
Bulletins and Printed Matter	1,131.91
Sundry Saleable Matter	632.81
Income from Annual Meeting	3,056.00
Total Income	72,276.62
EXPENSES:	
Journal	
Text Matter and Advertising Section	22,186.70
Technical Activity and Printed Matter	6,641.83
Administration	
Headquarters' Salaries	6,840.08
Postage	543.95
General Expense	318.99
Stationery and Printing	406.89
Telephone and Telegraph	108.74
Addressograph	65.94
Travel and Entertainment	83.60
Rent	695.49
Yearbook Printing	380.00
Committee Expense	308.58
Membership and Sections	4,356.13
Annual Meeting	4,496.21
Sections	
Section Refunds	8,662.69
Section Expense	122.46
Other Items	
Welding Handbook	692.17
Purchase of Equipment	603.33
Cost of Sundry Saleable Matter	402.91
Miller Memorial Medal	600.00
Total Expense	68,516.69
Net Excess	$ 3,759.93

AMERICAN WELDING SOCIETY
STATUS OF MISCELLANEOUS FUNDS
SEPTEMBER 30, 1939

SCHEDULE 1

MILLER MEMORIAL FUND

Balance on Hand—October 1, 1938	$ 710.84
Deposits Made by American Welding Society	600.00
Interest Credited by Bank for year	17.28
	1,328.12
Expenditures for Miller Medal	85.00
Balance in Union Dime Savings Bank—September 30, 1939	$1,243.12

SCHEDULE 2

SPECIAL TECHNICAL COMMITTEE ACTIVITIES

	Amount	Disbursements	Balance*
Donations Reserved and Allocated for Purposes of:			
Committee on Pipe for Hydraulic Purposes	$200.00	$106.83	$ 93.17
Committee on Rules for the construction of Oil Storage Tanks	75.00	75.00	...
Welding in Marine Construction	125.00	67.19	57.81
Totals	$400.00	$249.02	$150.98

SCHEDULE 3

WELDING HANDBOOK ACCOUNT

Balance on Hand—October 1, 1938	$ 349.68
Deposits—Year Ended September 30, 1939	10,786.45
	11,136.13
Disbursements—Year Ended September 30, 1939	7,862.03
Balance on Hand—September 30, 1939	$ 3,274.10

Deposited with:	
Corn Exchange Bank	$1,025.39
Emigrant Industrial Savings Bank	2,248.71
	$3,274.10

* Totals—Balance on Deposit with Bankers Trust Co.

BOUND VOLUME JOURNAL

The AMERICAN WELDING SOCIETY has made arrangements for members who wish to have their Journals for 1939 bound in attractive, imitation black leather covers, to do so by sending copies of the twelve issues to Russell-Rutter Company, Thirty-Third Street and Eighth Avenue, New York, N. Y., Att. Mr. Russell Lauben, Jr. A special reduced rate has been arranged at $1.75 per volume providing the issues of the JOURNAL are sent to the binder on or before February 10th. The bill for the binding and return postage will be sent direct from the Society's office.

TECHNICAL and SPECIAL COMMITTEES of the American Welding Society

COMMITTEE ON BUILDING CODES

APPOINTED January 9, 1928, to assist municipal and state code committees and building departments in preparation of suitable regulatory specifications for fusion welding in fabrication of structural steel, piping and tankage in building construction. It was intended that the committee issue recommendations for welding which have proved to be sound from an engineering standpoint and safe in practical application, and that it endeavor to have modified any unjust restrictions on welding and cutting which may now be imposed. In 1930 it was decided that the committee's activities also include the preparation of a Code for Resistance Welding in Building Construction.

The Committee prepared Code for Fusion Welding and Gas Cutting in Building Construction, Part A—Structural Steel, which was issued in 1928; it compiled data on existing welded structures and cities which have adopted or permitted the use of welding in building codes which was published in the Society's JOURNAL in January 1930; it prepared a second edition of Code for Fusion Welding and Gas Cutting in Building Construction, Part A—Structural Steel, which was issued in 1930.

A third edition of Code 1, Part A, for Fusion Welding and Gas Cutting in Building Construction was published in January 1935.

The provisions of our Building Code have now been adopted by more than two hundred municipalities, including the great metropolitan centers of New York, Chicago and Baltimore.

During the early part of 1938 this Committee issued a revision of Code 1, Part A, as the edition of 1937. It has cooperated with the Board of Standards and Appeals of New York City in formulating rules for fusion welding under the new City Building Code, and has also acted on inquiries from various cities and sections of the Society regarding building code matters.

COMMITTEE ON WELDING BRIDGES

Upon completion of its work in the preparation of the Specifications for the Design, Construction, Alteration and Repair of Highway and Railway Bridges by Fusion Welding, issued in April 1936, the original Committee on Welding Bridges was discharged. A new committee for the review and interpretation of the Specifications was authorized by the Executive Committee of the Society in order that proposals for the amplification of the Specifications and questions arising in connection with it might be properly handled.

The new committee is made up of official representatives of interested organizations and is called the Conference Committee on Welding Bridges and is devoting its activities to:

(1) Clarifying points in the specifications which may be questioned as a result of practical application; and

(2) Securing the adoption of the A. W. S. bridge specification by professional bodies throughout the country.

The Specification for the Design, Construction, Alteration and Repair of Highway and Railway Bridges by Fusion Welding, has continued to find an expanding field of application. It has been approved by the American Railway Engineering Association, and is to be published as an appendix in the 1938 Edition of the American Association of State Highway Officials Bridge Specifications.

As our Bridge Specification is applied to actual practice the lessons of experience are carefully recorded for use as a basis of revision along the lines of conformity to field and shop requirements. The permanent committee has been charged with the duty of considering revisions, and issuing periodically successive editions of the specification, which will embody progressive modifications and improvements. As a result of slightly more than two years of practical experience, the first of these amended editions, that of 1938, is now available.

Subcommittees on Workmanship and Butt Welds and Welding Technique have been active in preparing further revisions. The Committee has cooperated with Committees on Filler Metal, Qualification Procedure and Standard Tests for Welds in so far as they relate to the work of the Bridge Committee. The present Specifications will be revised and brought up to date and issued in 1940. It is hoped that the results of research work on fatigue strength of welded joints will then be available.

FILLER METAL SPECIFICATIONS COMMITTEE

Filler Metal.—After seven years of arduous labor, the Committee has brought about the issuance of a Specification for Iron and Steel Filler Metal that has been adopted and published jointly by the A.S.T.M. and the Society in 1938.

The Committee has undertaken revision of the Filler Metal Specifications with the idea of their simplification. Separate specifications will be issued for Gas and Arc Welding. The Arc Welding Specification has been prepared in preliminary form and is being submitted to a number of people for comments and criticisms. It is expected that this Specification will also be issued as a joint Specification with the A.S.T.M. As soon as issued, corresponding specifications will be undertaken for Gas Welding and for Alloy Steels and Non-Ferrous materials.

COMMITTEE ON WELDING IN MARINE CONSTRUCTION

Organized September 24, 1929, to prepare an "American Welding Society Marine Code for Fusion Welding and Gas Cutting"; to report on any question regarding the use of Welding in Marine Construction that may be submitted to the Society; to submit a periodical statement on status of Welding in Marine Construction both

at home and abroad; and to advise on the making of tests needed by the shipbuilding industry in case they are of a nature that might best be undertaken by an authoritative body, such as the Society.

In July 1930 the Committee was enlarged to include two Subcommittees—one on Boilers and the other on Hulls. The various Subcommittees were authorized to act as conference committees to the Bureau of Navigation and Steamboat Inspection of the U. S. Department of Commerce and assist that body in preparation of specifications for boilers, unfired pressure vessels, piping, hull structures and on other matters coming under the cognizance of the various Subcommittees.

The Code as outlined is subdivided as follows:

Part (*A*) General Application
(*B*) Rules for Fusion Welding of Drums and Shells of Marine Boilers and Pressure Vessels
(*C*) Rules for the Repair of Marine Boilers and Pressure Vessels
(*D*) Rules for the Fusion Welding of Hulls and Hull Parts
(*E*) Rules for the Repair of Hulls by Fusion Welding
(*F*) Rules for Fusion Welding of Marine Piping Installations

Part (*B*) has been completed and approved by the Main Committee and the Board of Directors of the Society. These rules are based on rules prepared by the A. S. M. E. Boiler Code Committee in cooperation with the A. W. S. Committee on Welded Pressure Vessels. They have been accepted by the Bureau of Navigation and Steamboat Inspection of the U. S. Department of Commerce Committee and have been embodied in Tentative Marine Boiler Rules of this Bureau.

The Boiler Subcommittee is undertaking the preparation of Part (*C*) for which there is a pressing demand. Tentative Rules for Fusion Welding Steam, Oil or Air Piping in Marine Construction, formulated by this Subcommittee, appeared in the April 1938 issue of THE WELDING JOURNAL. These rules apply to piping for working pressures in excess of 100 lb. per sq. in. The purpose of their publication was to obtain the benefit of comments, criticisms and suggestions and they have since been adopted by the Society.

Part (*D*) has been completed and published in the February 1935 JOURNAL OF THE SOCIETY. The new rules of the American Bureau of Shipping relating to hull construction have been based on these rules.

The Hull Subcommittee is undertaking the revision of Part (*D*) and the preparation of Part (*E*).

A Subcommittee, "Committee on Thermal Shrinkage and Stresses," has been organized. The objects of this committee are to make a survey of the present welding procedure practices of the various shipyards; to quantitatively determine the transverse and longitudinal shrinkage of various forms of welds and welded joints used in the construction of ship hulls; and to prepare the data obtained in such form that it can be used in the design and construction of ship hulls.

COMMITTEE ON SYMBOLS

The Committee on Symbols has prepared and offered for adoption revised welding symbols and instruction for their use. After approval by the Board of Directors, these symbols were published in the June 1937 issue of the Society JOURNAL and reprinted in bulletin form.

These revised symbols have been adopted by the various technical bureaus of the United States Navy, and were offered to the American Standards Association with recommendation for adoption as an American Standard.

The AMERICAN WELDING SOCIETY Committee on Symbols is now also a Joint Committee of the American Standards Association on the same topic. Some minor changes have been suggested in the AMERICAN WELDING SOCIETY's report on this subject, and, if these are adopted by both organizations, these symbols will become the American Standard.

The report has been approved by the Board of Directors of the AMERICAN WELDING SOCIETY and enough copies to meet the needs of industry will be made available by the Society until the A. S. A. can act.

The Symbols Committee is also the Committee on Standardized Welding Specification Reference for Use in the Tails of the Arrows. Some progress has been made in these references.

COMMITTEE ON DEFINITIONS AND CHART

During the year the Committee prepared a Master Chart of Welding Processes and a set of Definitions. These were set up in proof form and submitted to a large number of people for comments. These comments have been correlated and it is expected that the Committee will shortly be able to present its final report to the Board of Directors for adoption. Additional work will be needed on new terms and further modification of the main chart.

COMMITTEE ON LARGE WELDED PIPE FOR HYDRAULIC PURPOSES

The Committee has prepared a tentative draft of a code which comprises the general ground covered by the existing codes for unfired pressure vessels with the exception of rules for special fittings such as stiffening rings, supports, reinforcings around openings, Y's, Tees and such. These are left purposely in order to develop first the main body of the code, leaving treatment of specials, for a second stage.

A first draft of the Code was formulated and submitted to members of the Committee and to a few leading manufacturers for discussion and comment. Based on this discussion and comment a second draft has been drawn. Copies have been mimeographed and again distributed to the members of the Committee with a wider distribution among members. Based on the comment and criticism received from this second review it should be possible to draft a definite tentative Code. It is hoped that this objective may be reached during the year.

COMMITTEE ON STANDARD QUALIFICATION PROCEDURE

The Committee worked toward the elimination of confusion by correlating varying qualification test requirements by merging them into one uniform code, adequate to satisfactorily qualify welding operators working under any and all codes, and using any of the established fusion welding processes. It is the purpose of these qualification tests (1) to adequately determine the quality of a fusion welded joint by the specific process used and, this having been determined, (2) to test the ability of the welding operator to apply that process. After two years of the most intensive study in which all existing welding codes and specifications have been analyzed with a view to determining not only the purpose, but also the method involved in qualifying the operators, the Committee sub-

mitted for the Society's approval, a document which it believes gives promise of stabilizing and rendering practical, this rather difficult element of qualifying both the process and the operator of fusion welding. These rules have been published as a supplement to the October 1936 JOURNAL.

During 1938 this Committee revised the AMERICAN WELDING SOCIETY's "Tentative Rules for the Qualification of Welding Processes and Testing of Welding Operators," giving these rules the title of "Standard Qualification Procedure." While there are many methods of welding, some of which may not require a standard qualification procedure, it was felt by the Committee that the manual arc and gas welding processes demanded immediate attention. Therefore, the "Standard Qualification Procedure" was reported upon as "Section 1—Manual Arc and Gas Welding." This Standard Qualification Procedure was approved by the Board of Directors, and printed in pamphlet form by the Society.

This report has since been revised and the first draft was issued in mimeographed form to a number of people for comments. Based on these comments work is now under way on the revision. The principles of the Standard Qualification Procedure as revised, have been adopted by the Boiler Code Committee for revision of Par. P-112 of the Boiler Code. The Standard Qualification Procedure Committee expects to meet shortly and complete the revision. When that has been done the Committee intends to give consideration to the desirability of a Standard Qualification Procedure for automatic processes of welding and also for the welding of brittle materials or those which cannot suitably be tested by the guided bend test as outlined in the present Standard Qualification Procedure.

COMMITTEE ON STANDARD TESTS FOR WELDS

The first draft of "Standard Methods for Mechanical Testing of Welds," prepared in 1937, was distributed and discussed at the Annual Meeting of the AMERICAN WELDING SOCIETY, in October of that year. A number of revisions were agreed upon, and other matters were referred to a subcommittee for study. In June 1938, a revised draft was sent to the representatives of other Committees for study, and a few minor modifications have been suggested.

More important than the completion of these new "Standard Methods" is the fact that, largely on the basis of the general agreement reached at these meetings, such changes have already been made in the weld-testing methods provided in other general specifications as to greatly reduce the undesirable variation.

This Committee has since prepared a report entitled, "Standard Methods for Mechanical Testing of Welds." These standards were set up in proof form and distributed widely for comments. Based on these comments revisions were made and the report submitted to the Board of Directors for approval. There are a few minor items needing adjustment and it is expected that these standards will be issued very shortly.

COMMITTEE ON RULES FOR CONSTRUCTION OF OIL STORAGE TANKS (NOW JOINT COMMITTEE WITH A. P. I.)

Appointed February 7, 1933, to prepare rules covering procedure for welding of underground and basement oil storage tanks. Draft of tentative recommendations of committee which were approved by the Executive Committee of the Society, were furnished to Committee on Flammable Liquids of the National Board of Fire Underwriters. The preparation of standard welding procedure for the construction of oil storage tanks above ground (large-size tanks) was undertaken and the committee was enlarged to include representation from oil and tank manufacturing interests.

The report of this Committee, under the title of "A. W. S. Tentative Rules for Welding of Oil Storage Tanks," was issued during June 1938 in preprint form, and was published in the August 1938 issue of THE WELDING JOURNAL.

There has been no active work either in revision or interpretation of the new rules reported on last year. In order to eliminate conflict with the Code of the American Petroleum Institute, a joint Committee has been appointed to cover the welding features, the A. W. S. Committee on Welding of Oil Storage Tanks has been discharged, and publication of the A. W. S. rules discontinued.

COMMITTEE ON WELDING CODE FOR PRESSURE PIPING

Appointed April 12, 1928, to prepare a welding code for pressure piping for all applications including some hydraulic, gas, air, oil and refrigeration. It was intended that the committee's first activity should be the preparation of a draft of such code for submission to the Subcommittee on Welding of A. S. A. Sectional Committee on Pressure Piping Codes by whose invitation the Society undertook the formulation of a draft of suitable material for inclusion in the general codes being developed by the Sectional Committee. It was intended, however, that the Welding Code prepared by our Committee should ultimately be issued as an A. W. S. standard.

Final draft of Code as formulated by our Committee was formally presented at a public hearing at the 1933 Spring Meeting of the A. W. S. The Code was then approved by the Board of Directors at a meeting on April 28, 1933, when the Chairman of the Committee was instructed to formally transmit the Code to the A. S. A. Sectional Committee on Pressure Piping.

The A. S. A. Code for Pressure Piping in its entirety was issued in July 1935 and contains in Section 5, Chapter 3, the approved Welding Code for Pressure Piping.

The Committee on Pressure Piping of the AMERICAN WELDING SOCIETY is now identical with the American Standards Association Sectional Committee on Code for Pressure Piping, Subcommittee No. 8, Subgroup No. 4. During the past year the Committee has held several meetings and has about completed its assignment.

DEVELOPMENT OF INSPECTION METHOD USED IN MANUFACTURING OF U-69 AND U-70 PRESSURE VESSELS

The Committee has been concerned with the preparation of a specification covering the application of the trepanned plug method, originally suggested by Mr. E. R. Fish of the Hartford Steam Boiler Insurance and Inspection Company. Tentative report was submitted to the Committee for comment and criticism. As a result of replies received, the proposed specification was revised on a compromise basis and the revised draft was published in April 1938.

A. W. S. CONFERENCE COMMITTEE ON WELDING WITH A. S. M. E. BOILER CODE COMMITTEE

Appointed April 12, 1928, to cooperate with Subcommittee on Welding of Boiler Code Committee, A. S. M. E.,

for purpose of assisting the Boiler Code Committee in dealing with questions which arise from time to time, involving the application of fusion welding to boilers and pressure vessels.

During the early days and for a number of years, this Committee was of great help to the Boiler Code Committee and kept the revision relating to welding in conformity with the ideas of the AMERICAN WELDING SOCIETY. More recently the Committee has not been active. Complete revision of duties, methods of making assignment to the Committee, and personnel is under way.

COMMITTEE ON RULES FOR THE WELDING OF GRAVITY TANKS, TANK RISERS AND TOWERS

The first edition of these rules, issued by the National Board of Fire Underwriters in 1935, was based on information and suggestions furnished by our Committee, which served in a joint capacity as representatives of the AMERICAN WELDING SOCIETY and the International Acetylene Association. As a result of about two years of experience in the application of these rules, modifications and improvements were suggested, and, after further study of the subject by our Committee in conjunction with the Committee of the I. A. A., a revision of the rules was completed, and published in the May 1938 issue of THE WELDING JOURNAL. Preprint copies of the report were issued by the headquarters office of the Society in March 1938.

MACHINERY CODE

This Committee was recently appointed but has been extremely active. As a result the Committee has prepared the first draft of the Tentative Code which has been submitted to the members for their approval. Subcommittees have been appointed to cover special portions of the code.

COMMITTEE ON WELDING OF ELEVATED WATER STORAGE TANKS

This Committee organized in 1939 is cooperating with the American Water Works Association in the preparation of a Code for the Welding of Large Elevated Storage Tanks. It is expected that very shortly the first draft of the Code will be available for comments.

COMMITTEE ON RULES FOR THE FIELD WELDING OF STORAGE TANKS

This Committee was appointed in 1939 but has already prepared the first draft of a Code on this subject. It is now being revised in committee and, it is expected, will be available for general comment sometime during the coming year.

COMMITTEE ON MINIMUM REQUIREMENTS OF INSTRUCTIONS FOR WELDING OPERATORS IN TRADE SCHOOLS

Several states have recently passed legislation making it necessary for trade schools to be licensed. The A. W. S. has accordingly been requested to prepare minimum requirements for courses of instructions for trade schools and a Committee has been authorized.

SAFETY RECOMMENDATIONS

A Medical Advisory Committee of three has been appointed during the year, as has also a Subcommittee on Recommended Procedure for Welding or Cutting Containers That Have Held Combustibles.

AMERICAN WELDING SOCIETY REPRESENTATION IN OTHER TECHNICAL BODIES

Any report on the technical activities of the AMERICAN WELDING SOCIETY would be incomplete without reference to its very extensive representation in other technical bodies, which assures the mutual advantages of intimate collaboration, extending the influence and prestige of our own organization, and obtaining for ourselves the benefits to be derived from worldwide research and experience. The status of such representation as it exists at this time is as follows:

American Standards Association

Committee A-10—Safety Code for Construction Work

Our representative in collaboration with various interests has prepared a section dealing with demolition of buildings with particular reference to oxyacetylene cutting. This representative is also covering the section on welding. Material relating to the oxyacetylene process in regard to safe practice has been issued by the International Acetylene Association. Corresponding material on arc welding will be prepared.

Committee A-57—Building Code Requirements for Iron and Steel

During the year 1939, Committee A-57 recommended favorably to the Correlating Committee of the A. S. A. the inclusion of references in its general Building Code to the two AMERICAN WELDING SOCIETY Codes, used in building construction, namely, Code 1, Part A, Code for Fusion Welding and Gas Cutting in Building Construction; and Code 2, Code for Resistance Welding of Structural Steel in Building Construction.

With this action, any Code ultimately issued by the Correlating Committee will contain references to the two A. W. S. Codes mentioned above, thus facilitating the adoption of these Codes as standards by municipal, state and other regulatory bodies.

Committee B-5—Small Tools and Machine Tool Elements

Although this Committee has been very active and has completed 12 American Standards, no direct reference is made to welding. Our representative is in touch with the situation.

Committee B-31—Code for Pressure Piping

At the last meeting of the Sectional Committee, held in New York, N. Y., on Dec. 6, 1938, each of the subcommittees reported satisfactory progress in the revision of the particular sections of the Code assigned to them. The Subgroup on Welding has completed a draft of the proposed chapter on welding and has distributed it to all members of the subgroup and to a considerable number of individuals outside the subgroup, for the purpose of obtaining further criticisms.

Committee C-42—Definitions of Electrical Terms

The work of this Committee is reaching the stage where it is hoped that during the coming winter it will be possible to issue as an American Standard the first edition of Definitions of Electrical Terms, covering over 6000 terms.

Committee C-52—Electric Welding Apparatus

This Committee has been inactive for a considerable period of time and it is well recognized that the existing standards covering electric arc welding and electric resistance welding apparatus require revision. At the suggestion of the chairman of the Committee, Mr. F. M. Farmer, discussions have been under way looking toward the possible change of sponsor for the work from the A. I. E. E. and N. E. M. A. to the AMERICAN WELDING SOCIETY.

Committee Z-2—Code for Protection of Heads, Eyes and Respiratory Organs of Industrial Workers

The sectional committee for this project just completed a revision of the code which was approved by the A. S. A. late in 1938 as American Standard.

Committee Z-5—Ventilation Code

The Committee is in process of reorganization. It should be pointed out that the work of this Committee will be of great importance to the Society on account of the increasing claims of unhealthful conditions for welding operators as a result of fumes produced by the welding. The scope of the work will be broad enough to cover dust hazards and industrial ventilation, so that the question of fumes from welding operations will no doubt receive consideration.

Committee Z-14—Drawings and Drafting Room Practice

This committee has completed two American Standards: one on Drawings and Drafting Room Practice—Z14.1–1935 and one on Graphical Symbols—Z14.2–1935.

Committee Z-28—Work in Compressed Air

The development of the proposed Safety Code for Work in Compressed Air has been held in abeyance, pending completion of tests and research on decompression rates by the U. S. Navy Department and the U. S. Public Health Service.

Committee Z-32—Graphical Symbols and Abbreviations for Use on Drawings

The A. S. A. Sectional Committee on Graphical Symbols and Abbreviations for Use on Drawings has a number of subcommittees at work on various assignments.

The Subcommittee on Symbols for Welding is identical with the AMERICAN WELDING SOCIETY Committee for Symbols. The Society standard symbols have been submitted to the A. S. A. and it is hoped will be adopted as American Standards.

American Society for Testing Materials

Committee A-1—Subcommittee XXI

The AMERICAN WELDING SOCIETY has cooperated with Subcommittee XXI in the preparation of Joint Filler Metal Specifications. The specification issued jointly by the two societies is now in process of revision. It is expected that the portion relating to electrodes will soon be approved by the AMERICAN WELDING SOCIETY and the American Society for Testing Materials. This will serve as a pattern for similar specifications for gas welding, and later on, for specifications covering other materials than plain carbon and low alloy steels.

Committee B-5—Subcommittee on Copper and Copper Alloys

The Committee has been very active during the year working on specifications on (*a*) silicon copper sheets, (*b*) leaded brass sheet and strip, (*c*) copper-nickel-zinc, (*d*) copper-nickel and (*e*) copper tubes and pipes for various purposes.

Association of American Railroads

Committee on Fusion Welded Tank-Car Tanks

Our representative has not been called upon during the year.

American Institute of Electrical Engineers

Subcommittee on Power Supply for Welding Operations

Its purpose is the cooperation and mutual education of electric resistance welding machine manufacturers, users of resistance welding equipment, and power companies, so that the problems involved in installing resistance welding equipment will be intelligently and satisfactorily solved. As a part of this work, the Committee is preparing a bulletin which is entitled, "Guide to Good Electrical Performance of Resistance Welding Machines." This bulletin will be of help to purchasers of resistance welding equipment, in that it will help them obtain machines of the best electrical characteristics, and it will also help them to cooperate with the power companies in the matter of providing satisfactory power supply.

National Board of Boiler and Pressure Vessel Inspectors

The Executive Committee of this organization, on which our representative holds membership, has held nine meetings during the past year which were usually concurrent with meetings of the A. S. M. E. Boiler Code Committee. In cooperation with the latter Committee the National Board takes an active interest in the administrative questions connected with boiler and pressure vessel legislation in the various states. The National Board devotes its major attention to the question of uniformity of application of the rules recommended by the Boiler Code Committee and, as a result of its activities, has extended the application of the rules to twenty-three of the states of the nation.

At the present time many problems are arising in the Boiler Code Committee in connection with the extension of the use of welding for both construction and repair purposes. There has been a tendency in some states to frown upon applications of fusion welding particularly for repair purposes, and this problem is being given concentrated attention. Marked advances have been made during the year in expanding the permission for such work and further improvements are looked for during the ensuing year.

American Transit Association

Way and Structures Committee No. 3 on Welding

Our representative is cooperating in the preparation of the A. T. A. Filler Metal Specifications.

American Society for Metals

The AMERICAN WELDING SOCIETY has a representative on the Recommended Practice Committee which is concerned with the preparation of a Handbook of the American Society for Metals. This Handbook was issued around the first of the year. Our representative assisted in the editing of various articles, and in the preparation of materials relating to stainless chrome irons and steels and chrome nickel steels.

American Petroleum Institute

Subcommittee on Field Welding of Casing

During the mid-year meeting in New Orleans four very good papers were presented at the group session, May 28th, as follows:

1. "Temperature Determinations in the Cooling Beads of Arc Welds in Steel Casing," by Mr. W. A. Bruce, Carter Oil Company.
2. "Critical-Temperature Ranges of Steel by the Dilatometer Method," by Mr. L. H. Winkler, Bethlehem Steel Company.
3. "The Weldability of Casing Steels," by Mr. S. L. Hoyt, A. O. Smith Corporation.
4. "Application of Critical Data in the Field Welding of Casing," by Mr. L. R. Hodell, Carter Oil Company.

Standing Committees

PUBLIC RELATIONS

Appointed April 20, 1932, to deal with all State and Federal Legislative matters involving welding which come to the attention of the Society, particularly those proposals which are detrimental to the best interests of the welding art.

MANUFACTURERS

Appointed September 8, 1930, to consider arrangements for future expositions; handle matters pertaining to exposition and look after welfare of welding exhibitors. These duties have been enlarged to include matters of general interest to the manufacturers.

CONVENTION

Appointed April 27, 1932, to correlate activities dealing with Fall Meeting and Exposition matters; composed of following: Chairman, Manufacturers Committee, representing exhibitors; Chairman and Secretary, Program Committee; Chairman and Secretary, Section in locality where meeting is held; National Secretary and Treasurer.

COMMITTEE ON OUTLINE OF WORK

The Committee on Outline of Work was appointed in 1938 with the broad purpose of coordinating the technical activities of the Society and for stimulating progress in such activities.

During the year the appointment of the following committees has been authorized by the Board of Directors. In each case appointment has been made upon recommendation of the Committee on Outline of Work. Personnel in some cases has already been approved while in others membership is not yet complete.

New Committees Organized:

Machinery Code.
Rules for Field Welding of Storage Tanks.

New Technical Committees Authorized and in Process of Organization:

Committee to Outline Minimum Requirements of Instructions for Welding Operators in Trade Schools.
Non-Destructive Tests.
Code for Welding of High Alloy Steel Engineering Structures.
Code for Nickel Alloy Structures.
Grouping of Materials According to Weldability.

Technical Committees Discharged:

Inspection Methods in Manufacture of U-69 and U-70 Pressure Vessels.
Oil Storage Tanks.

Technical Committees in Process of Reorganization:

Filler Metal Specifications Committee—It was proposed that the scope of the Filler Metal Specifications be defined so as to include non-ferrous and alloy steels.
Standard Qualification Procedure—Scope enlarged to include non-ferrous materials.
Standard Tests for Welds—Scope enlarged to include tolerances for gages for measuring butt and fillet welds.

New Representation on Committees of Other Societies:

American Standards Association—Standardization Preferred Voltages, 100 Volts and Under; Building Code Requirements for Iron and Steel.
American Institute of Electrical Engineers—Power Supply for Welding Operations.

BY-LAWS

During the past year the Committee on Revision of By-Laws has been concerned primarily in reviewing the By-Laws of new Sections, and revised editions of others.

It also approved of wording of revision of By-Law (Section VIII, Section 1-*j*) relating to personnel of Committee on Outline of Work. This amended By-Law has been submitted and now stands approved by the members of the Society.

The By-Laws Committee at the request of the Directors has drawn up a Section of the By-Laws to provide for the appointment of a Permanent Funds Committee.

PUBLICITY

Duties under general policies approved by the Board of Directors and subject to the limitation of the approved budget, the Publicity Committee shall

1. Be in charge of the publicizing activities of the Society and shall prepare publicity articles for release to the technical press and to newspapers in respect to the organizational activities, annual reports, national and

district meetings, awards of medals and prizes, technical programs and papers, technical activities including code, specification, standardization and research work, educational activities and publications of the Society.

2. It shall prepare circulars and posters for soliciting advertising in THE WELDING JOURNAL.

3. It shall in consultation with the Membership Committee prepare advertising in respect to promotion of membership.

4. It shall cooperate with officials in charge of national and district meetings and chairmen of all interested committees in carrying out its program.

EDUCATION

As the duties of this Committee are limited to stimulating and aiding the Sections in the preparation of lecture courses, and none of the Sections has asked for assistance during the past year the Committee has been inactive.

CODE OF PRINCIPLES OF CONDUCT

It is pleasing to report that it has been found unnecessary for this Committee to function during the past year.

OTHER STANDING COMMITTEES

The reports of other standing Committees such as *Membership, Finance* and *Meetings and Papers* are given elsewhere in this report.

The functions of the above-mentioned Committees and others such as *Executive, Nominating* and *Board of Directors* are outlined in the Constitution and By-Laws reproduced in this Year Book.

Special Committees

1939 WAYS AND MEANS

The Committee was appointed on January 12, 1939, for the purpose of analyzing the income of the Society from Membership Dues, Advertising and Contributions to Research Work in order that a more equitable and balanced distribution of income from the various companies affiliated with the Society, might be planned. A second objective was to lay plans for securing this additional income and carrying out these plans. The Committee has held five meetings.

One of the first steps was to compile a list of 185 pages which included the names of companies supporting the Society, the addresses, the present number of members and their classifications, and the income received from Advertising and from Research from each of these companies. Space was provided for filling in amounts of expected additional income from each of these sources. Subcommittees will be appointed in most of the principal sections to assist the 1939 Ways and Means Committee in carrying out its plans. Supplementary literature including a general statement on the work of the Society and the Research Committee was prepared, as was also a statement depicting the importance of welding in key industries. Necessary literature and publicity campaigns are also being planned to make the work most effective.

In order to divide the work and make the efforts of the various subcommittees most effective, specific industries will be selected for approach at the rate of one a month during the year 1940. Special literature indicating the importance of welding in each of these industries, together with the value of the work of the Society and the Welding Research Committee to each of these industries, will be depicted.

RETURNS TO SECTIONS ON MEMBERSHIP DUES

The Board of Directors, acting upon a petition signed by a number of Section representatives (at its meeting on Oct. 21, 1938), appointed a Committee to make a thorough study of Returns to Sections on membership dues.

This Committee, under the Chairmanship of Mr. McFarland, worked diligently over a period of nearly a year to discover some plan which would, in so far as possible, meet the following conflicting conditions:

1. Acceptance by most of the Sections.
2. Distribution of total income of Society in such a way as to provide for varied activities as Section Activities, Journal, Technical Activities, National Meetings and the like.
3. Distribution of total returns to Sections in such a way as to provide for the needs of the small, medium and large size Sections.
4. Workability from the bookkeeping viewpoint, and source of a minimum of misunderstandings between National and Sections Headquarters.

The difficulty of securing unanimity of opinion is attested by the fact that some seven plans were proposed of varying character.

At a meeting of Section representatives a plan was proposed whereby the Sections would receive an increase in returns temporarily until the matter could be given further careful study.

CONSOLIDATION OF MEMBERSHIP AND ELIMINATION OF INACTIVE SECTIONS

The Board of Directors, acting upon a Resolution adopted at the Section Officers' Conference, Oct. 21, 1938, appointed a Committee to make a study of this matter.

ESTABLISHMENT OF SINGLE EXPIRATION DATE OF MEMBERSHIP DUES

This Committee was appointed at the request of Section representatives at their meeting on October 21, 1938. The Committee has given the subject very careful study and will shortly offer definite recommendations.

Award of Medals

BOARD OF AWARD

Samuel Wylie Miller Medal. Established 1927, by S. W. Miller, to be awarded during his lifetime for any meritorious achievement which, in the judgment of the Board of Trustees of the Medal, has contributed conspicuously to the advancement of the art of gas fusion welding and cutting or the art of electric arc fusion welding and cutting. Annually available, $150. Individuals only, but of any country and of any age, shall be eligible for the award.

Mr. Miller died on February 3, 1929, and with his death the medal ceased to exist. The Society, desiring to perpetuate the medal as a memorial to Mr. Miller, at the 1929 Annual (April) Meeting by resolution established the Samuel Wylie Miller Memorial Medal. The award is made annually by the Board of Award for any meritorious achievement which, in the judgment of the Board of Award, has contributed conspicuously to the advancement of the art of welding and cutting.

RECIPIENTS OF MEDALS

SAMUEL WYLIE MILLER MEDAL

Awarded to
C. A. Adams (1927)
J. H. Edwards (1928)

SAMUEL WYLIE MILLER MEMORIAL MEDAL

Awarded to
J. W. Owens (1929)
E. H. Ewertz (1930)
F. P. McKibben (1931)
H. H. Moss (1932)
J. C. Lincoln (1933)
C. A. McCune (1934)
H. M. Hobart (1935)
H. S. Smith (1936)
J. W. Meadowcroft (1937)
C. J. Holslag (1938)

Lincoln Gold Medal—In December 1936, the Board of Directors gratefully accepted the generous offer of Mr. J. F. Lincoln, president of the Lincoln Electric Company to donate a medal to be given annually through the AMERICAN WELDING SOCIETY for the paper published in the AMERICAN WELDING SOCIETY JOURNAL and presented before a Section or National Meeting that has contributed most to the development and advancement of welding in the judgment of a committee appointed by the Society.

The following awards have been made:
1937—T. M. Jackson
1938—J. C. Hodge and C. R. Sadler
1939—G. O. Hoglund and G. S. Bernard, Jr.

I. REGULATIONS GOVERNING THE BOARD OF AWARDS

1. *Membership.*—The Board of Awards shall consist of three members of the AMERICAN WELDING SOCIETY. One member shall retire each year. The President of the Society shall appoint a member each year to serve from the first day of the month next following the first session of the annual meeting of the Society for a term of three years. The senior member of the Board shall be chairman of the Board of Awards.

2. *Duties.*—The Board of Awards each year shall select the persons to receive the awards made by the Society.

II. REGULATIONS GOVERNING THE AWARD OF THE SAMUEL WYLIE MILLER MEMORIAL MEDAL

1. *Donor.*—THE AMERICAN WELDING SOCIETY.

2. *Purpose.*—To commemorate the many unique contributions of Samuel Wylie Miller to the science and art of welding, his unselfish encouragement of others, his achievements and effective work in convincing engineers and the public that welding should take its rightful place as a recognized method of fabricating metals, and especially his important contributions to the formation and early success of the AMERICAN WELDING SOCIETY.

3. *Award.*—The medal shall be awarded each year to the person who in the judgment of the Board of Awards is most deserving for conspicuous contributions to the advancement of the welding or cutting of metals.

4. *Eligibles.*—Any person shall be eligible for this award whether or not a member of the AMERICAN WELDING SOCIETY.

5. *Presentation.*—The medal for each year together with a suitable certificate shall be presented at a session of the Annual Meeting of the AMERICAN WELDING SOCIETY next following the year for which the award is made. The presentation shall be made by the chairman of the Board of Awards or by a member of the Society designated by the chairman.

III. REGULATIONS GOVERNING THE AWARD OF THE LINCOLN GOLD MEDAL

1. *Donor.*—Mr. J. F. Lincoln, President, Lincoln Electric Co.

2. *Purpose.*—To encourage the presentation before the AMERICAN WELDING SOCIETY of papers which are effective in promoting the use of welding.

3. *Award.*—The medal shall be awarded each year to the author or authors of the paper which in the judgment of the Board of Awards is the greatest original contribution to the advancement and use of welding.

4. *Eligibles:*

(*a*) For the author or authors to be eligible for this award, the paper shall describe clearly original work done by them or under their supervision on welding in any of its aspects by any method or process.

(*b*) The paper shall be a full disclosure of the subject.

(*c*) The paper shall contain no statement which is unethical advertising or sales promotion. The paper may contain statements of fact including the names of either individuals or organizations of any kind, commercial designations, trade names, etc.

(*d*) Papers having more than three authors shall not be considered for this award.

(*e*) Any one may be an author of a paper considered for this award whether or not a member of the AMERICAN WELDING SOCIETY.

(*f*) For the author or authors to be eligible for this award, the paper shall have been published in an issue of THE WELDING JOURNAL during a twelve-month period ending with the July issue before the Annual Meeting, and also, the paper shall have been presented or scheduled for presentation either at a meeting of the

AMERICAN WELDING SOCIETY or a meeting of any division or section of the Society.

5. *Presentation.*—The medal, bearing on the reverse side the name of each author of the paper, together with a suitable certificate, shall be presented at a session of the Annual Meeting of the AMERICAN WELDING SOCIETY during the year for which the award is made by the Chairman of the Board of Awards or by a member of the Society designated by the Chairman. Each author of the paper for which the award is made shall receive a medal and a certificate and the medals and certificates shall be duplicates.

RULES TO GOVERN ORGANIZATION,

Functions and Operations of Committees

INCEPTION OF A COMMITTEE

The need for a technical committee or project may be brought to the Society's attention by any group or individual interested. Request for appointment of a technical committee should be accompanied by a definite statement as to the necessity, scope and purpose of the committee. The request should be accompanied by a historical survey of conditions leading up to the request and a statement of activities of other organizations in the field of activity to be covered. On receipt of this request, the Society office shall refer the matter to the Committee on Outline of Work, who shall ascertain, if possible, (1) If there is real need for a project or committee of this sort, (2) If the cooperation of all interested parties can be secured, (3) Whether the committee should be organized as a committee of the AMERICAN WELDING SOCIETY or jointly with one or more other organizations and (4) Any unusual expense. If found desirable the Committee on Outline of Work shall recommend to the Executive Committee of the Board of Directors of the Society the appointment of such a committee, define the scope of its activity and recommend personnel or interests which should be included in the committee.

COMMITTEE PERSONNEL

The chairman of a committee will be selected for executive capacity and knowledge of the work involved. He will be consulted in the selection of personnel of the committee. Committee membership will be held as small as practicable, consistent with representation of all interests involved.

APPOINTMENTS

Although committee appointments in accordance with the By-Laws are for a term of one year, it is usually desirable in technical committees for members to be reappointed until the task is completed. Committee personnel is reconsidered at the end of each year and an opportunity is thus provided to leave off members who no longer are interested or who are unable to serve. Notification of appointment will be sent out by the Society's Secretary, with copy to the President and to the Chairman of the Committee on Outline of Work.

Additions or changes in Committee personnel shall be made by the President with the advice of the chairman of the committee in question and of the Committee on Outline of Work. Proposal for such changes or additions may come from other sources. Such proposals will be referred to the chairman of the committee in question and the Committee on Outline of Work before action by the President.

ORGANIZATION

Committees may elect a Vice-Chairman and a Secretary.

The chairman of a committee may appoint without further approval such subcommittees as he deems necessary. Membership of subcommittees need not be confined to members of the committee. The chairman of a subcommittee shall be a member of the committee. The chairman of the main Committee shall be ex-officio a member of all subcommittees.

The names of officers, subcommittees and personnel of subcommittees shall be reported in duplicate to the Secretary of the Society, one copy of which shall be forwarded to the Chairman of the Committee on Outline of Work.

Procedure of and reports to be furnished by subcommittees will be prescribed by the committee chairman.

COMMITTEE MEETINGS

Committee meetings will be held at the call of the chairman, usually at such time and place as suits the convenience of the majority of the members. In order that a committee shall retain an "active" status, it is desirable for the committee to hold at least two meetings during any calendar year.

Subcommittees will meet at the call of the chairman thereof.

REPORTS OF PROGRESS

The Secretary of the Society shall be regularly informed of current activities of committees. There shall be forwarded copies of notices of meetings and minutes of meetings, including reports of subcommittees.

APPROVAL OF REPORTS

The work of the committee shall be presented, in duplicate, to the Society in the form of Reports. One copy of the report shall be forwarded to the Chairman of the Committee on Outline of Work to determine whether this report covers the scope of work intended and whether the report has been properly correlated with the work or reports of other committees. The Chairman of Committee on Outline of Work will forward the report with recommendations to the Secretary of the Society for the action of the Board of Directors.* The Chairman of a committee, in presenting his report, shall furnish a written statement as to the procedure used in correlating his report with the work of interested committees of the AMERICAN WELDING SOCIETY, and of other technical societies or committees. He shall also report the results of letter ballot or other votes of his committee on his report. In case of negative votes, the report should include, if practicable, a statement of reasons for unfavorable votes and the opinion of the committee as to the validity of the objections raised.

It is desirable and committee chairmen will have the privilege of personally presenting their reports to the Committee on Outline of Work and to the Board of Directors. Arrangements for personal presentation may be made through the Secretary of the Society.

All reports bearing the name of the AMERICAN WELDING SOCIETY must be approved by the Executive Committee of the Board of Directors, subject to confirmation by letter ballot or at a regular meeting of the Board of Directors. If objections, other than editorial, are raised by three or more members of the Board, the report shall be reconsidered at a meeting of the Board.

After approval of reports by the Board of Directors they shall be published in the Society JOURNAL or in pamphlet form for the membership of the Society or as specially provided by the Executive Committee.

COOPERATION WITH OTHER COMMITTEES

It is desirable that the reports and findings of all committees be in harmony with each other. Chairmen of committees involving general reports, such as, Filler Metal Specifications, Symbols, Qualification of Welding Operators, Standard Tests for Welds and the like, shall keep other committee chairmen informed of the progress of the work of their committee. Conflicts in interest should be promptly brought to the attention of the Committee on Outline of Work.

* The Committee on Outline of Work shall have no jurisdiction over the technical features of Committee reports.

COOPERATION WITH OTHER SOCIETIES

It is the desire that the AMERICAN WELDING SOCIETY shall be the originator of all codes, standards and specifications involving welding. Nevertheless, on occasions it will be advisable to have such codes, standards and specifications issued jointly with other organizations. Joint committees shall fully respect the rights of all the participating organizations.

REVIEW OF COMMITTEE OPERATIONS

On or before September 1st each technical committee of the Society shall prepare a brief report of progress during the year and plans for future activities which shall be submitted in duplicate, one for the Secretary to assist in the compilation of the Annual Report and the other for the Committee on Outline of Work. This report shall indicate desirable changes in personnel. It shall be the duty of committee chairmen to call attention to the Committee on Outline of Work the lack of attendance at committee meetings or lack of interest on the part of personnel.

The Committee on Outline of Work should also be furnished by committee chairmen with copy of their proposed program of activities.

DISCHARGE OF COMMITTEES

Upon completion of its work on recommendation of the Committee on Outline of Work, a technical committee shall be discharged, unless new program of activities is contemplated.

FUNDAMENTALS OF WELDING METALLURGY

How many times have you wanted to obtain a very brief textbook on the A, B, C of Metallurgy which would enable you to understand articles on Metallurgy and to appreciate the metallurgical problems involved in welding? Recognizing the need for material of this sort, the New York Section has sponsored a series of ten lectures to familiarize members of the welding industry with the composition and structures of the metals commonly used, to show the effect of varied conditions of heat and stress in welding, to explain heat treatment and to illustrate how the knowledge of metallurgy can be used to control the welding processes.

In order to make these lectures generally available, the Society will reproduce them serially starting with the January issue. A complete bulletin of 46 pages is also available for sale at a dollar per copy. Special prices can be arranged for quantity lots of 10 or more. These lectures have been prepared by Professor Otto H. Henry of Polytechnic Institute of Brooklyn and Dr. G. E. Claussen of the same University, who is also connected with the Welding Research Committee. Inasmuch as a limited supply is available orders should be placed at once.

OFFICERS and COMMITTEES

American Welding Society 1939–40

BOARD OF DIRECTORS
1939-1940

President—G. T. HORTON, President, Chicago Bridge & Iron Co., 1305 W. 105th St., Chicago, Illinois

First Vice-President—T. C. SMITH, General Petroleum Corp., 2525 East 37th St., Los Angeles, Calif.

Second Vice-President—L. S. MOISSEIFF, Consulting Engineer, 99 Wall St., New York, N. Y.

District Vice-Presidents:
New York-New England District—W. F. HESS, Rensselaer Polytechnic Institute, Troy, N. Y.
Middle Eastern District—L. C. BIBBER, Carnegie-Illinois Steel Corp., Pittsburgh, Pa.
Middle Western District—K. L. HANSEN, Harnischfeger Corp., Milwaukee, Wis.
Southern District—A. J. MOSES, Combustion Engineering Co., Chattanooga, Tenn.
Pacific Coast District—L. W. DELHI, Western Pipe & Steel Co., San Francisco, Cal.

Treasurer—C. A. MCCUNE, Magnaflux Corporation, 25 W. 43rd St., New York, N. Y.

DIRECTORS-AT-LARGE

Term expires 1940

A. M. Candy, Hollup Corp., 3357 W. 47th Place, Chicago, Ill.
J. H. Deppeler, Metal & Thermit Corp., 120 Broadway, New York, N. Y.
E. R. Fish, The Hartford Steam Boiler Insp. & Ins. Co., Hartford, Conn.
A. E. Gaynor, J. A. Roebling Sons Co., 19 Rector St., New York, N. Y.
L. S. Moisseiff, Consulting Engineer, 99 Wall St., New York, N. Y.
*A. E. Gibson, Wellman Engineering Co., 7000 Central Ave., Cleveland, Ohio

Term expires 1941

C. A. Adams, E. G. Budd Mfg. Co., 25th & Hunting Park Ave., Philadelphia, Pa.
Marvin Cook, Humble Oil Company, Houston, Texas
H. M. Priest, U. S. Steel Corp., Railroad Research Bureau, 612 Frick Bldg., Pittsburgh, Pa.
C. E. Woodman, National Ref. Co., 27 Greystone Ave., Kansas City, Missouri
*P. G. Lang, Jr., Baltimore & Ohio R. R. Co., Baltimore, Md.

Term expires 1942

E. V. David, Air Reduction Sales Co., 60 E. 42nd St., New York, N. Y.
J. D. Gordon, Taylor-Winfield Corp., Warren, Ohio
G. F. Jenks, Ordnance Dept., United States Army, Washington, D. C.
H. S. Smith, Union Carbide Co., 30 E. 42nd St., New York, N. Y.
*H. C. Boardman, Chicago Bridge & Iron Co., 1305 W. 105th St., Chicago, Ill.

OTHER OFFICERS OF THE SOCIETY

Secretary and Assistant Treasurer—M. M. KELLY, 33 West 39th St., New York, N. Y.

Technical Secretary and Editor—W. SPRAGUE.

* Junior Past-Presidents.

PAST-PRESIDENTS OF AMERICAN WELDING SOCIETY

C. A. Adams (1919–20)
J. H. Deppeler (1920–21)
*S. W. Miller (1921–22)
C. A. McCune (1922–23)
*T. F. Barton (1923–24)
E. H. Ewerts (1924–25)
A. G. Oehler (1925–26)
F. M. Farmer (1926–28)
F. T. Llewellyn (1928–30)
E. A. Doyle (1930–32)
*F. P. McKibben (1932–34)
D. S. Jacobus (1934–35)
J. J. Crowe (1935–36)
A. E. Gibson (1936–37)
P. G. Lang, Jr. (1937–38)
H. C. Boardman (1938–39)

* Deceased.

Standing Committees of the American Welding Society

Executive Committee:

G. T. Horton, *Chairman*
C. A. Adams
David Arnott
H. C. Boardman
E. V. David
A. F. Davis
J. H. Deppeler
R. S. Donald
E. R. Fish
H. O. Hill
D. S. Jacobus
G. F. Jenks
O. B. J. Fraser
C. A. McCune
L. S. Moisseiff
W. W. Reddie
H. S. Smith
T. C. Smith
A. C. Weigel

Finance Committee:

C. A. McCune, *Chairman*
H. C. Boardman
M. M. Kelly, *Asst. Treasurer*
A. E. Gaynor
L. S. Moisseiff

Membership Committee:

A. F. Davis, *Chairman*

Section	*Chairman, Mem. Comm.*	*Prestige Man*
Birmingham	E. W. Braun	W. P. McCutcheon
Boston	G. R. Singleton	Henry F. Gray
Canton	R. M. Wallace	
Chattanooga	E. C. Chapman	B. L. Sylar
Chicago	R. L. Kohlbry	A. M. Candy
Cincinnati	W. P. Dettwiler	W. W. Petry
Cleveland	J. Carbis, J. T. Wolfenden, A. A. Markee	A. E. Gibson
Colorado	J. H. Johnson	Horace Jackson
Columbus	Geo. F. Herren	F. C. Caldwell
Connecticut	Frank W. Taft	E. R. Fish
Detroit	L. H. Frost	
Georgia	E. Guillot	
Hawaii	H. P. Ryan	
Indiana	Paul Grubbs	Alex Spink
Kansas City	Ned Ashton	J. A. Hall
Los Angeles	H. L. Newby	
Louisiana	H. J. Earle	Mace H. Bell
Maryland	J. W. Miller	
Memphis	R. R. Aschinger	Ed. Gill

Section	*Chairman, Mem. Comm.*	*Prestige Man*
Milwaukee	John Pawlowski	
Montana	Wm. Galloway	
New York	G. N. Bull	
Northern N. Y.	E. R. Spittler	
Western N. Y.		
Northwest	J. H. Barron C. M. Akins	
Oklahoma City	Francis Sprehe	J. M. Krisman
Omaha	George Thrapp	L. O. Schneiderwind
Peoria	W. I. Miskoe	G. R. Roberts
Philadelphia	J. J. Dunne	W. W. Barnes
Pittsburgh	A. J. Deacon	J. F. Minnotte
Puget Sound	E. F. Flohr	
Quad-Cities	W. J. Foley	
Rochester	Robert Cattanach	
St. Louis	A. A. Allina	
San Francisco	H. E. Constanz	E. L. Mathy
San Joaquin	R. Rutherford	
South Texas	Chas. W. Cryer	B. W. Farquhar
Tulsa	R. M. Thames	T. M. Heggie
Washington	C. E. Meissner	W. F. Dietz
Wichita	J. H. Dunham	
Youngstown	Robert Haas	C. A. Wills
York		
x Gr. Rapids	R. D. Layman	
x Syracuse	H. P. Bentley	
x Dallas	W. B. Van Wart C. K. Rickel	
x Toledo	C. H. Sherer	

x Sections in process of organization.

Committee on Admission to Membership:

F. E. Rogers, *Chairman*
A. W. Carpenter
J. L. Edwards
Chas. Kandel
S. S. Scott

Committee on Meetings and Papers:

E. Vom Steeg, *Chairman*
W. Spraragen, *Secretary*
C. A. Adams
G. N. Bull
A. M. Candy
E. V. David
E. J. Del Vecchio
R. S. Donald
T. C. Fetherston
Milton Male
A. G. Oehler
R. E. Powell
H. L. Rogers
J. W. Sheffer
M. L. Smith
G. Van Alstyne
J. L. Wilson

Subcommittee on Program:

E. V. David, *Chairman*
W. Spraragen, *Secretary*
G. N. Bull
A. M. Candy
E. J. Del Vecchio
R. S. Donald
T. C. Fetherston
R. E. Powell
H. L. Rogers
J. W. Sheffer
J. L. Wilson

Subcommittee on Publications:

A. G. Oehler, *Chairman*
W. Spraragen, *Secretary*
C. A. Adams
R. S. Donald
T. C. Fetherston
M. L. Smith
G. Van Alstyne

Committee on Revision of By-Laws:

E. M. T. Ryder, *Chairman*
J. J. Crowe
O. E. Hovey

Committee on Code of Principles of Conduct:

E. R. Fish, *Chairman*
E. H. Ewertz
F. M. Farmer
O. E. Hovey
D. S. Jacobus

Committee on Awards:

L. W. Clark, *Chairman*
C. H. Jennings
D. S. Jacobus

Committee on Outline of Work:

L. S. Moisseiff, *Chairman*
W. Spraragen, *Secretary*
H. C. Boardman
E. R. Fish
C. W. Obert
T. C. Smith
I. B. Yates
A. C. Weigel

Educational Committee:

A. M. Candy, *Chairman*
A. G. Bissell
Chas. Bowen
S. S. Scott
J. A. Warfel

Public Relations Committee:

H. S. Smith, *Chairman*
H. F. Reinhardt, *Vice-Chairman*
J. I. Banash
J. H. Deppeler
D. S. Jacobus
L. D. Meeker

Publicity Committee:

M. L. Smith, *Chairman*
L. P. Aurbach
A. F. Davis
Geo. Sykes
W. V. Merrihue
R. S. Kenrick
Ed. Logelin
M. E. Smith
J. H. Thomson
G. Van Alstyne

Manufacturers Committee:

J. B. Tinnon, *Chairman*
W. B. Baker
W. H. Bleecker
W. P. Burglund
Fred Connell
A. F. Davis
A. E. Gaynor
K. L. Hansen
O. L. Howland
L. D. Meeker
W. W. Reddie
G. Van Alstyne

Convention Committee:

C. A. McCune, *Chairman*
M. M. Kelly, *Secretary*
J. B. Tinnon, *Chairman*, Manufacturers Committee
M. L. Smith, *Chairman*, Publicity Committee
E. V. David, *Chairman*, Programs Committee
W. Spraragen, *Secretary*, Programs Committee
Chairman and Secretary, Local Section where meeting is held.

Technical Committees of the American Welding Society

Conference Committee on Welded Bridges:

A. R. Wilson, *Chairman*
W. Spraragen, *Secretary*
D. O. Cargill
A. W. Carpenter
G. D. Fish
F. H. Frankland
A. L. Gemeny
Wm. G. Grove
LaMotte Grover
O. L. Grover
S. C. Hollister
Walter Hopkins
O. E. Hovey
J. Jones
M. P. Korn
P. G. Lang, Jr.
C. E. Loos
L. S. Moisseiff
N. W. Morgan
H. H. Moss
Andrew Vogel
J. L. Vogel

Subcommittee on Materials:

J. Jones, *Chairman*
W. Spraragen
F. H. Frankland
H. H. Moss
A. R. Wilson

Subcommittee on Workmanship & Welding Technique:

A. W. Carpenter, *Chairman*
O. L. Grover
LaMotte Grover
Jonathan Jones
C. E. Loos
N. W. Morgan
H. H. Moss

Subcommittee on Fatigue & Impact:

A. Vogel, *Chairman*
A. L. Gemeny
Jonathan Jones

Correlating Committee:

Jonathan Jones, *Chairman*
L. S. Moisseiff
W. Spraragen

Editorial Committee:

N. W. Morgan, *Chairman*
O. E. Hovey
H. H. Moss

Committee on Standard Tests for Welds:

M. F. Sayre, *Chairman*
F. Eder
Otto Henry
J. R. Stitt
C. M. Underwood
H. L. Whittemore
Officer in Charge, Standards Branch Shipbldg. Div., Bureaus of Engrg. & C. & R., Navy Dept.

Subcommittee on Shear Tests:

F. Eder, *Chairman*
R. W. Clark
E. L. Durkee
Milton Male

Subcommittee on Nick Break and Gravity:

Otto Henry, *Chairman*

Subcommittee on Bend Tests:

J. R. Stitt, *Chairman*

Subcommittee on Tensile Tests:

H. L. Whittemore, *Chairman*

Committee on Definitions and Chart:

R. W. Clark, *Chairman*
L. C. Bibber
J. H. Deppeler
Leo Edelson
G. Mikhalapov
N. W. Morgan
C. W. Obert
F. C. Saacke
C. M. Underwood
Officers in Charge, Standards Branch Shipbldg. Div., Bureaus of Engrg. & C. & R., Navy Dept.

Committee on Large Welded Pipe for Hydraulic Purposes:

Wm. F. Durand, *Chairman*
W. J. Crook
H. L. Doolittle
Robert Glover
S. C. Hollister
K. V. King

Committee on Pressure Piping:

W. D. Halsey, *Chairman*
A. B. Bagsar
G. O. Carter
R. W. Clark
D. H. Corey
J. J. Crowe
F. C. Fantz
T. W. Greene
Design Manager, Bureau of Yards & Docks, Navy Dept.
L. R. Hodell
A. M. Houser
D. S. Jacobus
A. Kidd
N. L. Mochel
B. N. Osmin
A. L. Penniman, Jr.
B. Robinoff
J. H. Williams

Committee on Standard Qualification Procedure:

W. D. Halsey, *Chairman*
P. R. Cassidy
R. F. Cavanagh
E. C. Chapman
R. W. Clark
F. Eder
F. C. Fantz
H. O. Hill
C. W. Obert
Officers in Charge, Standards Branch Shipbldg. Div., Bureaus of Engrg. & C. & R., Navy Dept.

Committee on Welding in Marine Construction:

David Arnott, *Chairman*
C. W. Bryan, *Secretary*
L. C. Bibber
C. W. Obert, Sr.
J. W. Owens
H. W. Pierce
R. H. Cunningham
E. D. Debes
J. H. Deppeler
E. R. Fish
T. M. Jackson
D. S. Jacobus
B. E. Meurk
H. C. E. Meyer
H. W. Northcutt
E. C. Rechtin
S. S. Scott
F. M. Scotten
Officers in Charge, Standards Branch Shipbldg. Div., Bureaus of Engrg. & C. & R., Navy Dept.
Major H. B. Vaughan, Jr.
J. W. Wilson
J. M. Wood
H. A. Woofter

Subcommittee on Hull Construction:

C. W. Bryan, *Chairman*
D. Arnott
Officer in Charge, Standards Branch Shipbldg. Div., Bureaus of Engrg. & C. & R., Navy Dept.
R. H. Cunningham
L. C. Bibber
J. H. Deppeler
T. M. Jackson
H. W. Pierce
E. C. Rechtin

Subcommittee on Marine Boilers, Pressure Vessels and Piping:

D. S. Jacobus, *Chairman*
D. Arnott
C. W. Bryan
E. R. Fish
T. M. Jackson
B. E. Meurk
H. C. E. Meyer
C. W. Obert
S. S. Scott
J. W. Wilson
J. M. Wood
H. A. Woofter
Officer in Charge, Standards Branch Shipbldg. Div., Bureaus of Engrg. & C. & R., Navy Dept.

Subcommittee on Thermal Stresses:

H. W. Pierce, *Chairman*
D. Arnott
L. C. Bibber
C. W. Bryan
R. H. Cunningham
E. D. Debes
T. M. Jackson

Filler Metal Specifications Committee:

J. H. Deppeler, *Chairman*
L. C. Bibber
V. W. Bihlman
W. H. Bleecker
H. S. Blumberg
J. J. Chyle
W. T. Cramer
J. H. Critchett
F. H. Frankland
O. B. J. Fraser
J. R. Freeman, Jr.
C. F. Gailor
LaMotte Grover
H. O. Hill
G. O. Hoglund
J. H. Humberstone
G. F. Jenks
R. S. Johnston
H. E. Landis
J. F. Lincoln
C. A. McCune
N. L. Mochel
E. M. T. Ryder
J. W. Sheffer
E. W. P. Smith
E. H. Turnock
C. M. Underwood
C. R. Vincent, Jr.
G. L. Willins
Officer in Charge, Standards Branch Shipbldg. Div., Bureaus of Engrg. & C. & R., Navy Dept.

Committee on Symbols:

L. C. Bibber, *Chairman*
W. A. Bischoff
A. M. Candy
R. W. Clark
H. O. Hill
C. H. Jennings
A. B. S. Kvall (Alternate)
G. Mikhalapov
C. W. Obert
H. M. Priest
Officer in Charge, Standards Branch Shipbldg. Div., Bureaus of Engrg. & C. & R., Navy Dept.

Committee on Rules for Welding of Gravity Tanks, Tank Risers and Towers:

C. W. Obert, *Chairman*
J. J. Crowe, H. S. Smith } *I. A. A. Representatives*
C. J. Holslag, E. Vom Steeg } *A. W. S. Representatives*
H. A. Sweet, E. W. Fowler } *Members at Large*
Design Manager, Bureau of Yards & Docks, Navy Dept.

Committee on Welded Elevated Water Tanks:

L. R. Howson, *Chairman*
Geo. Horton
H. O. Hill
J. O. Jackson
C. W. Obert
H. A. Sweet
J. P. Schwada

Machinery Code Committee:

J. D. Gordon, *Chairman*
Everett Chapman
R. W. Clark
J. H. Cooper
W. E. Crawford
Ralph Diserens
John Kincaid
G. F. Jenks
C. H. Jennings
Officer in Charge, Standards Branch Shipbldg. Div., Bureaus of Engrg. & C. & R., Navy Dept.
Wm. Maddux
H. J. Stein
C. M. Underwood

Committee on Rules for Field Welding of Storage Tanks:

H. O. Hill, *Chairman*
H. C. Boardman
E. W. Fowler
J. O. Jackson
C. W. Obert, Sr.
Walter Samans
H. A. Sweet

A. P. I.-A. W. S. Conference Committee on Welded Oil Storage Tanks:

H. O. Hill, *Chairman*
H. C. Boardman
C. W. Obert, Sr.
G. Raymond
M. V. Reed

Committee on Minimum Requirements of Instructions for Welding Operators in Trade Schools:

A. R. Wrigley, *Chairman*
John Adams
H. L. Allen
H. L. Amonette
G. H. Fern
J. M. Hall
F. J. Morton
G. R. Singleton

Committee on Safety Recommendations:

H. S. Smith, *Chairman*
J. I. Banash
J. H. Deppeler

Subcommittee on Recommended Procedure for Welding or Cutting Containers That Have Held Combustibles:

H. F. Reinhard, *Chairman*
F. P. Gross, *Secretary*
S. S. Adelson
R. S. Bonsib
E. W. Fowler
A. F. Keogh
D. E. Roberts
E. Vom Steeg

Committee on Grouping of Materials According to Weldability:

C. H. Jennings, *Chairman*
L. C. Bibber
A. G. Bissell
J. H. Deppeler
W. D. Halsey
N. L. Mochel
H. Pierce
J. F. Randall
Chas. Schenck
W. G. Theisinger

Committee on Building Codes:

(Personnel to be named)

Committee on Non-Destructive Tests:

(Personnel to be named)

Committee on Code for Welding of High Alloy Steel Engineering Structures:

(Personnel to be named)

Committee on Code for Nickel Alloy Structures:

O. B. J. Fraser, *Chairman*
(Personnel to be named)

A. W. S.-A. S. M. E. Conference Committee:

C. W. Obert Sr., *Chairman*
C. A. Adams
H. C. Boardman
Walter Samans
A. C. Weigel

Personnel Special Committees

1939 Ways and Means Committee:

C. A. Adams, *Chairman*
W. Spraragen, *Acting Secretary*
R. F. Cavanagh
Everett Chapman
A. F. Davis
J. H. Deppeler
C. A. McCune
H. L. Rogers
H. S. Smith
E. Vom Steeg

Committee to Study Establishment of a Single Expiration Date of Membership Dues:

C. A. McCune, *Chairman*
E. R. Fish
Ed. G. Hosted
M. M. Kelly
K. V. King
F. H. Miller
A. L. Pfeil

Committee to Study Returns to Sections on Membership Dues and Report Back to the Executive Committee Its Findings and Recommendations:

R. E. McFarland, *Chairman*
E. E. Isgren
G. F. Jenks
C. A. McCune
O. D. Rickly
E. T. Scott

Special Committee on Consolidation of Membership and Elimination of Inactive Sections:

J. J. Crowe, *Chairman*
C. A. Adams
A. F. Davis
J. H. Deppeler
P. G. Lang, Jr.
C. A. McCune

Representatives of American Welding Society on Other Societies

American Institute of Electrical Engineers:

Committee on Electric Welding—Subcommittee on Power Supply for Welding Operations:

W. F. Hess
G. Mikhalapov

American Standards Association:

Sectional Committee A-10—Safety Code for Construction Work:

A. N. Kugler

Sectional Committee A-57—Building Code Requirements for Iron and Steel:

M. Male
E. A. Doyle (Alternate)

Sectional Committee B-5—Standards for Small Tools and Machine Tool Elements:

E. A. Mallett

Sectional Committee B-31—Code for Pressure Piping:

R. W. Clark
D. H. Corey
J. J. Crowe
F. C. Fantz

Sectional Committee C-42—Electrical Definitions:

R. W. Clark

Sectional Committee C-52—Electric Welding:
J. Blankenbuehler — W. Spraragen, E. Vom Steeg } Alternates

Sectional Committee Z-2—Protection of Heads, Eyes, etc. of Industrial Workers:
H. S. Smith — H. F. Reinhard (Alternate)

Sectional Committee Z-5—Ventilation Code:
C. W. Obert, Sr. — E. Vom Steeg (Alternate)

Sectional Committee Z-14—Drawings and Drafting Room Practice:
L. C. Bibber

Sectional Committee Z-28—Work in Compressed Air:
C. W. Obert, Sr.

Sectional Committee Z-32—Graphical Symbols and Abbreviations for Use on Drawings:
L. C. Bibber
Sub-Group on Welding Symbols—Identical to A. W. S. Committee on Symbols:

American Society for Metals:
Recommended Practice Committee:
H. L. Maxwell, assisted by—Advisory Committee:
Composed of A. J. Moses
H. E. Rockefeller
C. H. Jennings

American Society for Testing Materials:
Subcommittee B-5—on Copper & Copper Alloys:
I. T. Hook

Committee A-1 on Steel & Subcommittee XXI on Welding Wire:
J. H. Deppeler, *Chairman* — Anson Hayes
J. J. Crowe — J. H. Humberstone
A. E. Gaynor — C. A. McCune

American Transit Association:
Way and Structures Committee No. 3—Welding:
J. H. Deppeler

Association of American Railroads:
Committee on Fusion Welded Tank Car Tanks:
J. J. Crowe

American Petroleum Institute:
Field Welding of Casing:
P. D. McElfish — A. E. Marble (Alternate)
J. C. Hodge

National Board of Boiler and Pressure Vessel Inspectors:
C. W. Obert, Sr.

MAP SHOWING GEOGRAPHICAL DIVISIONS AMERICAN WELDING SOCIETY SECTIONS ESTABLISHED, IN PROCESS, AND CONTEMPLATED

Note: Hawaii Section Not Shown on Map

CATALOGUE OF MEMBERSHIP

NOVEMBER 22, 1939

Honorary Members—Comfort A. Adams and F. T. Llewellyn

List of Members of All Grades—Arranged Alphabetically

EXPLANATORY NOTE—The following alphabetical list contains the names of members of all grades. The letter in parentheses immediately after each name indicates the class to which the member belongs. The following letters have been used to classify the different grades of membership: A—Sustaining Members; B—Members; C—Associate Members; D—Operating Members; E—Honorary Members; F—Student Members. Where members desire to have their mail sent to their residence the home address is given and is preceded by (Res.). A list of members showing the geographical distribution follows the alphabetical list.

A

Abanuza, A. E. (C), Civil Engineer, Dixie Machine Welding & Metal Works, 1031 Annunciation St., New Orleans, La.

Abbe, T. W. (C), Petroleum Engineer, Sun Oil Co., Box 2657, Houston, Texas.

Abbott, H. W. (C), Director of Lab., Speer Carbon Co., St. Marys, Pa.

Abbott, James A. (C), Salesman, Taylor-Winfield Corp.; (Res.) 11909 Buckingham Ave., Cleveland, Ohio.

Abbott, Lester E. (B), Member Technical Staff, Bell Telephone Labs. Inc.; (Res.) 82 Jackson Road, Valley Stream, Long Island, N. Y.

Aborn, R. H. (C), Asst. Metallurgist, U. S. Steel Corp. Research Lab., Kearny, N. J.

Acker, L. E. (B), Bridge Engineer, J. B. Klein Iron & Fdy. Co., Oklahoma City, Okla.

Adams, C. A. (E), Consulting Engineer, The Edward G. Budd Mfg Co., 25th & Hunting Park Ave., Philadelphia, Pa.

Adams, C. H. (C), Salesman, Air Reduction Sales Co.; (Res.) 125 Edgewood Road, Towson, Md.

Adams, Chas. O. (C), Welding Engineer, Delco Products, Div. of G. M.; (Res.) 615 W. Fairview Ave., Dayton, Ohio.

Adams, David (B), Foreman, Hurley Machine Shop; (Res.) 3115 Grand Blvd., Brookfield, Ill.

Adams, Ernest N. (C), District Engineer, American Inst. of Steel Construction; (Res.) 192 Chandler St., Worcester, Mass.

Adams, Gale (B), Supervisor of Welding, Goodman Mfg. Co., 4834 So. Halsted St., Chicago, Ill.

Adams, John (C), Welding Instructor, New York Trade School, 304 East 67th Street, New York, N. Y.

Adams, Julien W. (C), Consulting Engineer, Goslin-Birmingham Mfg., Co.; (Res.) 209 North 52nd St., Birmingham, Alabama.

Adcock, A. A. (B), Supt., Vulcan Steel Tank Corp., Box No. 1844, Tulsa, Okla.

Addis, Wm. H. (B), General Foreman, Cameron Iron Works Inc., P. O. Box 1212, Houston, Texas.

Addison, James (B), Welding Engineer, Edward Wood & Co., Ltd., Ocean Ironworks, Manchester 17, England.

Adelson, J. S. (C), Chief Metallurgist, Steel and Tubes, Incorporated, 224 East 131st Street, Cleveland, Ohio.

Adelson, S. S. (C), Sales Dept., Wilson Welder & Metals Co. Inc.; (Res) 4663 188th St., Flushing, N. Y.

Adler, Robert (C), Partner, Adler Steel Products Company, 310 Thorpe Building, Minneapolis, Minnesota.

Agusto, John (D), 163 Atkinson St., Rochester, N. Y.

Ahlston, A. C. (C), Chief Mech. Engr., Victorian Railways, Spencer St., Melbourne C 1, Australia.

Ahlstrom, Walter (C), General Foreman, Commonwealth Edison Co., 2233 S. Throop St., Chicago, Illinois.

Akey, R. P. (B), Supt. Hobbs Mfg. Co., Box 1568, Fort Worth, Texas.

Akins, Clifford M., (B), Vice-President, Marquette Manufacturing Co., Inc., Minneapolis, Minnesota.

Albans, Thos. A. (D), Electric Welder, Tide Water Assoc. Oil Co.; (Res.) 42 Wall St., Cranford, N. J.

Aldrich, Robert H. (C), Design Engineer, Allen Elect. & Equip. Co., Kalamazoo, Mich.

Aldrich, William (B), Representative, Metal & Thermit Corp., South San Francisco, Calif.

Alexander, Chas. E. (C), Owner & Manager, Alexander Welding Co., 461 E. Main St., Patchogue, N. Y.

Alexander, G. W. (D), Welder, U. S. Engineers, 4510 Cutter St., Los Angeles, Calif.

Alexander, James F. (C), Supt., The Wheland Co.; (Res.) 813 Barton Ave., Chattanooga, Tenn.

Alexander, W. E. (D), Welder, Lone Star Cement Co., (Res.) R. R. 4, Greencastle, Ind.

Alhart, C. G. (D), Welding Superviser, Texas Iron Works; (Res.) 724 W. Cottage Ave., Houston, Texas.

Alison, R. G. (B), Sales Engineer, Lincoln Elec. Co. of Canada, Ltd., 65 Bellwoods Ave., Toronto, Ont., Canada.

Allan, Edwin (B), President, Allan Iron & Welding Wks., 133 Murray St., Rochester, N. Y.

Allen, Ben H. (C), Inspector, Southwestern Laboratories; (Res.) Rt. 6, Box 312, Houston, Texas.

Allen, Ben. L. (D), Maintenance Welder, National Mall. & Steel Casting Co.; (Res.) 203 White Ave., Sharon, Pa.

Allen, Charles W. (C), Fabrication & Welding Supervisor, S. Morgan Smith Co.; (Res.) 221 S. Albemarle St., York, Pa.

Allen, Clayton M. (C), Construction Engineer, Los Angeles Bureau of Power & Light, P. O. Box 240, Arcade Annex, Los Angeles, Calif.

Allen, Frank M. (C), President, Steel Engineers Co., Box 784, Salt Lake City, Utah.

Allen, Franklin T. (D), Flame Cutter, Fairbanks Morse & Co.; (Res.) 644 Park Ave., Beloit, Wis.

Allen, Guy F. (B), Structural Engineer, Detroit Edison Co.; (Res.) 220 California Ave., Royal Oak, Mich.

Allen, Herschel H. (C), Consulting Engineer, J. E. Greiner Co., 1201 St. Paul St., Baltimore, Md.

Allen, Leslie (B), Manager of Works, Bethlehem Steel Co., Oliver Building, Pittsburgh, Pa.

Allen, Terrance W. (C), Factory Manager, Taylor-Winfield Corp., 15120 Woodward Ave., Detroit, Mich.

Allina, Alfred (C), President Alpha Tank & Sheet Metal Mfg. Co., 5001 S. 38th St., St. Louis, Mo.

Allison, Edgar T. (C), Welder, Sinclair Ref. Co., Pipe Line Dept., Carrollton, Mo.

Allison, Joseph W. (B), Welding Sales Supervisor, Westinghouse Electric & Mfg. Co., 3001 Walnut St., Philadelphia, Pa.

Alt, William (D), Welder, Taylor Forge & Pipe Works; (Res.) 7209 Rhodes Ave., Chicago, Ill.

Ames, Ethan E. (C), John A. Roebling's Sons Co., 701 St. Clair, N. E., Cleveland, Ohio.

Ames, Louis (C), Supervisor Applied Eng. Dept., Air Reduction Sales Co., 1116 Ridge Ave., Pittsburgh, Pa.

Ames, M. S. (C), Foreman, % Leeds & Northrup Co., 4901 Stenton Ave., Philadelphia, Pa.

Ames, W. B., Jr. (C), 618 W. Buffalo, Ithaca, N. Y.

Amirikian, A. (C), Designing Engineer, Navy Dept., Bureau of Yards & Docks; (Res.) 2305 Tunlaw Road, N. W., Washington, D. C.

Amundsen, R. G. (C), 126—5th Ave., Brooklyn, N. Y.

Anchor, Charles J. (C), Shop Engineer, American Car & Foundry Co., Arch St. Office, Milton, Pa.

Anchors, E. H. (C), Branch Manager, Air Reduction Sales Co., 680 Hamilton Ave., S. E., Atlanta, Ga.

Andersan, Karl M. (D), Welder, Federal Shipbldg., & Dry Dock Co.; (Res.) 309 Chestnut Ave., Metuchen, N. J.

Andersen, Prof. Aksel (C), The Tech. University of Norway, Trondheim, Norway.

Andersen, C. S. (D), Supt., Penn. Furnace & Iron Co., Warren, Pa.

Andersen, G. M. (C), Engineer, Structure & Bldgs., Chicago Rapid Transit Co., Rm. 1240—72 W. Adams St., Chicago, Ill.

Andersen, H. M. (B), Instructor of Welding, Morton High School; (Res.) 2209 So. 61st Court, Cicero, Ill.

Anderson, Albert C. (B), Manager, South African Iron & Galo Works, P. O. Box 146 Jeppe, Johannesburg, South Africa.

Anderson, Alex. A. S. M. (B), Engineer, Onamea Sugar Co., Papaikou, Hawaii.

Anderson, Arthur R. (C), Research Associate, Mass. Inst. of Tech., Cambridge, Mass.

Anderson, B. A. (D), Welder, Hagstrom Mfg. Co.; (Res.) 2825 Belleview, Kansas City, Mo.

Anderson, C. D. (B), Welder, Tri-State Armature & Electric Works, 321 Butler Ave., Memphis, Tenn.

Anderson, E. D. (C), Manager Dist. Sales, Lincoln Electric Co., 1818 Main St., Kansas City, Mo.

Anderson, E. J. (C), Supervisor, York Ice Machinery Corp., Rt. 4, York, Pa.

Anderson, H. (D), Asst. Foreman, Whiting Corp., Route 3, Chesterton, Ind.

Anderson, James L. (B) Manager, Engineering Research Dept., Air Reduction Sales Co., 181 Pacific Ave., Jersey City, N. J.

Anderson, Karl H. (D), 2111 Myra St., Jacksonville, Fla.

Anderson, Louis (D), Welder, Cities Service; (Res.) 711 East Cleveland, Ponca City, Okla.

Anderson, Robert L. (C), Engineer (Structural), Navy Dept. Bureau Yard & Docks; (Res.) 4322—18th St. N., Arlington, Va.

Anderson, Walter (B), Vice-President, Taylor Winfield Corp., 2857 Grand Blvd., E., Detroit, Mich.

Anderson, Wm. M. (C), Supt. Columbian Steel Tank Co., 1509 W. 12th St., Kansas City, Mo.

Anderton, James T. (C), Leadingman Welder, Navy Yard; (Res.) Navy Y. M. C. A., 167 Sands St., Brooklyn, N. Y.

Andre Frank V. (C), Partner, Andre Lumber & Supply Co.; (Res.) 387 Hodge St., Blairsville, Pa.

Andrews, Chas. (B), Manager, Washburn Company, Andrews Division, Rockford, Ill.

Andrews, F. H. (C), 80 Raritan Road, Linden, N. J.

Andrews, James (C), Welder, International Refining Co., Box 24, Sunburst, Mont.

Andrus, B. S. (B), Owner, Riteway Welding Serv., Concord & Richmond Sts., So. St. Paul, Minn.

Anker, Raymond H. (D), Welder, Getz Powen Washer Co., Morton, Ill.

Anspaugh, Eldon (D), Welder & Heat Treater, Hause Valve Co.; (Res.) 419 E. Lawrence St., Montpelier, Ohio.

Anthony, Chas. C. (C), Vice-President, Byrne Doors Inc., 1223 Conn. Ave., N. W., Washington, D. C.

Appel, Leonard A. (D), New England Welding Lab., 88 St. Stephen St., Boston, Mass.

Applegate, C. W. (D), 1041 So. Downing, Denver, Colo.

Applegate, M. W., Jr. (C), Engineer, The Linde Air Prods. Co.; (Res.) 1816 E. 21st St., Brooklyn, N. Y.

Arbella, Edward (D), Arc Welder Oilgear Co.; (Res.) 202 E. Keefe Ave., Milwaukee, Wis.

Archer, Fred C. (C), District Manager, Lincoln Electric Co., 1560 W. Pierce St., Milwaukee, Wis.

Ardagh, J. C. (C), Welding Engineer, The Lincoln Electric Co., 600 W. 41st St., Chicago, Ill.

Arington, L. G. (D), Operator, Coleman Lamp & Stove Co.; (Res.) 1225 Stilwell, Wichita, Kans.

Armstrong, Charles H. (B), Edge Moor Iron Works, Inc., 30 Rockefeller Plaza, New York City.

Armstrong, R. M. (B), District Representative, Machinery & Welder Corp., 348 N. St. Francis St., Wichita, Kansas.

Arness, O. J. (C), Welder, Edison General Electric Appliance Co.; (Res.) 129 S. Parkside Ave., Chicago, Ill.

Arnold, A. C. (C), Salesman, National Cylinder Gas Co.; (Res.) 4079 E. 108th St., Cleveland, Ohio.

Arnott, David (A), Vice-President & Chief Surveyor, American Bureau of Shipping, 24 Old Slip, New York, N. Y.

Art, Vernon (C), Welding Dem., Jeffrey Mfg. Co.; (Res.) 155 E. Northwood Ave., Columbus, Ohio.

Arterburn, C. B. (B), Welding Engineer, American Air Filter Co., 215 Central Ave., Louisville, Ky.

Arthur, Guy, Jr. (B), Industrial Engineer, Chas. E. Bedaux Co.; (Res.) 590 E. 3rd St., %Pelhutchinson, Mt. Vernon, N. Y.

Aschenbrenner, F. J. (B), Chief Metallurgist, Wilson Welder & Metals Co., Box 96, North Bergen, N. J.

Aschinger, R. R. (B), Salesman, Lincoln Electric Co.; (Res.) 1319 Overton, Memphis, Tenn.

Ashby, I. F. (B), Construction Engineer, Williams Bros. Corp.; (Res.) 212 W. 32nd St., Houston, Texas.

Ashcroft, Robert B. (D), Electric Welder, Pfaudler Co.; (Res.) 101 Southern Parkway, Rochester, N. Y.

Ashley, Charles H. (D), Ashley Welding Machine & Iron Co. Inc., Henry & Sterling Sts., Kingston, N. Y.

Ashton, Frank W. (C), Assistant Engineer, U. S. Engineers, Clock Tower Bldg., Rock Island, Ill.

Ashton, W. L. (B), Chief Designer, Ash-Howard-Needles & Tammen, 1012 Baltimore Ave., Kansas City, Mo.

Atkins, C. N. (D), Electric Arc Welder, Milton Bowers Welding Co., 346 Madison, Memphis, Tenn.

Atmore, Guy D. (C), Salesman, Air Reduction Sales Co., 401 Santa Barbara St., Santa Paula, Calif.

Auld, Samuel (C), Supt. Leach Steel Corp., P. O. Box 744, Rochester, N. Y.

Aumann, Henry (D), 330 W. 12th St., New York, N. Y.

Aumiller, W. L. (D), Arc Welder, General Electric Co., 2030 Avenue B, Schenectady, N. Y.

Austin, Byron B. (D), Resistance Welding Engineer, Pontiac Motor, Div. of G. M.; (Res.) 156 W. Colgate Ave., Pontiac, Mich.

Austin, Herbert G. (C), Dist. Sales Mgr., Lukenweld, Inc., 131 State St., Boston, Mass.

Austin, John B. (C), Welding Consultant, Republic Struct. Iron Co.; (Res.) 1021 Nela View Rd., Cleveland Heights, Ohio.

Austin, William M. (B), Treasurer, Austin Hastings Co. Inc., Cambridge, Mass.

Axline, R. A. (C), President, Metallizing Engrg. Co., 21–07 41st Ave., Long Island City, N. Y.

Aztell, H. B. (D), Experimental Engineer, Taylor-Winfield Corp., Warren, Ohio.

B

Babbitt, C. A. (C), Welding Foreman, Western Pipe & Steel Co., 5717 Santa Fe Ave., Los Angeles, Calif.

Backus, Thomas, Jr. (B), Asst. Supt., Carnegie-Illinois Steel Corp., Mechanical Dept., Youngstown, Ohio.

Bacon, Harold (D), Welder, Electro Motive Corp.; (Res.) 6260 Stony Island Ave., Chicago, Ill.

Baer, Chester (D), Welder, Frick Co.; (Res.) R. R. 2, Waynesboro, Pa.

Bagsar, A. B. (B), Sun Oil Co., Marcus Hook, Pa.

Bailey, O. A. (B), Chief Engineer, Chicago Bridge & Iron Co., 1305 W. 105th St., Chicago, Ill.

Bailey, Robert P. (C), Shop Supt., General Electric Co., 5950—3rd Ave., Detroit, Mich.

Bailie, E. Harold, (B), Welding Supervisor, Nordberg Mfg. Co., Milwaukee, Wisconsin.

Bain, George (C), Salesman, Lincoln Electric Co., 1818 Main St., Kansas City, Mo.

Bainbridge, C. G. (B), Manager, Technical Service Dept., The British Oxygen Co., Ltd., Thames House, Millbank, S. W. 1, England.

Baines, Harry C. (D), Welder, Mack Mfg. Co.; (Res.) 1234½ Tilghman St., Allentown, Pa.

Baker, H. U. (B), Engineer, Roscoe Moss Co., Box 64, Station A, Los Angeles, Calif.

Baker, John D. (C), Production Manager, Hugh J. Baker & Co., P. O. Box 892, Indianapolis, Ind.

Baker, W. B. (B), Vice-President & General Manager, Una Welding, Inc., 1615 Collamer Ave., Cleveland, Ohio.

Bakshas, Sam B. (D), 1726—2nd St., Lincoln, Neb.

Balkcom, E. E. (C) Owner, Acme Welding & Electric Co., 619–21 Broadway, Macon, Ga.

Ball, D. A. (D), Welder, Emsco Derrick & Equip. Co.; (Res.) 6811 S. Alameda, Los Angeles, Calif.

Ball, Harry W. (D), Welder, 1025 W. 3rd St., Hutchinson, Kansas.

Ballantyne, W. M. H. (B), Installation Engineer, Penn Water & Power Co.; (Res.) 5510 Windsor Mill Rd., Balto Co., Woodlawn R. D. 5, Baltimore, Md.

allard, E. L. (C), Metallurgist, Timken Steel & Tube Div.; (Res.) R. D. 7, North Canton, Ohio.

Balsley, Eugene A. (B) Welding Engineer, Link Belt Co.; (Res.) 644 Diversey Parkway, Chicago, Ill.

Balzer, Otto (C), Welder, Sherritt Gordon Mines; (Res.) 4334 Albert St., Vancouver, B. C., Canada.

Banash, J. I. (B), Consulting Engineer, 230 No. Michigan Ave., Chicago, Ill.

Bandy, William (D), Welder, R. G. LeTourneau, Inc.; (Res.) 654 N. Main St., E., Peoria, Ill.

Banks, Kenneth B. (C), Engineering Dept., Black, Sivalls & Bryson, Inc., Box 1377, Oklahoma City, Okla.

Banzhaf, E. (C), Steel Union, Inc., 464 Chamber of Commerce Bldg., Los Angeles, Calif.

Barajas, Paul M. (F), 509 Hester St., Stillwater, Okla.

Barclay, Paul V. (B), P. O. Box 495, Mason City, Iowa.

Bard, J. K. (B), Supt. Fabricated Steel Constr., Bethlehem Steel Co., Buffalo Works, Bell & Abby Sts., Buffalo, N. Y.

Barker, George (C), R. R. 1, Goodrich Drive, Florence, Ky.

Barker, J. A. (D), Welder, Delco Appliance Div.; (Res.) 104 Wilder St., Rochester, N. Y.

Barker, J. E. (C), Adjuster, Hartford Steam Boiler Insp. & Ins. Co.; (Res.) 845 Aileen St., Oakland, Calif.

Barker, P. F. (C), Sheet Metal & Fabricating Methods, Gleason Works, 1000 University Ave., Rochester, N. Y.

Barkow, A. G. L. (B), Mechanical Consultant, Maryland Casualty Co.; (Res.) 3006 Pinewood Ave., Baltimore, Md.

Barkow, E. F. (C), Production Engineer, Wisconsin Bridge & Iron Co., 5023 N. 35th St., Milwaukee, Wis.

Barlette, Anthony (D), Electric Welder, Eastman Kodak Co., (Res.) 36 Avery St., Rochester, N. Y.

Barnes, Harold N. (C), Asst. Supt., Cherry-Burrell Corp., Little Falls, N. Y.

Barnes, I. H. (D), Welding Shop Supt., Moorlane Co., Box 1679, Tulsa, Okla.

Barnes, William H. (C), Eng. Surface Lines, Boston Elevated Railway, 31 St. James Ave., Boston, Mass.

Barnes, Wm. W. (C), Phila. Manager, Air Reduction Sales Co., 17th & Allegheny Ave., Phila., Pa.

Barnett, Orville T. (C), Metallurgist, Black, Sivalls & Bryson, Inc., Box 1377, Oklahoma City, Okla.

Barrett, George N. (B), Asst. to Metallurgist, Weirton Steel Co., Metallurgical Dept., Weirton, W. Va.

Barrett, Richard C. (C), District Manager, The Linde Air Products Co., 732 1st Wisconsin National Bank Bldg., Milwaukee, Wis.

Barrios, E. H. (B), Welding Technician, Woodward-Wight & Co. Ltd.; (Res.) 600 Codifer Blvd., New Orleans, La.

Barrois, Nelson P. (D), App. Welder, Freeport Sulphur Co.; (Res.) Myrtle Grove, Louisiana.

Barron, J. H. (B), Sales Engineer, Stainless Steel Products of Minnesota, 1000 Berry Ave., St. Paul, Minn.

Barrow, Clyde (B), General Manager, The Columbiana Boiler Co., Columbiana, Ohio.

Bartel, Leonard R. (C), Foreman, R. G. Le Tourneau, Inc.; (Res.) 2206 N. Jefferson, Peoria, Ill.

Bartels, Carl P. (B), Supt., The Mosler Safe Co., Hamilton, Ohio.

Bartges, W. S. (D), Welding Foreman, Cruse-Kemper Co.; (Res.) 602 Trinity Place, Ambler, Pa.

Bartter, Ivan R. (C), Welding Engineer, Lincoln Elec. Co., 222 So. 21st Ave. E., Duluth, Minn.

Bassett, H. H. (B), Dist. Manager, The Linde Air Products Co., National Bank of Tulsa Bldg., Tulsa, Okla.

Bassler, Clyde G. (B), Dist., Manager, Taylor-Winfield Corp., 1225 Washington Blvd., Chicago, Ill.

Bateman, Thos. H. (C), Representative, W. H. S. Bateman & Co., 813 Commercial Trust Bldg., Phila., Pa.

Bates, Earl (B), Owner, Earl Bates Welding Service, 634 Stubenville Ave., Cambridge, Ohio.

Bates, Harvey C. (C), Mech. Foreman, Socony-Vacuum Oil Co., Inc.; (Res.) 337 Main St., Augusta, Kansas.

Bauer, Leo (C), Sr. Inspector of Engg. Materials Mech., U. S. Navy Dept., 90 Church St., N. Y.; (Res.) 747 Van Buren Ave., Elizabeth, N. J.

Bauerfeld, Jack E. (D), Welder, American Hoist and Derrick Co.; (Res.) 268 Lafond St., St. Paul, Minnesota.

Bauleke, Chester H. (D), 329 So. 4th St., Le Sueur, Minn.

Baumann, Henry R. (D), Electric Welder, N. Y. Navy Yard; (Res.) 115 Gates Ave., Brooklyn, N. Y.

Baumer, Scott D. (B), Superintendent Welding & Sheet Metal Work, Bethlehem Steel Co.; (Res.) P. O. Box 15, Johnstown, Pa.

Baumler, Harold A. (D), A. O. Smith Corp., 3003 N. 2nd St., Milwaukee, Wis.

Baylor, W. B. (B), Engineer, Pittsburgh Steel Co., 931 Richfield Bldg., Los Angeles, Calif.

Bayston, Richard B. (C), Assistant Sales Manager, Chattanooga Boiler & Tank Co., Box 110, Chattanooga, Tenn.

Beadle, T. M. (B), Managing Director, The Beadle Welding & Engrg. Co., 186 Tuam St., Christchurch, C 1, N. Z.

Beal, W. (D), Manager, Ferro Welding & Eng. Co.; (Res.) 224 W. Bridge St., Elyria, Ohio.

Bean, Ray (D), Welder, Consolidated Coal Co.; (Res.) R. R. 1, Carterville, Ill.

Bearor, Theodore (D), 244 W. 101st St., New York, N. Y.

Beattie, William H. (B) Foreman Welder, The Mount Lyell Mining and Railway Co. Ltd., Box 63, Queenstown, Tasmania, Australia.

Beatty, R. C. (C) 1200 So. 60th St., Philadelphia, Pa.

Bechtel, S. D. (B), President, W. A. Bechtel Co., 155 Sansome St., San Francisco, Calif.

Beck, Tage (C), Sales Engineer, Truscon Steel Co.; (Res.) 122 E. Broad St., Falls Church, Va.

Beck, William (B), District Sales Manager, The Linde Air Products Co., 532 E. 25th St., Baltimore, Md.

Becker, A. J. (D), Washington, Ill.

Becker, Clarence W. (C), Salesman, The National Cylinder Gas Co., 4620 Este Ave., Cincinnati, Ohio.

Becker, Edward C. (B), Structural Engineer, Phoenix Engineering Corp., 2 Rector St., New York City.

Beckett, Geo. H. (B), Master Mechanic, Canadian Canners Ltd.; (Res.) Box 212, Mission City, B. C., Canada.

Bedworth, Robert E. (B), Welding Representative, Eastern Dist., Westinghouse Electric & Mfg. Co., 150 Broadway, New York, N. Y.

Beebe, Erle J. (B), Welding Engineer, Southwest Exploration Co.; (Res.) 558 N. Bayou Ave., Bellflower, Calif.

Beeley, Raymond (B), Shop Supt., Texas Pipe Bending Co.; (Res.) 301 Frio St., Houston, Texas.

Beers, Frank (D), 1315 Bradbury St., Indianapolis, Ind.

Beeson, J. K. (A), Asst. General Sales Manager, Pittsburgh Steel Co., P. O. Box 118, Pittsburgh, Pa.

Begley, F. P. (C), Master Mechanic, National Cast Iron Pipe Co., Birmingham, Ala.

Begtrup, E. F. (B), Asst. Supt., Metal & Thermit Corp., 92 Bishop St., Jersey City, N. J.

Behling, Ellsworth (F), Franklin Ave., Oconto Falls, Wis.

Behling, Emil A. (C), Supt.; The Vilter Mfg. Co., 1618A So. 26th St., Milwaukee, Wis.

Behling, Harold W. (D), Welding Clerk, The Vilter Mfg. Co., 2217 So. 1st St., Milwaukee, Wis.

Behmke, Peter S. (D), 2171 So. 6th St., Milwaukee, Wis.

Beiersdorfer, Louis W. (D), 58 Glenwood Ave., Cincinnati, Ohio.

Beilke, Martin (D), Welder, Minn. Gas Light Co.; (Res.) 2348 Thomas Ave. N., Minneapolis, Minn.

Beishline, Guy C. (C), General Welding Supervisor, American Car & Foundry Co., Berwick, Pa.

Beldon, Arthur J. (B), Service Supervisor, The Linde Air Products Co., 1001 So. 22nd St., Birmingham, Ala.

Bell, John L. (C), Welder, Eastern States Milling Corp.; (Res.) 57 Warren Ave., Kenmore, New York.

Bell, Mace H. (C), District Engineer, American Institute of Steel Construction, 506 Stern Bldg., New Orleans, La.

Bellamy, Benjamin (C), Salesman, Carnegie-Illinois Steel Co., 71 Broadway, New York, N. Y.

Belli, Raymond (D), 1323 Clifford Ave., Rochester, N. Y.

Bender, R. C. (B), Hollup Corp., 1201 Commerce Ave., Houston, Texas.

Benedict, D. N. (A), Vice-President & General Manager, Frick Co., Waynesboro, Pa.

Benedict, Everett R. (B), Vice-President, Contract Welders, Inc., 2545 E. 79th St., Cleveland, Ohio.

Bengston, Nils B. (B), Acting Mech. Supt., Colonial Beacon Oil Co., 30 Beacham St., Everett, Mass.

Bennett, A. A. (C), Caldwell, Idaho.

Bennett, F. C. (C), Atlantic Gulf & Pacific Co. of Manila, P. O. Box 626, Manila, P. I.

Bennett, William (B), Principal Surveyor (U. S. A. & Canada), Lloyd's Register of Shipping, 17 Battery Place, New York.

Bentley, Harold P. (B), President, Bentley Weldery Inc., Teall Ave. & Riegel St., Syracuse, N. Y.

Berg, Fred (B), Hobson-McFarland Tractor Co., 2131 Washington St., Kansas City Mo.

Bergmann, J. R. (B), P. O. Box 44, Cleves, Ohio.

Berkeley, L. R. (C), Sales Dept. Contract Welders; (Res.) 3229 Fairmount Blvd., Cleveland, Ohio.

Bernard, Frank M. (C), Welding Supervisor, Westinghouse Electric Co., 306 Fourth Ave., Pittsburgh, Pa.

Berning, Harold J. (D), Pipe Line & Extra Welder, Sinclair Refining Co., Box 26, Iola, Kansas.

Berry, Edwin (D), Welder, Southern Pacific R. R. Co.; (Res.) 1520 Arcade Blvd., Sacramento, Calif.

Berry, Elmo (D), Electric Arc & Oxyacetylene Operator; (Res.) % Roscoe Berry, Louisa, Kentucky.

Bert, John D. (C), Welding Engineer, Navy Department; (Res.) 5112 Connecticut Ave., Wash. D. C.

Bertsch, Dr. John A., (B), Consulting Chemical Engineer, Cuproweld, 3512 Halliday Ave., St. Louis, Mo.

Best, Frank A. (B), President, Backstay Standard Co., 813 Mercer St., Windsor, Ont., Canada.

Beveridge, Edwin F. (C), Assistant Shop Foreman, Motor Center; (Res.) 2428½ Eye St., Bakersfield, Calif.

Beyreuther, Robert (B), Welding Engineer, Carrier Corp.; (Res.) 116 Boise Dr., Syracuse, N. Y.

Bibber, Leon C. (A), Welding Engineer, Carnegie-Illinois Steel Corp., Carnegie Bldg., Pittsburgh, Pa.

Bick, Richard R. (D), Electric Welder, Foster Wheeler Corp.; (Res.) 25 Park Ave., Dansville, N. Y.

Biedermann, Harry (C), Acetylene & Welding Equip. Co., 38 Newton Blvd., Free-Port, N. Y.

Biery, Albert A. (D), Welder, Cincinnati Gas & Electric Company; (Res.) 428 S. Miami Ave., Cleves, Ohio.

Bieszk, Anthony (B), Joint Owner, Bieszk Brothers Co., 37705 Plymouth Road, Plymouth, Mich.

Biggs, W. E. (C), Sales Engineer, The Lunkenheimer Co.; (Res.) 4000 Cathedral Ave. N. W., Washington, D. C.

Binck, Arthur S. (D), Works Laboratory, General Electric Co., Schenectady, N. Y.; (Res.) 520 Riverside Ave., Scotia, N. Y.

Binder, Henry J. (D), Welder, J. I. Case Co.; (Res.) 1008 Grand Ave., Racine, Wis.

Binder, R. W. (C), Engineer, Bethlehem Steel Co., P. O. Box 58, Watts Station, Los Angeles, Calif.

Bingaman, Roy (D), Welder, York Ice Machinery Corp.; (Res.) 120 N. Queen St., York, Pa.

Bird, George T. (B), President, Bird-Potts Co., Inc., 376 Marietta St., N. W., Atlanta, Ga.

Birkholz, John (C), Fabricating Shop Foreman, Bethlehem Steel Co.; (Res.) 613 E. St., Sparrows Pt., Md.

Birnie, Albert W. (C), Welder, Fitchburg Paper Co.; (Res.) 5 Oxford St., Fitchburg, Mass.

Bisbee, David P. (C), Prod. Mgr., Rheem Mfg. Co.; (Res.) 4361 Firestone Blvd., South Gate, Calif.

Bissell, A. G. (C), Senior Welding Engineer, Bureau of Construction and Repair, Room 2130 Navy Building, Navy Dept., Washington, D. C.

Bissell, Kenneth B. (C), Welding Engineer, American Brass Co.; (Res.) 50 Maple St., Milford, Conn.

Bitticks, Glenn L. (C), Plant Supt., Commerce Mining & Royalty Co., Box 873, Miami, Okla.

Bittner, Henry (C), Foreman, Nash Engineering Co., South Norwalk, Conn.

Bjornstad, C. I. (C), Welder Salesman, Harnischfeger Corp.; (Res.) 2446 Aldrich Ave., So. Minneapolis, Minn.

Black, J. N. (B), Welding Specialist, General Electric Co., 40 S. 3rd St., Columbus, Ohio.

Black, William A. (D), Welder, Dept. Water-Power-Light, City of Los Angeles, Calif.; (Res.) 6026 Falcon Ave., Long Beach, Calif.

Blackburn, Bryan (C), Engineer, R. D. Cole Mfg. Co., Newnan, Ga.

Blackman, E. N. (D), Welder, Dunlap Welding & Bit Works; (Res.) 523 A. Kentucky St., Bakersfield, Calif.

Blackmore, F. L. (C), Ruth, Nevada.

Blais, Lucien (C), 12A Montcalm, St. Joseph De Sorel, Canada.

Blake, James C. (C), Vice-President & Los Angles Manager, Victor Equipment Co., Wldg. Equip. Div., 3821 Santa Fe Ave., Los Angeles, Calif.

Blake, J. W. (D), Mech. Engineer, Oklahoma Gas & Elec. Co., 3rd & Harvey, Oklahoma City, Okla.

Blakely, R. A. (C), Sales Manager, National Cylinder Gas Co., 541 Seneca St., Buffalo, N. Y.

Blakeney, Lewis B. (C), Sales Engineer, Westinghouse Elec. & Mfg. Co., 207 North 3rd St., Cincinnati, Ohio.

Blakey, Robert M. (D), Salesman, Marquette Mfg. Co., Minneapolis, Minn.; (Res.) 3718 Beehler Ave., Baltimore, Md.

Blanchard, B. E. (B), 947 N. Oakdale, De Pere, Wis.

Blanchard, Jules R. (C), Welding Instructor & Welding Supervisor, S. T. Institute, Shawinigan Chemicals Co., Ltd., Shawinigan Falls, Quebec, Canada.

Bland, George O. (C), Service & Dem., Hill Equip. & Eng. Co., 3431 Chouteau Ave., St. Louis, Mo.

Blankenbuehler, John (B), Designing Engineer, Westinghouse Electric & Mfg. Co., (Res.) 234 Lehigh St.; Swissvale P. O., Pittsburgh, Pa.

Blanton, John W. (D), Electric Welder, Navy Yard; (Res.) 3021 E. Linwood Ave., Parkville, Md.

Blazey, John S. (D), Canal & Holmes St., Palmyra, N. Y.

Bleecker, Kenneth (B), Sales Engineer, Page Steel & Wire Div., American Chain & Cable Co., 230 Park Ave., New York, N. Y.

Bleecker, W. H. (A), Sales Manager, Page Steel and Wire Division, American Chain and Cable Company, Inc., Monessen, Penna.

Bleikamp, C. E. (C), Executive Asst., St. Louis Car Co., St. Louis, Mo.

Blennerhassett, L. I. (C), Engineer, C. F. Braun & Co., Alhambra, Calif.

Blesi, John E. (D), Serviceman, Air Reduction Sales Co., 2825 N. 29th Ave., Birmingham, Ala.

Blickman, Saul (B), President, S. Blickman, Inc.; (Res.) 536 Gregory Ave., Weehawken, N. J.

Blind, J. George (C), Supt., Midwest Piping and Supply Co., Inc., 5 Central Ave., Clifton, N. J.

Blodgett, Omer (F), 1410 E. 5th St., Duluth, Minn.

Blomberg, M. (B), Car Body Engr., Electro-Motive Corp., La Grange, Ill.

Blome, Henry (D), Scheduling, R. G. Le Tourneau; (Res.) 408 Bootz Ave., Peoria, Ill.

Bloodgood, C. M. (C), Pacific Coast Manager, Air Reduction Sales Co., 220 Bush St., San Francisco, Calif.

Bloxton, C. R. (C), Dist. Engineer, American Inst. of Steel Constr., 405 Bona-Allan Bldg., Atlanta, Ga.

Blubaugh, Earl F. (C), Manager & Part Owner, West Side Service Co. of Taft; (Res.) 130 Center St., Taft, Calif.

Blum, Harold P. (C), District Sales Manager, Una Welding, Inc.; (Res.) 3329 Daleford Rd., Shaker Heights, Ohio.

Boardman, Harry (B), Research Engineer, Chicago Bridge & Iron Co.; (Res.) 10357 S. Hoyne Ave., Chicago, Ill.

Bockemohle, C. L. A. (C), Structural Engineer, Los Angeles Union Passenger Terminal; (Res.) 3164 Hill St., Huntington Park, Calif.

Boeck, R. E. (C), Welding Engineer, Worden Allen Co.; (Res.) 3889 No. 4th St., Milwaukee, Wis.

Boehmke, Arthur H. (D), Arc Welder, Otis Steel Co.; (Res.) 1548 Olivewood Ave., Lakewood, Ohio.

Boetcher, Hans Nielsen (C), Asst. to Supt. of Power Production Stations, Consol. Gas, Electric Light & Power Co., Westport Steam Station, Baltimore, Md.

Boggs, R. W. (B), Tech. Publicity, Union Carbide Co., 30 E. 42nd St., New York, N. Y.

Bogh, Waldemar (D), Welder, San Antonio Gold-Mines, Ltd.; (Res.) Bissett, Man., Canada.

Boian, Wilbur O. (B), Structural Engineer, Pittsburgh-Des Moines Steel Co., 1015 Tuttle St., Des Moines, Iowa.

Boland, F. J. (D), 12 Clark Place, W. New Brighton, Staten Island, N. Y.

Bolaskey, Frank (B), Owner, Bennett & Bolaskey, 209 Main St., Beacon, N. Y.

Boley, Frederick W. (C), Salesman, Air Reduction Sales Co., 7244 Coles Ave., Chicago, Ill.

Bolin, Nick (C), Salesman, Lewis Supply Co., 477 S. Main St., Memphis, Tenn.

Bollenbach, W. M. (B), President & Treasurer, % Edward E. Johnson, Inc., 2304 Long Ave., St. Paul, Minn.

Bollin, Edward F. (C), Welding Foreman, Buffalo Foundry & Machine Co.; (Res.) 80 Hartwell Rd., Buffalo, N. Y.

Bollinger, J. Gale (C), Asst. Sales Manager, Air Reduction Sales Co., Parke & Halleck Sts., Emeryville, Calif.

Boncher, Harry P. (D), Welding Foreman, A. O. Smith Corp.; (Res.) 4042 N. 25th St., Milwaukee, Wis.

Bond, Frank A. (A), Vice-President, The McKay Company, 1005 Liberty Ave., Pittsburgh, Pa.

Bonge, Albert J. (D), R. R. 6, W. 17th St., Holland, Mich.

Boniewski, Edward F. (D), 760 Cortland St., Perth Amboy, N. J.

Bonneval, M. E. (C), Welder, Freeport Sulphur Co., Port Sulphur, La.

Bonneval, L. Robert (C), Welder, Freeport Sulphur Co., Happy Jack, La.

Boom, W. B. (B), President & General Manager, Boom Boiler & Welding Co., 2313 Elm St., Cleveland, Ohio.

Booth, C. E. (D), Welder, Buehler Tank & Welding Works, 229 Fontana, Downey, Calif.

Booth, Henry (A), Sales Manager, Shawinigan Products Corp., Empire State Building, New York, N. Y.

Booth, T. H. (C), Asst. Supt., Walworth Co., Greensburg, Pa.

Borcherdt, Walter O., Jr. (B), Mechanical Engineer, 50 Dartmouth Rd., Mountain Lakes, N. J.

Borne, Floyd O. (C), Assoc. Engineer, U. S. Engineers, 615 Commerce Bldg., St. Paul, Minn.

Borner, Fred (C), 1309 Boulevard, Broad Channel, L. I.

Bornscheim, John F. (B), President, International Trades Inst. Inc., 741 W. 70th St., Chicago, Ill.

Borresen, William (B), Plant Supt., W. K. Mitchell & Co., Inc., 2940 Ellsworth St., Phila. Pa.

Borst, G. E. (C), Manager, Phila. Office, Chicago Bridge & Iron Works, 1700 Walnut St., Philadelphia, Pa.

Borst, W. H. (C), Sales Engineer, Lincoln Electric Co.; (Res.) 52-111 79th St., Elmhurst, Long Island, N. Y.

Borsum, Finn (C), Chief Clerk, Sveiseindustri, 15 Bentsebrogt, Oslo, Norway.

Borton, George W. (C), President and General Manager, Pennsylvania Crusher Co., 17th Fl., Liberty Trust Bldg., Philadelphia, Pa.

Bos, John, Jr. (B), Vice-President, Bos-Hatten Inc.; (Res.) 60 Westfield Rd., Eggertsville, N. Y.

Bosse, E. M. (C), Jos. T. Ryerson & Son, 5 Clinton St., St. Louis, Mo.

Boswell, E. N. (B), Manager, American Locomotive Co., Dunkirk, N. Y.

Bott, Harry B. (D), Electric Arc Welder, International Nickel Co.; (Res.) 1236 Robert St., Hillside, N. J.

Botts, John (D), Welder, Weirton Steel Co.; (Res.) R. D. No. 1, Weirton, W. Va.

Bourgman, V. (B), Civil Engineer, Office of Commissioner of the U. S. S. R. at the New York World's Fair, 261 Fifth Ave., New York, N. Y.

Bowden, Alfred D. (C), Manager Research Dept., Pittsburgh Piping & Equipment Co., 10—43rd St., Pittsburgh, Pa.

Bower, Cecil L. (C), Associate Engineer, Mo. State Highway Dept., Jefferson City, Mo.

Bowers, F. M. (A), General Manager, The Fibre-Metal Products Co., 5th & Tilghman Sts., Chester, Pa.

Bowers, Milton (B), Owner, Milton Bowers Welding Co., 346 Madison Ave., Memphis, Tenn.

Bowles, H. J. (C), General Foreman, % Bethlehem Steel Co., Bethlehem, Pa.

Boyd, David (B), Welding Engr., Canadian Car & Fdy. Co.; (Res.) 6—36th Ave., Lachine, Que., Canada.

Boyd, Douglas E. (B), Sales Engineer, Jos. T. Ryerson & Son, Inc., 645 St. Marks Ave., Westfield, N. J.

Boyd, Murray G. (B), Technical & Commerical Manager, Spanner Thimble Tube Boilers, Ltd., 9 Billiter Sq., London E. C. 3, Eng.

Boyd, W. R. (B), Prop., Boyd Welding Co., 160 E. 8th St., Erie, Pa.

Boyer, F. E. (D), Reed Roller Bit Co., Houston, Texas.

Boyer, Thomas H. (C), Automatic Welding Unionweld Process, Sun Shipbldg. & DD Co.; (Res.) 146 E. Main St., Coatesville, Pa.

Boyes, Stanley (D), Arc Weld operator, Dominion Rubber Co., Box 43 R. M. S., Kitchener, Ontario.

Boyle, Leo W. (C) Welding Foreman, International Harvester Co.; (Res.) 428—15th Ave., E. Moline, Ill.

Boyle, Richard A. (D), Welder, General Electric Co.; (Res.) 7 Davis Court, Saugus, Mass.

Bradbury, Geo. L. (C), Welding Equipment Salesman, Hickinbotham Bros., Ltd., Drawer 1730, Stockton, Calif.

Bradley, Chas. E. (D), Taylor Forge & Pipe Works; (Res.) 7855 Indiana Ave., Chicago, Ill.

Bradley, R. B. (C), Welding Sales Dem., Metal & Thermit Corp.; (Res.) 12813 Wallace St., Chicago, Ill.

Bradshaw, A. (C), Chief Inspector of Boilers, Government of Alberta; (Res.) 10534—126th St., Edmonton, Alberta, Canada.

Brady, Edward J. (B), Manager, Stainless & Alloy Division, The McKay Company; (Res.) 1417 E. Market St., York, Pa.

Brague, William W. (B), Supt., Welding Dept., The Warren City Tank & Boiler Co.; (Res.) 432 Fairmount Ave., Warren, Ohio.

Braithwaite, R. G. (B), Welding Engineer, % Braithwaite & Co. Engrs., Ltd., Crown Bridge Works, West Bromwich, Staffordshire, England.

Bramhall, Charles H. (C), Asst. Engineer, Bureau of Yard & Docks, Navy Dept.; (Res.) 201 Garwood St., Silver Spring, Md.

Brand, E. A. (C), Buffalo Niagara & Eastern Power Co., Electric Bldg., Buffalo, N. Y.

Brandsma, R. (D), Operator, Rheem Mfg. Co.; (Res.) 10126 Wentworth Ave., Chicago, Ill.

Brandt, Elmer (B), Production Manager, Hevi-Duty Electric Co., 4214 W. Highland Blvd., Milwaukee, Wis.

Brandt, H. L. (C), Welder, Freeport Sulphur Co., Diamond, Louisiana.

Brant, Robt. P. (B), Salesman, Indiana Oxygen Co., 135 Delaware St., Indianapolis, Ind.

Brass, Eugene (B), Engineer, Buffalo Tank Corp., P. O. Box "D" Mariners Harbor, Staten Island, N. Y. C.

Braun, Ben N. (C), Treas., Midland Structural Steel Co.; (Res.) 1300 S. 54th Ave., Cicero, Ill.

Braun, E. W. (B), Sales, The Linde Air Products Co., 1001 So. 22nd St., Birmingham, Ala.

Breese, Clayton D. (C), Vice-President, The Breese Bros. Co., 2347 Reading Road, Cincinnati, Ohio.

Brehm, Elmer (D), Welder, Sheboygen County Highway Dept.; (Res.) 1722 Garden Court, Sheboygen, Wis.

Brekelbaum, Erwin C. (B), Welding Supt., Harnischfeger Corp.; (Res.) 3319 No. 52 St., Milwaukee, Wis.

Bremer, E. W. (C), Sales Manager & Vice-President, Electroloy Co., Inc., 1600 Seaview Ave., Bridgeport, Conn.

Brendle, Russell W. (B), Welding Supervisor, Great Lakes Engrg. Wks.; (Res.) 198 Summit Street, River Rouge, Mich.

Brenner, Sebastian K. A. (D), Welder, Phila. Navy Yard; (Res.) 1616 Wingohocking St., Phila., Pa.

Brett, Geo. S. (C), Asst. Sales Manager, A. M. Castle Co., 32 W. Conn St., Seattle, Wash.

Brewer, W. D. (C), Chief Engineer, Okla. Gas & Elect. Co., Lincoln-Beer Bower Sta., Ponca City, Okla.; (Res.) 5125 Belle Isle Ave., Oklahoma City, Okla.

Brian, J. K. (B), Welder, Greenlease Motor Car Co.; (Res.) 2737 N. 8th St., Kansas City, Kansas.

Brickley, Earl M. (D), Elect. Welder, American Locomotive Co.; (Res.) 95 N. Allen St., Albany, N. Y.

Briggs, Clay (C), Chief Engineer, Cities Service Oil Co., Bartlesville, Okla.

Bringer, R. A. (D), Welder, Midwest Pipe & Supply Co.; (Res.) 235 S. Moore Ave., Monterey Park, Calif.

Britt, Oscar L. (C), Mech. Engineer, National Bureau of Standards; (Res.) 6209—30th St., N. W., Washington, D. C.

Britten, Clarence R. (B), Secretary, Monroe Calculating Machine Company; (Res.) 555 Mitchell St., Orange, N. J.

Broadhurst, Chas. T. (C), Struct. Designer, Gibbs & Hill; (Res.) P. O. Box 64, Wortendyke, N. J.

Broadley, Charles V. (B), Supervising Engineer, The Fidelity and Casualty Co. of N. Y., 122 South 4th St., Philadelphia, Pa.

Brocklebank, P. T. (B), Vice-President, J. P. Devine Mfg. Co., Mt. Vernon, Ill.

Bronson, Charles (D), Welder, Detroit Edison Co.; (Res.) 75 Oak St., River Rouge, Mich.

Brooke, Thomas V. (C), Experiment Engineer, The Lincoln Elec. Co., 310 So. Michigan Ave., Chicago, Ill.

Brooker, Edgar (C) Metallurgist, U. S. Navy; (Res.) 1629 So. Nelson St., Arlington, Va.

Brooking, Walter J. (B), Member of Engineering Department, R. G. Le Tourneau, Inc.; (Res.) 855 W. Virginia, Peoria, Illinois.

Brooks, W. E. (B), Shop Supt., Tennessee Coal, Iron & R. R. Co., Fairfield, Ala.

Broski M. Chester (D), Millwright Welder, Bethlehem Steel Co.; (Res.) 323 S. Cornwall St., Baltimore, Md.

Brotherton, Earl S. (D), Operator, Borbein Young & Co., 220 So. Emporia, Wichita, Kansas.

Broughton, Thomas M. (F), 449 West 5th Ave., Columbus, Ohio.

Brown, Alexander C., Jr. (C), Salesman, Air Reduction Sales Co., 7991 Hartwick St., Detroit, Mich.

Brown, Arthur G. (C), Designing Engineer, The Heil Co.; (Res.) 3278 So. Pennsylvania Ave., Milwaukee, Wis.

Brown, C. M. (B), J. B. Klein Iron & Fdy. Co., Oklahoma City, Okla.

Brown, Edgar Burr (C), Salesman, The American Brass Co., 174 S. Clark St., Detroit, Mich.

Brown, Fred E. (C), Welding Foreman, Bailey Meter Co.; (Res.) 7612 Avondale Ave., Garfield Hgts., Cleveland, Ohio.

Brown, Henry H. (C), Metallurgist, Eastman Kodak Co., Kodak Park Bldg. 23, Rochester, N. Y.

Brown, Irving C. (C), Dist. Manager, Thomson-Gibb Electric Welding Co.; (Res.) 2010 E. 102nd St., Cleveland, Ohio.

Brown, J. E. (D), Welder, General Petroleum Corp., P. O. Box 521, Lomita, Calif.

Brown, J. G. (D), Welding Foreman, Atlantic Refining Company, 3144 Passyunk Ave., Philadelphia, Pa.

Brown, Jesse J. (B), Electric Engineer, 435 Jamesville Ave., Syracuse, N. Y.

Brown, Leland (C), Vice-President, Darby Corp.; (Res.) 647 W. 59th St., Kansas City, Mo.

Brown, Maurice J. E. (B), Salesman, Union Engrg. Supply Co. Ltd.; (Res.) 10 Wallace Road, Durban, South Africa.

Brown, R. Earl (D), Machinist & Welder, Johnson & Thomas Mach. Wks.; (Res.) 2933 Sixth St., Riverside, Calif.

Brown, R. S. (D), Structural Steel Welder, Texas & New Orleans Ry. Co., 324 S. P. Bldg., Houston, Texas.

Brown, Thomas R. (B), Engineer, The Stearns-Roger Mfg. Co., 1720 California St., Denver, Colo.

Brown, Walter V. (B), President, Brown Steel Tank Co., 2943—4th St. S. E., Minneapolis, Minn.

Browne, R. L. (B), Metal & Thermit Corp., 75 State St., Albany, N. Y.

Browning, Roy T. (D), Electric Welder, U. S. War Engineers, 4407 Blackwater St., Fort Peck, Montana.

Bruce, George H. (C), Chief Engineer, Pittsburgh & Conneaut Dock Co., Conneaut, Ohio.

Bruckmiller, Edwin H. (C), 1703 Sage St., Houston, Texas.

Bruckner, Walter H. (C), Res. Asst. Prof. in Met. Eng., University of Illinois, Urbana, Ill.

Brucks, H. (D), Welder, Link Belt Co.; (Res.) 7736 Indiana Ave., Chicago, Ill.

Brueckner, Julius R. (B), Vice-President, National Electric Welding Machines Co., 6–255 General Motors Building, Detroit, Mich.

Bruen, Henry (C), Link Belt Co., 2410 W. 18th St., Chicago, Ill.

Bruer, C. B. (D), Welder, Allsteel Products Co.; (Res.) 1121 Pearce, Wichita, Kansas.

Brugge, B. J. (B), Welding Engineer, Big Three Welding & Equipment Co.; (Res.) 5915 Annapolis, Houston, Texas.

Brumbaugh, A. Kyle, Jr. (C), Engineer, General Petroleum Corp. of Calif., 2525 E. 37th St., Vernon, Calif.

Brumbaugh, I. V. (C), 4931 Daggett Ave., St. Louis, Mo.

Brummitt, P. W. (D), Welder, Bethlehem Steel Co.; (Res.) 49 Rogers St., Quincy, Mass.

Brunt, Roy (B), Salesman, Magnolia Airco Gas Prods. Co., Box 319, Houston, Texas.

Brush, Charles C. (C), Naval Architect, U. S. Coast Guard, Lighthouse Section, Route 3, Box 1165, Bethesda, Md.

Bruton, James J. (B), Service Engineer, The Linde Air Products Co., 2305 E. 52nd St., Los Angeles, Calif.

Bryan, Jr., Charles W. (B), Vice-President, Federal Shipbuilding and Dry Dock Co., 21 West St., New York, N. Y.

Bryan, Harry, Jr. (D), Electric Welding Leader, Bethlehem Steel Co., 1508 Irving St., N. E., Washington, D. C.

Bryla, Stefen (C), Professor, Politechnika School, Budownictuo II, Warsaw, Poland.

Buchanan, Wirt F. (C), Foreman of Welders, Cooper, Bessemer Corp., 325 McConnell St., Grove City, Pa.

Buckley, Chas. (D), Welder, American Locomotive Co.; (Res.) 721 Gerling St., Schenectady, N. Y.

Budds, George W. (D), Time Study Engineer, The Delco Appliance Corp.; (Res.) 181 Crittenden Blvd., Rochester, N. Y.

Buehler, Walter (B), Owner, Buehler Tank & Welding Works, 5000 Pacific Blvd., Los Angeles, Calif.

Bulkley, Harold F. (B), General Advertising & Publicity Manager, Union Carbide Co., 30 East 42nd St., New York City.

Bull, Charles M. (C), Engineer, Pidgeon-Thomas Iron Co., 1795 Kendale, Memphis, Tenn.

Bull, G. N. (C), District Sales Manager, The Lincoln Electric Co., 330 West 42nd St., New York City.

Bullock, C. L. (B), Structural Supt., J. B. Klein Iron & Fdy. Co., 1401 N. W. 3rd St., Oklahoma City, Okla.

Bullock, H. R. (C), Manager, Welding Dept., Austin-Hastings Co., Inc., 226 Binney St., Cambridge, Mass.

Bunn, Jr., W. B. (C), Welding Engineer, Naval Gun Factory, Washington Navy Yard, Washington, D. C.

Buonasera, Albert (B), Welder, Colonial Welding Co., 108–53 Roosevelt Ave., Corona, L. I.

Burdge, Oscar B. (D), Washington Ave., North Bend, Ohio.

Burge, Kenneth A. (D), 913 Masonic, Albany, Calif.

Burge, R. G. (C), Plant Supt., Butler Mfg. Co., 13th & Eastern Ave., Kansas City, Mo.

Burgess, Leslie U. (D), Welders' Helper, Whiting Corporation; (Res.) 15520 Lexington Ave., Harvey, Illinois.

Burgess, Norman O. (D), Foreign Y. M. C. A., 150 Bubbling Well Road, Shanghai, China.

Burggraf, Fred (C), Supt., Ellicott Machine Corp., 1611 Bush St., Baltimore, Md.

Burghoorn, L. (D), Toolmaker & Machinist, Westinghouse Electric Co.; (Res.) 468 Washington Ave., Belleville, N. J.

Burgston, C. H. (C), Metallurgist, Deere & Co., Moline, Illinois.

Burke, W. P. (C), 606 W. Saulnier St., Houston, Texas.

Burkett, W. D. (D), Welder, R. G. Le Tourneau, Inc.; (Res.) Box 163, Hanna City, Ill.

Burks, R. H. (C), Engineer, Goodman Mfg. Co.; (Res.) 618 Western Ave., Glen Ellyn, Ill.

Burnam, C. M., Jr. (B), Engineering Editor, Heating, Piping and Air Conditioning, 6 N. Michigan Ave., Chicago, Ill.

Burnett, Clarence (D), Welder, General Electric Co.; (Res.) 38 Grant Road, Swampscott, Mass.

Burnett, Robert R. (D), 15457 Euclid Ave., Suite 11, East Cleveland, Ohio.

Burns, T. V. (C), Serviceman, Applied Engrg. Dept., Magnolia Airco Prods. Co., P. O. Box 319, Houston, Texas.

Burns, Wm. T. (C), Engineer of Production, Kings County Lighting Co., 55th St. & 1st Ave., Brooklyn, N. Y.

Burque, L. A. (C), Salesman, Westinghouse Electric & Mfg. Co., 800 Lloyd Bldg., Seattle, Wash.

Burr, Louis H. (C), Production Manager, The Austin Co.; (Res.) 14729 Ardenall Ave., E., Cleveland, Ohio.

Burriss, Luther J. (C), Plant Engineer, The Weston and Brooker Co.; (Res.) 812 Abelia Rd., Columbia, S. C.

Butcher, R. L. (D), Welder, Mid-Continent Petroleum Corp.; (Res.) P. O. Box 81, Tulsa, Okla.

Butler, Harry (C), Assistant Foreman, Sun Shipbuilding & Dry Dock Co.; (Res.) 1106 Johnson St., Chester, Pa.

Buxton, M. L. (D), Longview Farm, Lee's Summit, Mo.

Byers, C. H. (C), Production Supt., The Ohio Public Service Co., Warren, Ohio.

C

Cady, George F. (F), Horton Steel Works, Fort Erie, Ont., Canada.

Cain, J. (C), Prop., Cain's Mach. Shop, Box 238, Tuscaloosa, Ala.

Caldwell, Francis S. (B), Welding Supt., Jeffrey Mfg. Co.; (Res.) 403 E. Maynard Ave., Columbus, Ohio.

Callahan, James (C), Manager, Peoria Welding Supply Co., 529 S. Adams, Peoria, Illinois.

Callahan, John F. (C), Asst. Adv. Manager, Air Reduction Sales Company, 60 East 42 St., New York City.

Callahan, J. Walter (D), Welding Instructor, Boston Trade School; (Res.) 312 Humphrey St., Lowell, Mass.

Callas, Henry (D), Welder, Link Belt Co.; (Res.) 4437 Shields Ave., Chicago, Ill.

Callery, S. S. (C), Welder, Equitable Gas Co.; (Res.) Box 242, Finleyville, Pa.

Calley, Joseph (C), Electric & Welding Engineer, Murray Corp.; (Res.) 1088 Meldrum Ave., Detroit, Mich.

Cameron, A., III (C), Salesman, Carnegie-Ill. Steel Corp., 71 Broadway, New York City.

Camp, C. L. (B), President, Camp & Lathy, Inc., 1924 Baltimore Ave., Kansas City, Mo.

Campbell, B. H. (D), Railroad Welder, 1144 Churchman Ave., Indianapolis, Ind.

Campbell, Geo. F. (D), Operator, Southwestern Eng. Co., 4800 Santa Fe, Los Angeles, Calif.

Campbell, Howard (B), Editor, Modern Machine Shop, 431 Main St., Cincinnati, Ohio.

Campbell, H. F. (C), Chief Draftsman, Pennsylvania Crusher Co., Liberty Trust Bldg., Philadelphia, Pa.

Campbell, Lorn, Jr. (A), President, Harris Calorific Co., 5501 Cass Ave., N. W., Cleveland, Ohio.

Campbell, L. W. (C), Pipe Line Supt., Memphis Natural Gas Co., Sterick Bldg., Memphis, Tenn.

Candee, E. T. (B), Technical Supervisor, The American Brass Co., Box 791, Waterbury, Conn.

Candy, Albert M. (B), Consulting Engineer, Hollup Corporation, 3357 West 47 Place, Chicago, Illinois.

Canfield, Milton E. (C), Representing Reid-Avery Co., 420 E. 3rd St., Los Angeles, Calif.

Cannon, Walter B., Jr. (D), Welder Highway Dept., 11048 Wedding St., N. Hollywood, Calif.

Canty, J. P. (C), Asst. to Engineer, B. & M. Railroad, North Station, Industrial Bldg., Boston, Mass.

Canty, T. A. (B), President, T. A. Canty, Inc., 1023 Cathedral St., Baltimore, Md.

Cape, Gordon (C), Dominion Bridge Co., Ltd., P. O. Box 280, Montreal, Que., Canada.

Capper, Chester W. (C), Welder, U. S. Engineers, 4503 A, Fort Peck, Mont.

Carbis, J. (C), Sales Engineer, Williams & Co.; (Res.) 3050 Kensington Rd., Cleveland, Ohio.

Carbon, Earl (D), Welding Supt., Forrest Producing Co., Delaware, Oklahoma.

Card, Harold S. (C), Sales Consultant; (Res.) 449 Elmora Ave., Elizabeth, N. J.

Cardwell, H. W., Jr. (B), Vice-President, Allsteel Prods. Mfg. Co., 801 S. Wichita St., Wichita, Kans.

Carewe, Patrick J. (D), Welder, Sinclair Ref. Co., Pipe Line Dept., Carrollton, Mo.

Carey, Chas D. (D), Welder, Eastman Kodak Co.; (Res.) 4329 Lake Ave., Rochester, N. Y.

Carl, James W. (B), Engineer, Simonds-Warden-White Co., 1101 Negley Place, Dayton, Ohio.

Carle, W. J. (D), Supervisor, Air Reduction Sales Co.; (Res.) 828 Belmont St., Davenport, Iowa.

Carleton, H. E. (B), Sales Engineer, Foster Wheeler Corp., Tower Bldg., Washington, D. C.

Carlin, L. C. (C), District Sales Mgr., Eastern Oxygen Company, 412 Medford Street, Boston (Charlestown), Mass.

Carlson, Chas A. (B), Wash. Manager, Lukenweld, Inc., 810 Union Trust Bldg., Washington, D. C.

Carlson, C. E. (C), Chicago Bridge & Iron Co., 1305 W. 105th St., Chicago, Ill.

Carlson, C. L. (C), Attn. Library, Crockett, Calif.

Carlson, O. M. (B), Assistant Superintendent, Dept. 4, Crane Co.; (Res.) 5819 No. Christina Ave., Chicago, Ill.

Carlson, W. W. (C), Professor of Shop Practice, Kansas State College, 1722 Laramie St., Manhattan, Kansas.

Carnac, A. J. Rivett- (B), Works Manager, British Mining Supply Co., 5 Maddison St. Jeppe, Johannesburg, South Africa.

Carney, J. E. (C), Asst. Metallurgist, Sun Oil Co., Marcus Hook, Pa.

Carpani, Peter J. (D), Electric Welder, U. S. E. D., War Dept.; (Res.) 3605 Apt. D., Fort Peck, Mont.

Carpenter, Allan W. (B), Engineer of Bridges, New York Central Railroad, Room 1004, 466 Lexington Ave., New York, N. Y.

Carpenter, B. P. (C), Sales Engineer, Hawaiian Gas Products Ltd., Box 2454, Honolulu, T. H.

Carpenter, F. H. (B), Vice-President, Pipe-Weld, Inc., 2628 S. Sacramento Ave., Chicago, Ill.

Carpenter, John (D), General Welding Inspector (Bridges), New York Central Railroad; (Res.) 13 Hazelhurst Ave., Albany, N. Y.

Carr, Earl (D), Welder, R. G. Le Tourneau, Inc.; (Res.) R. F. D. 104, Peoria, Ill.

Carr, James H. (D), P. O. Box 44, Arlington, N. J.

Carrier, L. E. (C), Combination Welder, U. S. Bureau of Reclamation; (Res.) Beachwalk Inn, Honolulu, T. H.

Carson, W. F. (C), 508 Commercial Trust Bldg., Philadelphia, Pa.

Carter, Glenn O. (A), Consulting Engineer, The Linde Air Products Co., 30 E. 42nd St., New York, N. Y.

Carver, John A. (D), Welder, Hughes Tool Co.; (Res.) 3012 Morrison St., Houston, Texas.

Casavant, Aime N. (C), Welder, Univ. of N. H. Navy Dept.; (Res.) 85 High Street, Amesbury, Mass.

Case, W. R. (C), Sales Manager, Seattle Steel Co., 1200—4th Ave. So., Seattle, Washington.

Casey, R. H. (C), Industrial Engineer, The Cincinnati Milling Machine Co.; (Res.) 7024 Grace Ave., Cincinnati, Ohio.

Cash, Lester M. (D), Ship Fitter, U. S. N., Receiving Ship, U. S. S. Seattle, New York, N. Y.

Cassells, John (B), Foreman, Mech. Dept. Aluminum Co. of Canada, Ltd., P. O. Box 4, Arvida, Que., Canada.

Cassidy, P. R. (A), Executive Assistant, The Babcock & Wilcox Co., 85 Liberty St., New York, N. Y.

Caswell, Alexis (C), Secretary and Business Manager, Manufacturers' Association of Minneapolis, Inc., 200 Builders Exchange Building, Minneapolis, Minnesota.

Catlett, James T. (C), Welding Engineer, General Electric Co.; (Res.) 21 Irving Road, Scotia, New York.

Cattanach, Robert L. (B), Welding Foreman, American Laundry Machinery Co., 110 Buffalo Rd., Rochester, N. Y.

Cavagnaro, John, Jr. (D), Welder, Consolidated Macaroni Mch. Corp., 156—6th St., Brooklyn, N. Y.

Cavanagh, R. F. (B), Supt. Engrg. Dept., The Fidelity & Casualty Co. of N. Y., 80 Maiden Lane, New York, N. Y.

Cave, Joseph (B), Works Electrician, Canadian General Electric Co., 940 Lansdowne Ave., Toronto, Canada.

Cavin, Gustave (C), Chief Mechanical Engineer, Canadian Locomotive Co., Ltd., Kingston, Ontario.

Cecil, Carl (C), Foreman of Sub. Str., R. G. Le Tourneau, Inc.; (Res.) 209 Alice Ave., Peoria, Ill.

Cecil, Robt. E. (B), Vice-President, Wm. B. Scaife & Sons Co., Oakmont, Pa.

Cella, P. J. (C), Sales Engineer, Una Welding, Inc.; (Res.) 12203 Clifton Blvd., Lakewood, Ohio.

Centers, C. (B), 3202 Whitney, Detroit, Mich.

Chaffee, W. J. (C), Sales Manager, Welder Div., Hobart Brothers Co., Hobart Square, Troy, Ohio.

Chalmer, Joseph (B), Factory Superintendent, Kaeleku Sugar Co., Hana Maui, T. H.

Chalmers, Robert F. (C), Sugar Mill Engineer, Paauhau Sugar Plantation Co.; (Res.) P. O. Box 45, Paauhau, Hawaii, T. H.

Chambers, Hugh (B), President, G. D. Peters & Co., Ltd., 1021 New Birks Bldg., Montreal, Canada.

Chambers, R. A. (B), Vice-President, Kane Boiler Works, Inc., 1001 Electric Bldg., Houston, Texas.

Champion, Pierre (A), President, The Champion Rivet Co., East 108th & Harvard Ave., Cleveland, Ohio.

Champion, Robert (D), Welder, Champion Rivet Co., E. 108th & Harvard Ave., Cleveland, Ohio.

Chan, Adolph F. (C), Welding Foreman, U. S. Engineers Fort Peck; (Res.) Nashua, Montana.

Chandler, Ezra A. (C), Field Supt., Western Pipe & Steel Co., 5717 Santa Fe Ave., Los Angeles, Calif.

Chaney, Harold P. (C), Chief Electrician, Bethlehem Shipbldg. Corp., Union Plant, San Francisco, Calif.

Chapin, Richard N. (D), Specialist, Applied Engrg. Dept., Air Reduction Sales Co., 60 E. 42nd St., New York, N. Y.

Chapman, Edward C. (B), Metallurgical Engineer, Combustion Eng. Co., Inc., Hedges-Walsh-Weidner Div., Chattanooga Tenn.

Chapman, Everett (A), President, Lukenweld, Inc., Coatesville, Pa.

Chapman, Leo (F), 10610 Scarritt, Kansas City, Mo.

Chapman, R. B. (B), Southern Pacific Ry., 65 Market St., San Francisco, Calif.

Chapman, W. D. (B), Research & Development Engr., EMF Electric Co. Pty. Ltd., 1005 Rathdown St., Nth Carlton, Melbourne N 4, Victoria, Aust.

Chappelear, J. A. (C), Manufacturers Representative, J. A. Chappelear & Son, Mills Bldg., Washington, D. C.

Charlier, Pierre (B), Ingénieur A. I. Ms.—Directeur Technique of La Soudure Electrique Autogene S. A. 58–62 Rue des Deux Gares, Bruxelles-Midi, Belgium.

Charvat, Jos. J. (D) Welder, Steel Warehouse Corp.; (Res.) 5228 So. Tolman Ave., Chicago, Ill.

Chatfield, Howard (B), Supervising Engineer, The Fidelity & Casualty Co. of N. Y., 942 McKnight Bldg., Minneapolis, Minn.

Chauncey, Clarence (D), Welder, Eastman Kodak Co.; (Res.) 59 Clayton St., Rochester, N. Y.

Cheever, H. A. (B), President, H. A. Cheever Company, 1962 First Ave. S., Seattle, Wash.

Cheney, William B. (C), Industrial Heating Eng., So. Calif. Edison Co., Ltd., Box 351, Los Angeles, Calif.

Chief Engineer, (B), P. O. Box 577, Hilo, T. H.

Chitro, John (D), Welding Instructor, New England Welding Labs., 88 St. Stephen St., Boston, Mass.

Chivvis, George D. (D), Welder, Walsh Construction Co.; (Res.) 345 Walnut St., Lindenhurst, L. I., N. Y.

Choate, R. E. (B), President, Laplant-Choate Mfg. Co., Inc., Cedar Rapids, Iowa.

Christensen, Harry (B), Asst. Sales Manager, A. M. Castle & Co., 1132 Blackhawk St., Chicago, Ill.

Christensen, L. H. (B), Welding Enginee , Reid Avery Co., Dundalk, Baltimore, Md

Christie, R. L. (B), Foreman Sheet Metal Dept., Aluminum Ore Co., 3500 Missouri Ave., E. St. Louis, Ill.

Churchard, Chas. F. (D), Arc Welder, General Electric Co.; (Res.) 228 Salem St., Wakefield, Mass.

Churchill, William W. (C), Sales Engineer, General Electric Co., (Industrial Department) Schenectady, N. Y.

Churchward, Jack (B), Churchward Engrg. Co., 33 Sperry St., New Haven, Conn.

Chyle, John J. (C), Director Welding Research, A. O. Smith Corp.; (Res.) 284 N. 51st St., Milwaukee, Wis.

Cipperly, E. J. (C), Arc Welding Specialis Industrial Heating Specialist, General Electric Co., 5201 Santa Fe Ave., Los Angeles, Calif.

Clark, Donald S. (B), Assistant Professor of Mechanical Engrg., California Institute of Technology, 1201 E. California St., Pasadena, Calif.

Clark, E. E. (D), Welder, Denver Tramway Co., 65 S. Lincoln, Denver, Colo.

Clark, Henry Wilmot (B), Indoor Assistant to the Civil Engineer, London Passenger Transport Board; (Res.) 133 Harrowdene Road, Wembley, Middlesex, Eng.

Clark, L. W. (C), Engineer, Electrical System, The Detroit Edison Co., 2000 Second Ave., Detroit, Mich.

Clark, LeRoy W. (C), Head of the Dept. of Mechanics, Rensselaer Polytechnic Institute, Troy, N. Y.

Clark, Lewis S. (C), 75 Main Ave., Clinton, Iowa.

Clark, Roger W. (C), Welding Engineer, General Electric Co., Works Lab. Bldg. 7, Schenectady, N. Y.

Clark, W. R., Jr. (B), Dist. Manager, The Linde Air Products Co., 912 Baltimore Ave., Kansas City, Mo.

Clarke, Neil A. (C), Assoc. Naval Architect, Navy Dept., Bureau of Const. & Repair, Washington, D. C.

Clausen, E. W. (C), Commonwealth Edison Co , 2233 S. Throop St., Chicago, Ill.

Claussen, Gerard (C), Research Assistant, Welding Research Committee, 29 W. 39 St., N. Y.

Clay, Phillip L. (D), Welder, Mid-Continent Petroleum Corp.; (Res.) 416 Industrial St., R. 1, Sand Springs, Oklahoma.

Cleaver, H. P. (B), Works Manager, The J. G. Brill Co., 6200 Woodland Ave., Philadelphia, Pa.

Clements, R. B. (B), Dist. Manager, The Linde Air Products Co., 1421 N. Broad St., Philadelphia, Pa.

Cleeland, Robert (C), Sales Engineer, Rubicon Co., 29 N. 6th Place, Philadelphia, Pa.

Clifford, E. (C), Chief Engineer, J. S. Thorn Co., 20th & Allegheny Ave., Philadelphia, Pa.

Clifford, Jerry (D), Welder, 91 Prince St., Rochester, N. Y.

Cline, Charles R. (D), 1327—8th Ave., New Brighton, Pa.

Clinkscale, Roy (D), Quarterman Welder, Mare Island, Navy Yard, Shop 26, Mare Island, Calif.

Close, H. C. (B), Dist. Manager, Machinery & Welder Corp., 2833 Locust St., St. Louis, Mo.

Clotfelter, R. A. (C), General Foreman, Western Pipe & Steel Co., 5717 Santa Fe Ave., Los Angeles, Calif.

Clothier, A. L. (D), Supervisor, Applied Engrg. Dept., Air Reduction Sales Co.; (Res.) 8002 Willow St., New Orleans, La.

Clough, Frank H. (C), City Engineer, City Hall, S. Pasadena, Calif.

Coakley, John A., Jr. (C), Vice-President, The Lincoln Electric Railway Sales Co., 1202 Marshall Building, Cleveland Ohio.

Coates, A. G. (C), Chief Clerk & Sales, Compressed Industrial Gases, 311 E. Third St., Tulsa, Okla.

Cochran, W. H. (C), Welding Engineer, General Electric Co.—Erie Works, Works Lab., East Lake Road, Erie, Pa.

Cochrane, A. G. (C), Supt., Arc Welding Dept., General Electric Co.; (Res.) 106 Snowden Ave., Schenectady, N. Y.

Cochran, Virgil (B), General Del., Toccoa, Ga.

Cody, Louis (D), Welder, Whiting Corp., Harvey, Ill.

Coffee, Henry Colman (D), Welder Foreman, Union Oil Co.; (Res.) 220 Montgomery St., San Francisco, Calif.

Coffin, Howard C. (C), Asst. Marine Engineer, Bureau of Marine Insp. & Navigation, Dept. of Commerce, Washington, D. C.

Cogan, L. J. (C), Salesman, Lincoln Elect. Co.; (Res.) 1938 Elston Ave., Philadelphia, Pa.

Coghlan, S. F. (B), Mech. Engineer, Metropolitan Water District of So. Calif.; (Res.) 414—9th St., Santa Monica, Calif.

Cohen, F. W. (B), Vice-President & Asst. Secretary, Metal & Thermit Corp., 120 Broadway, New York, N. Y.

Colbert, James E. (C), 237 A Beach Walk, Honolulu, T. H.

Colby, Fred C. (D), 217 College Ave., Waterville, Maine.

Cole, Amos G. (C), Chief Engineer, Standard Steel Works Co., Burnham, Pa.

Coleby, Comdr. Francis J. A. (C), Asst. Naval Attaché, British Embassy, Washington, D. C.

Colemen, Frank L. (D), Welding Operator, Truck Parts Co.; (Res.) Buckeye, Aris.

Coleman, Hazen E. (C), Foreman, Consolidated Coal Co.; (Res.) 209 S. Park Ave., Herrin, Ill.

Collins, Frank E. (D), Welder, Pacific Fruit Express Co.; (Res.) 105 Elefa St., Roseville, Calif.

Collins, F. J. (C), Asst. Sales Mgr., Air Reduction Sales Co.; (Res.) 3360 Bradford Road, Cleveland Heights, Ohio.

Collison, Thomas A. (B), Chief Mechanical Engineer, Mt. Vernon Car Mfg. Co., Mt. Vernon, Ill.

Collom, Cletus J. (B), Development Engineer, Wettronic Corp.; (Res.) 20022 Cameron, Detroit, Mich.

Colosi, Anthony (D), 2722 S. 8th St., Philadelphia, Pa.

Combs, A. L. (B), Dist. Manager, The Linde Air Prods. Co., 441 Stuart St., Boston, Mass.

Comerford, J. E. (D), Whiting Corp., Harvey, Ill.

Comfort, C. E. (B), Supt., St. Paul Structural Steel Co., 162 York St., St. Paul, Minn.

Commins, E. A. (B), Stuart Oxygen Co., 211 Bay St., San Francisco, Calif.

Conley, G. D. (D), Asst. Chief Engineer, Huey Station, Oklahoma Gas & Elec. Co., 5125 Belle Isle Ave., Oklahoma City, Okla.

Conley, Wm. V. (B), Instructor, Univ. of Rochester, River Campus, Rochester, N. Y

Connell, Fred (B), Asst. General Manager of Sales, American Steel & Wire Co., 350—5th Ave., New York, N. Y.

Connelly, Peter K. (B), President, Prest-O-Sales & Service, Inc., 29-23—40th Road, Long Island City, N. Y.

Conor, Frank (D), Master Mechanic, South City Lumber & Supply Co., Box 408, South San Francisco, Calif.

Conrad, Frank A. (B), Supt., Puget Sound Sheet Metal Works, 3631 East Marginal Way, Seattle, Wash.

Constanz, H. E. (B), S. F. Manager, Midwest Piping & Supply Co., 72 New Montgomery St., Rm. 426, San Francisco, Calif.

Conway, Frank (C), Engineer, Refrigeration Engrg. Dept., General Electric Co., Bldg. 23, Room 209, Schenectady, N. Y.

Conwell, Fred (D), Welding Shop Operator, Conwell Garage, Oxford, Kansas.

Cook, Fredk. S. (B), Pacific Coast Manager, Robt. W. Hunt Co., 251 Kearny St., San Francisco, Calif.

Cook, Marvin (B), Petroleum Engineer, Humble Oil & Refining Co., Houston, Texas.

Cook, Ray (D), Prop. Western Welders, 483—25th St., Oakland, Calif.

Cook, Willis E. (D), Electric Welder, Massey Harris Co., Bergen, New York.

Cooke, A. E. (B), Asst. Eng., Dutchess County Hwy. Dept., Court House, Poughkeepsie, N. Y.

Coombs, Anthony S. (C), Structural Engineer, The Whitney Engineering Co.; (Res.) 100 Monument St., West Medford, Mass.

Cooper, Gerald A. (D), Welder, Harnischfeger Corp.; (Res.) 727 No. 21st St., Apt. 103, Milwaukee, Wis.

Cooper, John (C), Prop., Coopers Garage, Salunga, Pa.

Cooper, J. H. (D), Welding Engineer, The Taylor-Winfield Corp., 1052 Mahoning Ave., N. W., Warren, Ohio.

Cooper, Sidney B. (B), Welding Supervisor, Gerstein & Cooper Co., 1 W. 3rd St., South Boston, Mass.

Corey, Donald H. (C), Welding Engineer, The Detroit Edison Co., 2000—2nd Ave., Detroit, Mich.

Corne, E. J. (C), Welder, Freeport Sulphur Co., Port Sulphur, La.

Cornelius, Martin P., Jr. (C), Student Executive, International Harvester Co., Inc.; (Res.) 413—48th St., Moline, Ill.

Cornwall, Francis (D), Welder, J. B. Klein Iron & Fdy. Co.; (Res.) Route 6, Box 277, Oklahoma City, Okla.

Corstorphine, John (C), Foreman Welder, Burmah Oil Corp., Khodauny, Burma. Temporary address: % Swan Hotel, Kennoway, Fifeshire, Scotland.

Coryell, Howard S. (D), Combination Welder, Vdylite Co.; (Res.) 17545 Wanda Ave., Detroit, Mich.

Costello, John J. (D), Welder Federal Shipbuilding & Dry Dock Co.; (Res.) 32 W. 52nd St., Bayonne, N. J.

Costello, Thomas A. (D), Asst. Foreman, Bethlehem Steel Co., 23 Keith St., Weymouth, Mass.

Coster, Charles H. E. (C), Welding Instructor, Worcester Boys Trade School; (Res.) 48 Houghton Street, Worcester, Mass.

Cotman, Carl A. (B), Junior Engineer, General Elect. Co.; (Res.) 7619 Whittington Dr., Parma, Ohio.

Cotton, Charles G. (C), Assistant Inspector of Boilers, Bureau of Marine Inspection and Navigation, 801 Custom House, Philadelphia, Pa; (Res.) 305 Richey Ave., West Collingswood, N. J.

Cottrell, W. P. (C), Cottrell Engrg. Co., 207 Mesnager St., Los Angeles, Calif.

Couch, W. O. (C), Supervisor Applied Engineering Dept., Air Reduction Sales Co., Post & McNinch St., Charlotte, N. C.

Cousineau, L. P. (C), Welding Supt., Marine Industries, Ltd., P. O. Box 353, Sorel P. Q., Canada.

Courtright, L. H. (B), Shop Foreman, Reed Roller Bit Co.; (Res.) 1438 Munger, Houston, Texas.

Cox, A. T., Jr. (B), Dist. Mgr., Lincoln Elect. Co., 1205—4th Ave., Moline, Ill.

Cox, Chas. N. (D), Welder, Inland Steel Co., Kennan, Wis.; (Res.) 6318 Kenwood Ave., Chicago, Ill.

Craig, E. S. (C), Sales Engineer, Harnischfeger Corp., 307 So. Front St., Memphis, Tenn.

Craig, G. (D), Welder, The Whiting Corp.; (Res.) 15311 Myrtle Ave., Harvey, Ill.

Craig, J. B. (A), Vice-President Titan Metal Mfg. Co., Bellefonte, Pa.

Cramb, D. L. (C), Welding Expert, General Electric Co.; (Res.) 507 Maxwell Avenue, Royal Oak, Michigan.

Cramer, L. L. (C), Welding Supervisor, Kansas City Structural Steel Co.; (Res.) R. F. D. 1, Box 41, Overland Park, Kans.

Cramer, W. T. (C), American Steel & Wire Co., Rockefeller Bldg., Cleveland, Ohio.

Crampton, D. K. (C), Director of Research, Chase Brass & Copper Co., Waterbury, Conn.

Crapo, Fred M. (C), V. P. & Genl. Manager, Indiana Steel & Wire Co., Muncie, Ind.

Crase, George H. (C), Engineer, Chicago Bridge & Iron Co., Room 1609, 330 Bay St., Toronto, Ontario, Canada.

Crawford, Wm. E. (A), Consulting Engineer, A. O. Smith Corp., Milwaukee, Wis.

Crawford, W. F. (C), Vice-President, The Edward Valve & Mfg. Co., Inc., 1200 W. 145th Street, East Chicago, Indiana.

Creager, H. W. (B), Sales Engr., Hendrie & Bolthoff Mfg. & Supply Co., 1635—17th St., Denver, Colo.

Crecca, John D. (C), Comdr. U. S. Navy, Bureau of Construction & Repair, Navy Dept., Washington, D. C.

Crigger, John (C), Foreman, Ford Motor Co.; (Res.) 4450 Burns Ave., Detroit, Mich.

Critchett, James H. (B), Vice-President, Union Carbide & Carbon Research Laboratories, Inc., 30 East 42nd Street, New York City.

Crocker, Thomas D. (D), Sales Student, Lincoln Electric Company, 12818 Coit Road, Cleveland, Ohio.

Crosett, A. D. (B), 512—5th Ave., New York, N. Y.

Cross, L. H. (D), Welder, Midwest Electric Arc Welding Co.; (Res.) 112 E. 18th St., Kansas City, Mo.

Crouch, Cleer (D), Wheel Dept., R. G. Le Tourneau, Inc.; (Res.) 103 Illinois St., Chillicothe, Ill.

Crouch, R. G. (C), Salesman, Wm. M. Orr Co.; (Res.) 52 Lincoln Ave., Bellevue, Pa.

Crowe, John J. (B), Manager, Apparatus Research & Development Department, Air Reduction Sales Co., 181 Pacific Ave., Jersey City, N. J.

Crowell, Tracy (C), Supt., Wilcox Crittenden & Co., Inc., Middletown, Conn.

Cryer, Chas. W. (C), Sales Engineer, Midwest Piping & Supply Co. Inc.; (Res.) 2306 Shakespeare, Houston, Texas.

Culbertson, J. L. (C), Chief Engineer, The Texas Pipe Line Co., P. O. Box 2332, Houston, Texas.

Culbertson, Russel P. (D), Welder, Bethlehem Steel Co.; (Res.) 2303 W. 138th St., Blue Island, Ill.

Cullen, David (D), Hilyard Newbold Co., Norristown, Pa.

Cullen, Lewis J. (D), 199 Beach Road, Great Kills, Staten Island, New York.

Cumberland, John C. (C), Manager, Areway Equip. Co., Room 1000, Lexington Bldg., Baltimore, Md.

Cummings, R. L. (D), Welder Leadman, Shell Petroleum Corp.; (Res.) 277—7th Street, Wood River, Ill.

Cummings, W. E. (C), Treas., National Tank & Mfg. Co., Florence Branch, Los Angeles, Calif.

Cunha, Frank (C), Dept. Supt., Hawaiian Pineapple Co., Honolulu, T. H.

Cunningham, G. (D), Welder, Western Cartridge Co., East Alton, Ill.

Cunningham, J. B. (B), Welding Foreman, American Rolling Mill Co., Ashland, Ky.

Cunningham, R. H. (B), Welding Supervisor, Newport News Shipbldg. & D. D. Co., Newport News, Va.

Currell, John W. (D), Welder, York Ice Machinery Co.; (Res.) 521—4th St., N. W., Washington, D. C.

Currie, John W. (C), Asst. Trick Foreman in Metal Shop, Eastman Kodak Co., Kodak Pk., 77 Dalston Rd., Rochester, N. Y.

Curry, Robert A. (B), % A. E. Marvin, Distributor, Armco International Corp., Barranquilla, Colombia.

Curtis, Wm. F. (C), Supt., Monterey County Shop; (Res.) 305 W. Main St., Salinas, Calif.

Curtiss, Joseph C. (D), Welder & Burner, Mather Spring Co.; (Res.) 1924 Berkshire Pl., Toledo, Ohio.

Cusson, F. J. (C), Indust. Engr., Hollup Corp., 3357 W. 47th Place, Chicago, Ill.

Cutler, Ralph W. (C), Structural Engineer, Western Pipe & Steel Co. of Calif.; (Res.) 1812 So. Raymond, Alhambra, Calif.

Czyzak, Stanley J. (C), Chemist, Una Welding, Inc., 1615 Collamer Ave., Cleveland, Ohio.

D

Dahle, Frederick B. (B), Research Metallurgist, Battelle Memorial Institute, 505 King Ave., Columbus, Ohio.

Dahlgreen, R. F. (B), Plant Supt., Lumsden & Van Stone Co., 426 First St., Boston, Mass.

Dahlquist, C. E. (C), Manager, Engrg. Dept., Butler Mfg. Co., 900—6th Ave., S. E., Minneapolis, Minn.

Dalcher, John T. (B), Consulting Engineer, 33 Rector St., New York, N. Y.

Dalcher, L. M. (C), Asst. Tech. Secy., American Welding Society; (Res.) 899 Westfield Ave., Elizabeth, N. J

Daley, P. L. (B), Metallurgical Engineer, National Tube Co ; (Res.) 727 Crescent Ave., Ellwood City, Pa.

Dall, David MacDonald (B), Works Manager, Hume Steel S. A. Ltd., P. O. Box 204, Germiston, Transvaal, South Africa.

Daly, William (D), Welder, Quaker City Iron Works; (Res.) 3420 N. 18th Street, Philadelphia, Penna.

Dana, Theron G. (C), Welder Foreman, Kellefer Mfg. Co.; (Res.) 6524c Marbrisa, Huntington Park, Calif.

Dand, Raymond (B), Welder, Watertown Arsenal; (Res.) 15 Scottfield Rd., Allston, Mass.

Daniels, Chas. J. (B), 2120 W. 110th St., Chicago, Ill.

Daniels, H. J. (D), Welder, Western Electric Co.; (Res.) 2116 W. Jackson Blvd., Chicago, Ill.

Daniels, Ludlow (C), Chief Draftsman, Ampco Engrg. Co., 815 Tower Petroleum Bldg., Dallas, Texas.

Daniels, Robert M. (C), Sales Engr., The Lincoln Electric Co., 1612 Cowart St., Chattanooga, Tenn.

Danielson, L. G. (C), Sales, Harnischfeger Corp., 20 No. Wacker Dr., Chicago, Ill.

Danner, W. A. (B), Dist. Manager, American Rolling Mill Co., 171—27th Ave., S. E., Minneapolis, Minn.

Darby, H. C. (C), Dist. Sales Manager, Inland Steel Co., 922 Walnut St., Kansas City, Mo.

D'Arcy, Raymond A. (C), Welding Inspector, Metropolitan Water Supply Comm., Boston; (Res.) 12 Harbor View Road, Nahant, Mass.

David, E. V. (B), Asst. Manager—Applied Engrg. Dept., Air Reduction Sales Co., 60 East 42nd St.; (Res.) 155 E. 49th St., New York, N. Y.

David, Wm. (C), Prop., DeLuxe Welding Co., 1131 Gray, Detroit, Mich.

Davidson, Earl H. (C), Manager, Structural & Plate Bureau, Met. Div., Carnegie-Illinois Steel Corp., 208 S. La Salle St., Chicago, Ill.

Davidson, R. W. (C), Structural Engineer, Ingalls Iron Works Co., Birmingham, Ala.

Davis, Alton F. (B), Vice-President and Secretary, The Lincoln Electric Company, 12818 Coit Road, Cleveland, Ohio.

Davis, Augustine (B), President, The Davis Welding & Mfg. Co., 1110 Richmond St., Cincinnati, Ohio.

Davis, Chester E. (B), Vice-President, Alanwood Steel Co., Conshohocken, Pa ; (Res.) 1337 DeKalb St., Norristown, Pa.

Davis, Claude E., Jr. (C), Commercial Welding Engr., 15 Main St., Fair Oaks, Pa.

Davis, Fred (D), Welding Specialist, General Electric Co.; (Res.) 34 Menlo Ave., Lynn, Mass.

Davis, F. W. (A), Metallurgist, E. B. Badger & Sons, 75 Pitts St., Boston, Mass.

Davis, Maxwell R. (B), Plant Superintendent, Hamilton Bridge Western, 195 W. First Ave., Vancouver, B. C., Canada.

Davis, Roy (D), Welder, Pomona Pump Co.; (Res.) 1472 E. Phillips, Pomona, California.

Davis, R. H. (D), Electric Arc Welder, U. S. Navy Yard; (Res.) 146 So. Lafayette St., Bremerton, Wash.

Davis, Walter E. (C), Engineer of Special Work & Welding, The Cleveland Railway Co., 600 Midland Bldg., Cleveland, Ohio.

Davis, Wilbert (D), Welder, American Hoist & Derrick Co.; (Res.) 70th St. & Sheridan Ave., Minneapolis, Minn.

Davis, William J. (C), Welder, Sun Shipbldg. & Dry Dock Co.; (Res.) 502 Huddell Ave., Box 231, Linwood, Pa.

Dawson, James K. (F), Electrical Engineer Student, Oklahoma A. & M. College, R. R. 2, Stillwater, Okla.

Dawson, J. R. (B), Research Metallurgist, Union Carbide & Carbon Research Laboratories, Inc., P. O. Box 58, Niagara Falls, N. Y.

Day, C. L. (C), Chicago Bridge & Iron Works, 165 Broadway, New York City.

Day, Geo. (C), Asst. Shop Supt., The Boardman Co., Oklahoma City, Okla.

Deacon, A. J. (B), Welding Engineer, Bethlehem Steel Co.; (Res.) 3054 Grassmere Ave., Pittsburgh, Pa.

Deas, R. R., Jr. (C), Asst. to Vice-President, American Cast Iron Pipe Co., Birmingham, Ala.

Debes, E. D. (B), Bethlehem Shipbldg. Corp., Quincy, Mass.

De Blois, Weldin (C), The Armco International Corp., Ramon Power 63, San Juan, P. R.

De Camp, Ray E. (B), Plant Eng., Consolidated Steel Corp., Box 1348, Arcade Sta., Los Angeles, Calif.

Decker, Walter J. (C), Foreman, Delco Appliance Corp.; (Res.) 59 Dickinson St., Rochester, N. Y.

Dedman, S. F. (D), Welding Operator, E. I. Du Pont Co.; (Res.) 1024 E. 2nd St., Walnut Ridge, Ark.

De Freitas, P. J. (C), Welding Foreman, Pacific Coast Engineering Company, Maritime & Warf Sts., Oakland, Calif.

De Ganahl, Carl (C), President, Fleetwings, Inc., Bristol, Pa.

De Golyer, A. G. (C), President, Vulcan Alloy Corp., 7 E. 44th St., New York, N. Y.

Delbridge, C. (C), Dist. Manager, Air Reduction Sales Co., 122 Mt. Vernon St., Upham Crs., Boston, Mass.

Delhi, L. W. (B), Manager, Western Pipe & Steel Co., 444 Market St., San Francisco, Calif.

De Lice, Peter A. (C), Foreman, Eastman Kodak Co.; (Res.) 253 Congress Ave., Rochester, N. Y.

De Long, Chas. E. (C), Specialist, Wilson Welder & Metals Co., 2423 E. 58th St., Los Angeles, Calif.

Del Vecchio, E. J. (C), Sales Promotion, The Taylor-Winfield Corporation, Warren, Ohio.

De Maille, Roland (C), Arim, 8, Avenue Gourgaud, Paris, XVII, France.

Demars, Arthur J. (C), Welding & Steel Inspector, Jackson & Mareland; (Res.) 35 Groton St., Lawrence, Mass.

Dembitsky, Thomas (B), Designer, Dominion Bridge Co., 6709 Sherbrooke St. West, Apartment 30, Montreal, Quebec, Canada.

Deming, George M. (C), Development Engineer, Air Reduction Sales Co., 181 Pacific Ave., Jersey City, N. J.

De Montigny, Lucien (D), Electric & Gas Welder, Brompton Pulp & Paper Co.; (Res.) P. O. Box 695, E. Angus, Quebec, Canada.

Dempsey, J. S. (A), President, The Buda Co., Harvey, Ill.

Dencer, F. W. (C), Assistant Division Engineer, American Bridge Co., 208 So. La Salle St., Chicago, Ill.

De Nicholas, Joseph (D), Welder, United Engrg. & Fdy. Co.; (Res.) 349 N. Truesdale Ave., Youngstown, Ohio.

Denison, F. E. (D), Welder Operator, Mid-Continent-Petroleum Corp.; (Res.) R-6-Box 71, Tulsa, Okla.

De Pew, T. E. (C), Eastern Manager, The Welding Engineer, 215 American Circle Bldg., New York, N. Y.

Deppeler, J. H. (A), Chief Engineer—Works Manager, Metal & Thermit Corp., 120 Broadway, New York, N. Y.

Derby, F. R. (C), Salesman, John A. Roebling's Sons Co. of Cal., Box 1217, Arcade Station, Los Angeles, Calif.

Derr, George M. (C), Asst. Regional Manager, Truscon Steel Co., 700 Investment Bldg., Washington, D. C.

Despain, Harry F. (D), 4540 W. 41st St., Tulsa, Okla.

Destefano, J. James (C), Assistant Welding Foreman, Diebold Safe Lock Company; (Res.) 927 Wertz Ave., S. W., Canton, Ohio.

Detlor, Leonard T. (C), 3120 Beechwood Blvd., Pittsburgh, Pa.

Dettwiler, Walter P. (B), District Sales Manager, Williams & Co., Inc., Henry & Dunlap Sts., Cincinnati, Ohio.

De Vries, Henry K. (C), Welding Engineer, Metal & Thermit Corp., 6113 Webbland Place, Pleasant Ridge, Cincinnati, Ohio.

Dewald, W. A. (C), Assistant to the Vice-President, The Lincoln Electric Co.; (Res.) 2235 Jackson Blvd., University Heights, Ohio.

Dexter, D. O. (D), 2628 Walnut, Huntington Park, Calif.

Dial, Donald (D), Operator, Massey-Harries Co.; (Res.) 44 Walnut St., Batavia, New York.

Dick, Walter G. (C), Supervisor of Welding, Dolomite Marine Corp.; (Res.) R. D. 1, Dansville, N. Y.

Dickau, Henry H. (D), 107-32—118th St., Richmond Hill, L. I., N. Y.

Dickens, W. N. (C), Safety Engr., The Coleman Lamp & Stove Co.; (Res.) 1001 Pattie, Wichita, Kansas.

Dickerson, R. D. (D), Welder, Kansas Gas & Elec. Co., Ripley Station, Wichita, Kansas.

Dickins, G. F. (C), Dist. Manager, Air Reduction Sales Co., 226 B. M. A. Bldg., Kansas City, Mo.

Dickson, R. H. (B), Vice-President, Hetherington and Berner, Inc., 701 Ky. Ave., Indianapolis, Ind.

Didday, Herman C. (C), Maintenance Foreman, Cincinnati Milling Machine Co.; (Res.) R. R. 5, Batavia, Ohio.

Diekelman, W. (D), Thornton, Illinois.

Dierckx, Jules (C), 136 West 16th Street, New York City.

Dietrich, N. M. (C), Welding Foreman, Slimp Machine Works; (Res.) Route 1, Box 973, Hawthorne, Calif.

Dietz, William F. (B), Asst. to Manager, Westinghouse Electric & Mfg. Co., 323 Washington Bldg., Washington, D. C.

Dill, Frederick H. (C), M. E. Dept., American Bridge Co.; (Res.) 619 Maple Lane, Sewickley, Pa.

Dillinger, L. (C), Owner, White Welding Service, P. O. Box 267, Torrance, Calif.

Dillman, E. E. (B), Efco, Inc., 3501 W. 11th St., Houston, Texas.

Dimelow, Harry K. (B), Works Manager, M. W. Kellogg Co., Foot of Danforth St., Jersey City, N. J.; (Res.) 639 Todt Hill Road, Dongan Hills, Staten Island, N. Y.

Dion, Ralph J. (D), Welder, Malahat Lumber Co.; (Res.) Port Renfrew, B. C.

Diserens, Ralph (C), Chief Draftsman, Cincinnati Shaper Co., Hopple and Garrard Sts., Cincinnati, Ohio.

Dishman, Chas. H. (C), Manager of Sales, Western District, Granite City Steel Co., 1104 R. A. Long Bldg., Kansas City, Mo.

Dissmeyer, Edward F. (C), Electrical Engineer, Commonwealth & Southern Corp., Jackson, Michigan.

Doan, Prof, Gilbert E. (B), Lehigh University Dept. of Metallurgical Engrg., Bethlehem, Pa.

Dobson, S. W. (C), Foreman, R. G. Le Tourneau, Inc., Peoria, Ill.

Dodd, Leslie H. (C), District Engineer, American Institute of Steel Const., Inc., 1405 Paul Brown Bldg., St. Louis, Missouri.

Dodge, Paul (D), Tool & Die Welder, De-Soto Motor Car Co.; (Res.) 14526 Welland Ave., Detroit, Mich.

Doering, Walter (D), Machinist, RCA Victor Co.; (Res.) 15 Gradwell Ave., Maple Shade, N. J.

Doherty, E. R. (C), Chief Inspector, Mutual Boiler Insurance Co. of Boston, 60 Batterymarch, Boston, Mass.

Doll, F. A. (C), Supt., Basca Mfg. Co., 3019 Roosevelt Ave., Indianapolis, Ind.

Doll, Theodore (B), Designer, Ash-Howard-Needles & Tammen, 1012 Baltimore Avenue, Kansas City, Missouri.

Donald, Russell S. (B), Sales Engineer, Thomson-Gibb Electric Welding Co., 50 Church Street, New York, N. Y.

Donaldson, J. R. (C), Chicago Bridge & Iron Works, 2919 Main St., Houston, Tex.

Donaldson, Jos. T. (B), Jr. Designer, Bethlehem Steel Co.; (Res.) 1252 Maple St., Bethlehem, Pa.

Donegan, Charles E. (C), Salesman, The Linde Air Products Corp., 912 Baltimore Ave., Kansas City, Mo.

Donegan, James F. (B), Supervising Engineer, The Fidelity & Casualty Co. of N. Y., 1505 Federal St., Dallas, Texas.

Donnelly, Chas. (C), J. M. Tull Metal & Supply Co., 285 Marietta St., N. W., Atlanta, Ga.

Donnelly, Wm. Wise (C), Shop Supt., % Atlantic Gulf & Pacific Co., Manila, P. I.

Donovan, James (B), Treas., Artisan Metal Products Inc., West St. at Sullivan Square, Charlestown, Mass.

Dormer, John A. (C), Structural Engineer, Design Div., Bureau of Yards & Docks, Navy Dept., Washington, D. C.

Dorsey, J. Richard (C), Civil Engineer, Koppers Co.; (Res.) 3012 Beverly Rd., Baltimore, Md.

Doucette, H. W. (B), Manager, Chief Instructor, New England Welding Labs. of R. I.; (Res.) 252 Aborn St., Providence, R. I.

Doud, H. P. (C), Arc Welding Specialist, General Electric Co., 700 Antoinette St., Detroit, Mich.

Douglas, G. C. (C), Sales Manager, Broadway Welding & Mach. Works, 1021 W. Broadway, Spokane, Wash.

Douglas, W. R. (C), The American Shipbldg. Co., Lorain, Ohio.

Douglass, Arthur S. (A), Construction Engineer, The Detroit Edison Company, 2000 Second Avenue, Detroit, Michigan.

Douglass, Earl E. (D), Gas & Arc Welding, A. B. Farquhar Co., Ltd.; (Res.) 291 W. Market St., York, Pa.

Dow, William G. (B), Associate Professor of Electrical Eng., University of Michigan; (Res.) 1417 Golden Ave., Ann Arbor, Mich.

Downing, H. M. (C), 432 Eighth Ave., La Grange, Ill.

Dowson, Harry (C), National Cylinder Gas Co., 205 W. Wacker Drive, Chicago, Ill.

Doyal, John P. (C), Welding Foreman, Combustion Engrg. Co.; (Res.) 662 Central Y. M. C. A., Chattanooga, Tenn.

Doyle, E. A. (B), The Linde Air Products Co., 30 E. 42nd St., New York City.

Doyle, James H. (C), Manager, Work Order Dept., Jos. T. Ryerson & Son, Inc., P. O. Box 484, Jersey City, N. J.

Drake, H. C. (B), Director of Research, Sperry Products, Inc., 1505 Willow Ave., Hoboken, N. J.

Drall, George J. (D), 1849 S. 55th Court, Cicero, Ill.

Drennon, R. E. (C), Prop., Raleigh Drennon Axle & Spring Service, 357 W. Peachtree St., Atlanta, Ga.

Dresher, C. S. (B), Welder, Greenwich Gas Co., Box No. 33, Cos Cob, Conn.

Drew, L. F. (B), President, Truck Welding Co., 739—9th Ave. No., Seattle, Washington.

Drinkwater, Wade (D), Operator, Wade's Garage, 5311 Classen Blvd., Oklahoma City, Okla.

Drinnon, O. C. (C), Draftsman, Chattanooga Boiler & Tank Co.; (Res.) 5003 St. & Elmo Ave., Chattanooga, Tenn.

Driscoll, J. M. (C), Dist. Manager, Air Reduction Sales Co., 1210 W. 69th St., Cleveland, Ohio.

Druetzler, Chas. O. (B), Welding Foreman, Electro-Motive Corp.; (Res.) 3640 Wesley Ave., Berwyn, Ill.

Drum, A. E. (D), Welding Foreman, Penn. Furnace & Iron Co.; (Res.) 109 Redwood St., Warren, Pa.

Drury, B. B., Jr. (C), National Cylinder Gas Co., 2421 Alamo St., Dallas, Texas.

Dubash, D. S. (B), Elect. Engineer, 214 Cadell Rd., Bombay, India. Temporary address: % American Express, New York, N. Y.

Dubie, O. L. (C), Foreman, R. G. Le Tourneau, Inc., Grant St., Peoria, Ill.

Du Bois, Edward (D), Welder, Fore River Shipbuilder; (Res.) 89 Norton St., N. Weymouth, Mass.

Duemmel, Frank (D), Arc Welder, Eastman Kodak Co.; (Res.) 962 Glide Street, Rochester, New York.

Duesing, Ernest (D), Foreman, Oliver Mack Co.; (Res.) R. R. 2, Grand Rapids, Mich.

Duff, Gordon (B), 2255 Sepulveda Blvd., West Los Angeles, Calif.

Duff, Ralph (C), Local Manager, Williams & Co., Inc., 31 N. Grant Ave., Columbus, Ohio.

Dukes, Marcus R. (B), Maintenance Welder, U. S. Army Engrg. Dept., Midway Island, Pacific.

Dunham, J. H. (C), Sales Representative, Phillips & Easton Supply Co.; (Res.) 207 N. Sedgwick, Wichita, Kansas.

Dunham, M. Keith (B), Chairman, National Cylinder Gas Co., 205 W. Wacker Drive, Chicago, Ill.

Dunklee, Robert E. (B), Prop., Machine & Welding Shop, R. E. Dunklee, 72 Flat St., Brattleboro, Vt.

Dunn, Oscar (D), Owner, Dunn Welding Wks., 116½ Harrison St., Taft, Calif.

Dunn, Thomas J. J. (D), Welding Foreman, Horace T. Potts Co., 500 E. Erie Ave., Philadelphia, Pa.

Dunn, U. S. (B), Chief Engineer, Pier Equipment Mfg. Co.; (Res.) R. 1, Box 49, St. Joseph, Mich.

Dunne, John J. (B), Sales Manager, Paschall Oxygen Co., 2221 S. Island Ave., Philadelphia, Pa.

Dunne, M. F. (C), Supervisor, Applied Engrg. Dept., Air Reduction Sales Co., P. O. Box 2338 Stockyard Station, Oklahoma City, Okla.

Durand, William F. (B), Prof. Mech. Engrg., Stanford University, Palo Alto, Calif.

Durham, Clyde, (D), Welding Supervisor, General Elect. Co.; (Res.) 574 Ontario St., Schenectady, N. Y.

Durkee, E. Leland (C), Resident Engineer, Mississippi River Bridge, Bethlehem Steel Co., P. O. Box 198, Baton Rouge, La.

Durstine, John E. (C), 404 N. 23rd St., Birmingham, Ala.

Duty, James M. (D), Welder, Washington Navy Yard; (Res.) 412 East Alexandria Ave., Alexandria, Va.

Dwyer, Edward (C), Manager of Welding Dept., Cook Iron Store Co., 128 St. Paul St., Rochester, N. Y.

Dyar, Hugh H. (B), Dist. Manager, The Linde Air Products Co., 1517 Superior Ave., Cleveland, Ohio.

Dye, Gil V. (B), 1109 Austin, Houston, Texas.

Dyer, Wm. (C), Structural Engineer, The Austin Company; (Res.) 1147 Carlyon Road, East Cleveland, Ohio.

Dyke, Edw. H. (C), Welding Operator, U. S. Air Corps, 66th Service Squadron, Manila, P. I.

E

Eaglesfield, R. D. (B), Partner, R. D. Eaglesfield, 300–400 So. La Salle St., Indianapolis, Ind.

Eaglesfield, R. D., Jr (C), Partner, R. D. Eaglesfield, 300–400 S. La Salle St., Indianapolis, Ind.

Earl, Lloyd R. (C), Plant Supt., Cons. Steel Corp.; (Res.) 1617 Primrose, Alhambra, Calif.

Earle, Harry J. (B), Sales Manager, Wm. D. Seymour Co., 614 S. Peters, New Orleans, La.

Eastman, Leslie E. (D), Partner, Eastman Welding Works; (Res.) 425 Irving St., Glendale, Calif.

Easton, F. S. (C), Chief Engineer, Mexico Tramways Co., Apartado 124 Bis., Mexico D. F., Mexico.

Easton, W. J. (B), President, The Phillips & Easton Supply Co., 244 S. Wichita St., Wichita, Kansas.

Eaton, Aaron L. (A), General Supt., Treadwell Const. Co., Midland, Pa.

Eaton, C. D. (B), Charge Hand Welder, J. K. Eaton & Sons Ltd., 312 Sydney Road, Durban, South Africa.

Eaves, J. M. (D), Welders Helper, Union Oil Co.; (Res.) 419 Rose Ave., Taft, Calif.

Ebashi T. (C), Welder, Yokokawa Bridge Works, Ltd., 1 Chome, Tsukimi-Cho Shiba-Ku, Tokyo, Japan.

Eckberg A. R. (B), Supt. Engr. & Maint. Shop, Eastman Kodak Co.; (Res.) 185 Shoreham Drive, Rochester, N. Y.

Ecklund Paul A. (C), Foreman, Delco Appl. Corp.; (Res.) 36 Hargrave St., Rochester, N. Y.

Eddingsaas O. G. (C), Manager, Wauwatosa Welding Shop; (Res.) 230 N. 70th St., Milwaukee, Wis.

Edelman, Howard J. (D), Welding Operator, Nazareth Steel Fabs, Nazareth, Pa.

Edelson, L. (B), Handy & Harmon; (Res.) 161 Irwin St., Brooklyn, N. Y.

Eder, F. (B), Engineer, Robert W. Hunt Co., 59 Murray St., New York, N. Y.

Edleman, L. A. (C), Welding Foreman, American Potash & Chem. Corp., Trona, Calif.

Edmondson, G. D. (C), Electric Welder, N. Y. C. R. R. Co.; (Res.) 6 Brady Pl., White Plains, N. Y.

Edmundson, C. D. (B), Engineer, Acetone Illumination & Welding Co., Ltd., P. O. Box 149, Napier, New Zealand.

Edsall, Stanley D. (C), Manager, Air Reduction Sales Co., 1116 Ridge Ave., N. S., Pittsburgh, Pa.

Edward, Edward J. (B), Chief Metallurgical Engineer, American Locomotive Co., Schenectady, New York.

Edwards, James L. (B), Consulting Engineer, H. G. Balcom Associates, 10 East 47th St., New York, N. Y.

Edwards, John (D), Welder, Lukenweld, Inc.; (Res.) Y. M. C. A., Coatesville, Pa.

Edwards, J. Preston (C), 6152 Locust, Kansas City, Mo.

Edwards, Oliver H. (C), Welding and Sheet Metal, Dept. Manager, Cincinnati Butchers Supply Co., 2145 Central Parkway, Cincinnati, Ohio.

Egan, Howard P. (C), Dist. Manager, Lincoln Elec. Co.; (Res.) 6729 Dublin Road, Worthington, Ohio.

Egger, E. J. W. (B), Service Engineer, The Linde Air Products Co., 311 Ross St., Pittsburgh, Pa.

Eirons, Ray (C), Salesman, Taylor-Winfield Corp., 13th Floor, Citizens Bldg., Cleveland, Ohio.

Eisenman, W. H. (B), Secretary, American Society for Metals, 7016 Euclid Ave., Cleveland, Ohio.

Eiwen, Chas. J. (C), Welding Engineer, U. S. Naval Gun Factory; (Res.) 1235—11th St., N.W., Washington, D. C.

Ekblad, Axel H. (D), Blacksmith Welder, Northern States Power Co.; (Res.) 3731 Quincy St. N. E., Minneapolis, Minn.

Eksergian, C. L. (B), Budd Wheel Co., 12141 Charlevoix Ave., Detroit, Mich.

Elbourne, C. H. (C), Welder, Standard Oil Co. of La; (Res.) R. R. 2, Baton Rouge, La.

Elder, Clayton Thomas (A), Salesman, Cleveland Electric Illuminating Co., 75 Public Square (Box No. 6776), Cleveland, Ohio.

Eldred, S. E. (C), Foreman, R. G. Le Tourneau, Inc.; (Res.) 1106 E. McClure Ave., Peoria, Ill.

Ellinger, Geo. A. (C), Assoc. Metallurgist, National Bureau of Standards, Washington, D. C.

Elliott, Edward (B), Assistant Principal Engineer, Pullman-Standard Car Mfg. Co.; (Res.) 6 Devens Road, Worcester, Mass.

Elliott, John E. (C), Plant Engineer, American Bridge Co., Frick Bldg., Pittsburgh, Pa.

Elliott, Joseph (D), 774 St. George Ave. Woodbridge, N. J.

Elliott, Richard B. (D), Welder, Central Engineering Co., General Delivery, Summerville, South Carolina.

Elliott, Thomas C. (D), Spot Welder, Falls Spring & Wire Co., 3339 Blaine Ave., Detroit, Mich.

Elliott, William (B), Welding Engineer, Honolulu Iron Works; (Res.) 4812A Kahala Ave., Honolulu, T. H.

Ellis, A. R. (B), President, Pittsburgh Testing Laboratory, P. O. Box 1646, Pittsburgh, Pa.

Ellis, J. J. (C), Welding Foreman, Tulsa Boiler & Mach. Co.; (Res.) 1218 S. Zunis, Tulsa, Okla.

Ellis, Richard (B), The Austin Co., 877 Dexter Horton Bldg., Seattle, Wash.

Ellison, Ralph (D), Welding Operator, Ben Sibbett Iron & Fdy. Co.; (Res.) 710 E. First, Wichita, Kansas.

Ellve, Eric (C), Engineer, A. B. Gasaccumulator, Stockholm, Lidingo, Sweden.

Elly, Robt. D. (C), Industrial Engineer, Foster Wheeler Corp.; (Res.) 409 West End Ave., Elizabeth, N. J.

Elsener, L. A. (C), District Manager, Chicago Bridge & Iron Co., 414 Rialto Bldg., San Francisco, Calif.

Elwood, E. S., Jr. (D), Testing Dept., General Elec. Co.; (Res.) 704 Smithson Ave., Lawrence Park, Erie, Pa.

Elwood, Howard (B), Box 443, Ranger, Texas.

Emery, Frank C. (C), Engrg. Dept., Air Reduction Sales Co., 2423 E. 58th St., Los Angeles, Calif.

Emery, John F., Jr. (D), Welding Operator, Spicer Mfg. Co., R. D. 3, Pottstown, Pa.

Emery, Willard (B), Supt. of Welding, Worthington Pump & Mach. Corp., Harrison, N. J.

Emig, Emery (B), Gen. Foreman, Jaeger Mach. Co.; (Res.) 469 Townsend Ave., Col., Ohio.

Emin, G. H. (B), Bridgeport Brass Co., Chicago, Ill.

Emmert, Keith (C), Welding Instructor, Fresno Technical High School, Stanis Laus & "O" St., Fresno, Calif.

Emmett, W. E. (C), District Engineer, Amer. Inst. of Steel Const., Inc., 303 Sharon Bldg., San Francisco, Calif.

Emmons, Robert H. (C), Welding Supervisor, Procter & Gamble Co.; (Res.) 7410 Clovernook Ave., Mt. Healthy, Ohio.

Enck, E. G. (C), Chemical Director, Foote Mineral Co.; (Res.) "Tail Winds," Swedesford Road, Gwynedd Valley, Pa.

Endlich, P. J. (C), Assoc. Struct. Engineer, War Dept. Q. M. C. Const. Div., Design Branch, U. S. Gov., P. O. Box 676, Portsmouth, N. H.

England, William (D), Welder, Lunkenheimer Co.; (Res.) 3958 Regent Ave., Norwood, Ohio.

Engle, A. D. (B), Director of Research, The Austin Co.; (Res.) 3978 Monticello Blvd., Cleveland Heights, Ohio.

Engler, Paul E. (B), President & Manager, The Balbach Co., Omaha, Nebr.

English, Harold W. (D), Serviceman, Air Reduction Sales Co.; (Res.) 195 Bakerdale Road, Rochester, N. Y.

English, Richard C. (C), Draftsman, Bld. 81, U. S. Navy Yard, Portsmouth, N. H.; (Res.) 123 Landseer St., West Roxbury, Mass.

English, Walter W. (C), Welding Instructor, Board of Education; (Res.) 4322 Walton Ave., Los Angeles, Calif.

Ennis, H. V. (C), Welding Supervisor, American Car & Fdry, Co.; (Res.) 661 E. 26th St., Paterson, N. J.

Enslin, E. M. (B), Supt., St. Louis Shipbldg. & Steel Co., Foot of Marceau St., St. Louis, Mo.

Ensminger, Frank P. (D), Arc Welding, A. B. Farquhar & Co., Ltd.; (Res.) 288 Cottagehill Road, York, Pa.

Enzian, R. I. (C), A. M. Byers Co., 1040 Shoreham Bldg., Washington, D. C.

Eresch, Joseph (B), Morton, Wash.

Erickson, C. J. (B), 1104½ S. Kingsley Dr., Los Angeles, Calif.

Erickson, Harry A. (B), 421 S. Mariposa St., Burbank, Calif.

Ericson, Carl B. (C), Foreman, Welding Dept., Sullivan Machinery Co., Claremont, N. H.

Erler, J. (C), Chief Metallurgist, Farrel Birmingham Co., Ansonia, Conn.

Estes, L. L. (C), 6656 Avenue O, Houston, Texas.

Etter, Harold P. (C), District Manager, Air Reduction Sales Co., 2423 E. 58th St., Los Angeles, Calif.

Evans, M. S. (C), Plant Engineer, American Car & Fdry. Co., Berwick, Pa.

Evans, W. L. (D), Asst. Supt. Boiler Shop, Foster Wheeler Corp., Carteret, N. J.

Eveleth, F. V. (C), Welder, Pitts. Piping & Equipment Co.; (Res.) 255 Lafayette Ave., Grantwood, N. J.

Everhard, E. P. (C), Vice-President, William A. Pope Co., 26 No. Jefferson St., Chicago, Ill.

Evert, Herbert W. (D), Operating Welder, Vilter Mfg. Co.; (Res.) 1840 N. Farwell Ave., Milwaukee, Wis.

Ewertz, E. H. (B), Consulting Engr., Ewerts & Reed-Hill, 29 Broadway, New York, N. Y.

Ewertz, Harold N. (B), Sales Manager, Arcos Corp., 401 N. Broad St., Philadelphia, Pa.

F

Faden, James L. (B), Electric Heating Engineer, Boston Edison Co., 39 Boylston St., Boston, Mass.

Fairchild, Frank (D), Welder, Danuser Mach. Shop, 108 W. 4th St., Fulton, Mo.

Fairchild, G. Barnett (C), Welder, Cruse-Kemper Co., Ambler, Pa.

Falconer, Archer E. (C), Civil Engineer (Structural), Navy Dept., Bureau of Yards & Docks; (Res.) 3511—22nd St. N. Arlington, Virginia.

Fancher, A. C. (D), Machinist and Welder, Ireland Machine and Foundry Co.; (Res.) 40 Bordern Ave., Norwich, N. Y.

Fantz, F. C. (B), Vice-President, Midwest Piping & Supply Co., 1450 S. Second St., St. Louis, Mo.

Fantz, Fred D. (F), 140 So. Gray, Webster Groves Branch, St. Louis, Mo.

Farmer, F. M. (B), Chief Engineer, Electrical Testing Labs., 79th St. & East End Ave., New York, N. Y.

Farnham, M. A. (C), Churchward Eng. Co., 33 Sperry St., New Haven, Conn.

Farnsworth, R. D. (D), Fuel Tank Welder, Douglas Aircraft Co.; (Res.) 3657 Boise Ave., Venice, Calif.

Farquhar, B. W. (B), Welding Engineer, Gulf Oil Co., Port Arthur, Tex.

Farr, William S. (C), Washington Representative, Air Reduction Sales Co., 1823 L St. N. W., Washington, D. C.

Farris, Howard S. (C), Welder, Keystone Steel-Wire Co.; (Res.) 100 N. 13th St., Pekin, Ill.

Fassero, Frank (C), Foreman, Edison General Elect. Appl. Co.; (Res.) 5109 W. 24th Place, Cicero, Ill.

Faulk, F. R. (C), Owner, F. R. Faulk Equipment Sales, 405 Penn Ave., Pittsburgh, Pa.

Faulkner, James H. (C), Sales Engineer, Commonwealth Edison Co., 72 W. Adams St., Room 1000, Chicago, Ill.

Fawcett, Lewis H. (B), Metallurgist, Metallurgical & Testing Div., Navy Yard, Washington, D. C.

Fay, Patrick (C), Managing Director, South of Ireland Electric Arc Welding Co., Ltd., 8 Griffith Ave., Dublin, Ireland.

Fay, Z. John (B), Engineer in Bridge Dept., New York Central R. R. Lines West; (Res.) 2148 Brown Road, Lakewood, Ohio.

Featheringham, T. J. (C), Supt., Youngstown Welding & Eng. Co., Youngstown, Ohio.

Febrey, H. H. (C), Amer. Steel & Wire Co., 350—5th Ave., New York City.

Feely, H. M., Jr. (C), Chief Engineer, Pittsburgh Tube Co., 1817 Pennsylvania Ave., Monaca, Pa.

Fehr, R. B. (C), Chief Engineer, Una Welding, Inc., 1615 Collamer Ave., Cleveland, Ohio.

Feiner, Mark A. (B), Secretary, Engineer, P. Feiner & Sons, Inc., 552 West 52nd St., New York City.

Feldhaus, Frank J. (C), Supt., Combustion Engr. Co., Heine Boiler Div., 5319 Shreve Ave., St. Louis, Mo.

Fellows, H. S. (C), Huntington Road, Brighton S. 5, Melbourne, Australia.

Feniewicz, Roy (D), Welding Instructor, % Elec. Welding Co., Torresdale Ave. & Paul St., Frankford, Philadelphia, Pa.

Fennell, Le Barron J. (C), Vice-President, New England Welding Labs., Inc., 88 St. Stephen St., Boston, Mass.

Fentress, D. Wendell (C), Engineer, Chicago Metal Hose Corporation, Maywood, Illinois.

Ferguson, J. E. (B), Manager & Engineer, The Austin Co., Box 3028, Cleveland, Ohio.

Ferguson, Samuel (B), Managing Director, Ferguson, Pailin Ltd., Buckley St., Higher Openshaw, Manchester, England.

Ferguson, William (B), Asst. Supt., Eng. Division, Travelers Indemnity Co., 700 Main St., Hartford, Conn.

Fergusson, Hugh B. (B), Civil Engineer, Director, G. A. Harvey & Co. (London) Ltd., Charlton, R. D., London, England.

Ferre, Herman (C), General Supt., Production Dept., Porto Rico Iron Works, Inc., P. O. Box 1589, Ponce, Puerto Rico.

Ferree, E. B. (B), Welder Foreman, Freeport Sulphur Co., P. O. Box 22, Port Sulphur, La.

Ferril, Paul (F), Amorita, Okla.

Fetcher, John R. (B), Welding Engineer, Aircraft Division, E. G. Budd Mfg. Co., 25th & Hunting Park Ave., Philadelphia, Pa.

Fetherston, Thomas C. (B), Manager, Publicity Division—General Publicity Department, The Linde Air Products Company, 30 East 42nd St., New York City.

Fetz, A. F. (C), 13340 Greiner Ave., Detroit, Mich.

Feyling, P. L. F. (B), Factory Manager, Whitehead Metal Products Co., Inc., 235 Bridge St., Cambridge, Mass.

Ffield, Paul (B), Materials Engineer, Bethlehem Shipbuilding Corp., 98 E. Howard St., Quincy, Mass.

Fiero, Austin (D), Electric Welder, Massey-Harris Co.; (Res.) 213 Washington Ave., Batavia, N. Y.

Filimon, Victor (F), 159 E. Lane, Columbus, Ohio.

Filker, Arthur G. (C), Partner, Reliance Welding Co., 334 N. West St., Farmington, Ill.

Findlay, Samuel N. (C), Foreman of Maintenance, National Tube Co., Lorain, Ohio.

Fink, Kamille B. (D), 2627 So. Harding Ave., Chicago, Ill.

Fischer, Michael (D), Welder, Standard Oil Co., 21 Tunnel Ave., Richmond, Calif.

Fish, Edwards R. (B), Chief Engineer, Boiler Div., Hartford Steam Boiler Insp. & Ins. Co., 56 Prospect St., Hartford, Conn.

Fish, Gilbert D. (B), Consulting Engineer, 11 West 42nd St., New York, N. Y.

Fish, J. Arthur (C), Maintenance Engineer, E. I. du Pont de Nemours, Niagara Falls, N. Y.

Fisher, A. (D), Welding Foreman, Algoma Steel Corp., Sault Ste. Marie, Ont.

Fisher, Leonard C. (C), Salesman, Aluminum Co. of America, Room 605, Southern Bldg., Washington, D. C.

Fisher, O. W. (B), Shop Supt., L. O. Koven & Bros., Inc., 154 Ogden Ave., Jersey City, N. J.

Fisher, Robt. (D), Supt., Machine Shop, Coplay Cement Mfg. Co., Coplay, Pa.

Fisher, Russell (C), Owner, Arc Welding Co., P. O. Box 4576, Oklahoma City, Okla.

Fitzgerald, F. R. (C), Mech. Engr., Pure Oil Co., 35 E. Wacher Drive, Chicago, Ill.

Fitzgerald, Robert (C), 507 Hilton Ave., Catonsville, Md.

Fitzpatrick, John (D), Welder, Baker, Smith & Co.; (Res.) 32-21—44th St., Long Island City, N. Y.

Fizzell, James L. (B), Vice-President & Treasurer, National Steel Products Co., 1611 Crystal Ave., Kansas City, Mo.

Fladland, Henry G. (D), Electric Welder, Puget Sound Navy Yard, P. O. Box 308, Bremerton, Washington.

Flanigan, J. Gordon (C), Supt., McCatheron Boiler Co.; (Res.) 1510 Norman St., Bridgeport, Conn.

Flanagan, Robert F. (C), Foreman, % R. G. Le Tourneau, Inc., Toccoa, Ga.

Flask, Joseph (B), Welding Foreman, Heltzel Steel Form & Iron Co., Warren, Ohio.

Fleiss, H. H. (B), Fabrikant, P. O. Box 276, Duisburg Rhine, Germany.

Fleming, Herbert J. (B), Master Mechanic, Bendix Products Div.; (Res.) 3101 Lincoln Way West, South Bend, Wis.

Flint, William M. (B), Assistant to the Manager, Olympic Steel Works, 151 Horton St., Seattle, Wash.

Flocke, Frank G. (B), Welding Engineer, International Nickel Co., Inc., New York, N. Y.; (Res.) 938 Woodmere Drive, Westfield, N. J.

Flodeen, Albin H. (B), Welding Foreman, International Harvester Co.; (Res.) 2416 28th St., Moline, Ill.

Floeting, Edward R. (C), Structural Steel Designer, Bureau of Construction, Board of Education; (Res.) 934 Putnam Ave., Brooklyn, N. Y.

Flohr, E. Firmin (C), Salesman, Bethlehem Steel Co., Term. Box 3147, Seattle, Wash.

Flood, E. J. (B), District Sales Manager, Page Steel & Wire Division, 400 West Madison St., Chicago, Ill.

Flood, J. P. (C), Supervisor, Applied Engrg. Dept., Air Reduction Sales Co., 2825 29th Ave., N. Birmingham, Ala.

Floyd, Walter M. (D), Welder, Chicago Bridge & Iron Co., 1500 N. 50th St., Birmingham, Ala.

Flynn, C. F. (C), Shop Metallurgist, Timkin Detroit Axle Co., 100–400 Clark Ave., Detroit, Mich.

Fogle, Earl (D), Arc Welding, A. B. Farquhar Co. Ltd.; (Res.) 223 Park Place, York, Pa.

Foley, W. J. (B), 2729 Irving Ave., Minneapolis, Minn.

Foley, Wm. J. (B), Salesman, Machinery & Welder Corp., 729 Third Ave., Moline, Ill.

Fontana, Leon F. (D), Welder, Delco Appliance Co.; (Res.) 60 Hartsdale Rd., Rochester, N. Y.

Forbes, Edward Gordon (C), Foreman, Imperial Oil Ltd.; (Res.) 237 Shepherd St., Sarnia, Ontario.

Ford, Wm. F. (D), U. S. Bureau of Reclamation, Coulee Dam, Wash.

Forgett, Ernest (D), Electric Welder, American Locomotive Co.; (Res.) 37 Craig St., Watervliet, N. Y.

Forgett, V. (C), Specialist, Applied Engrg. Dept., Air Reduction Sales Co.; (Res.) 329 Hickory St., Teaneck, N. J.

Forker, E. W. (A), Manager, Process Equipment Dept., Blaw-Knox Co., Pittsburgh, Pa.

Forseth, Geo. O. (C), Salesman, Metal and Thermit Corp., 533 South 7th St., Minneapolis, Minnesota.

Forssell, Pontus (C), Welding Engineer, Eriksbergs Mek. Verkstadsaktiebolag, Gothenburg, Sweden.

Fosler, G. S. (C), General Supt., The Gas Machinery Co., 16100 Waterloo Road, Cleveland, Ohio.

Foss, Earl H. (B), Welding Supt., Murray Corporation of America; (Res.) 7239 Kentucky Avenue, Dearborn, Mich.

Foss, F. F. (A), Asst. to Chairman & General Metallurgist, Wheeling Steel Corp., Wheeling, W. Va.

Foster, George J. (B), Manager, Canadian Office, The Quasi-Arc Co. Ltd., 32 Front St. West, Toronto, Ont., Canada

Foster, John H. (B), Foreman, Lockheed Aircraft Corp., Burbank, Calif.

Foust, J. A. (B), Instructor of Welding, Ohio State Univ.; (Res.) 2333 Neil Ave., Columbus, Ohio.

Fowler, E. W. (B), Engineer, National Bd. of Fire Underwriters, 85 John St., New York, N. Y.

Fowler, Lee M. (D), Chief Instructor, Providence School of Practical Training, 97 Fountain St., Providence, R. I.

Fox, E. R. (D), Foreman Welder, U. S. Engineers, P. O. Box 97, Memphis, Tenn.

Franer, Frank X. (D), Supt., Chattanooga Boiler & Tank Co., 1011 East Main St., Chattanooga, Tenn.

Frank, Louis C. (C), Engineer, % Trackson Co., 3333 S. Chase Ave., Milwaukee, Wis.

Frank, Martin M. (D), Assistant Foreman, Maintenance, Westinghouse Electric & Mfg. Co.; (Res.) R. F. D. 1, Mansfield, Ohio.

Frankland, F. H. (B), Chief Engineer, American Institute of Steel Construction, 101 Park Ave., New York City.

Frankland, John M. (C), Physicist, Experimental Model Basin, Washington Navy Yard, Washington, D. C.

Franks, Fred (D), Welder, Sutton Garten Co.; (Res.) R. R. 5, Box 460, Indianapolis, Ind.

Frantz, Lloyd (D), Asst. Welding Instructor, Lincoln Electric Co., Coit Rd. & Kirby Ave., Cleveland, Ohio.

Frantzen, Chell (B), Welder, Federal Shipbuilding & Dry Dock Co.; (Res.) 1368 W. Clinton Ave., Irvington, New Jersey.

Franzen, James F. (C), Supervisor, Applied Engrg. Dept., Air Reduction Sales Co.; (Res.) 5829 So. Campbell Ave., Chicago Lawn Sta., Chicago, Ill.

Fraser, O. B. J. (A), Director of Technical Service on Mill Products, International Nickel Co., Inc., 67 Wall St., New York, N. Y.

Fraser, P. V. (C), General Production Engineer, The Ohio Public Service Co., P. O. Box 6058, Cleveland, Ohio.

Fraser, Robert H. (C), Asst. Engineer, NYC Omnibus; (Res.) 251 E. 52nd St., New York, N. Y.

Frazier, Joe (D), Welder, Hild & Son; (Res.) 914 N. Oxford, Indianapolis, Ind.

Fredene, P. J. (B), Foreman, The Symington Gould Corp.; (Res.) P. O. Box 993, Rochester, N. Y.

Freeburg, W. S. (D), 1326 S. Second St., Milwaukee, Wis.

Freedman, C. F. (C), General Manager & Treasurer, American Agile Corp., 5806 Hough Ave., Cleveland, Ohio.

Freeman, Chas. S. (C), District Manager, Lincoln Electric Co., 807 Iroquois Bldg., Buffalo, N. Y.

Freeman, H. B. (B), Supt. of Shops, The Phoenix Iron Co., Phoenixville, Pa.

Freeman, Harry William (D), Apprentice, Carnegie Illinois Steel Co.; (Res.) 6347 Drexel Ave., Chicago, Ill.

Freeman, P. J. L. (C), Supt., Lucey Boiler & Mfg. Corp.; (Res.) 3101—14th Ave., Chattanooga, Tenn.

Frenzel, Wm. (D), Welding Foreman, Indianapolis Railway, 1150 W. Washington St., Indianapolis, Ind.

Fresher, George S. (B), President, Presto Battery Service, 106 Chapel St., Hartford, Conn.

Frey, Chas. A. (B), Master Mechanic, Phila. Electric Co.; (Res.) 858 N. 32nd St., Camden, N. J.

Frick, C. W. (C), Supt., Standard Steel Works, 16th & Howell, N. Kansas City, Mo.

Friebel, G. J. (C), Sales Engineer, Harnischfeger Corporation; (Res.) 12754 Northlawn, Detroit, Mich.

Friedrich, W., Dr. (B), 222 Riverside Drive, New York City.

Frierson, W. D. (C), Wilburn's Garage, 409 N. McDonough St., Decatur, Ga.

Friese, A. H. (C), President, Welding Engrg. Co., 264 E. Ogden Ave., Milwaukee, Wis.

Frink, Francis G. (B), Secretary, Washington Iron Works, 1500—6th Ave., Seattle, Wash.

Fritsch, R. E. (B), Vice-President, Tube-Turns, Inc., 224 East Broadway, Louisville, Kentucky.

Fritzsch, T. N. (C) Welding Supervisor, Carnegie-Illinois Steel Corp., Lorain Division, Johnstown, Pa.

Frohlin, John (B), Supt., Bergen Point Iron Works, Foot West S St., Bayonne, N. J.

Froman, Hugh C. (D), Maintenance & Welding, National Gypsum Co.; (Res.) Box 123, Sun City, Kansas.

Frost, L. H. (B), Welding Engineer, Electric Controller & Mfg. Co.; (Res.) 7310 Woodward Ave., Detroit, Mich.

Frost, Sargeant E. (F), Aggie Lodge, Stillwater, Okla.

Fry, John O. (C), Salesman, Earle M. Jorgensen Co., 10510 So. Alameda, Los Angeles, Calif.

Fuchs, B. L. (C), President, Fuchs Machinery & Supply Co., 1102 Farnam St., Omaha, Neb.

Fulkerson, E. V. (B), President, Acetylene Products Co., P. O. Box 552, Indianapolis, Ind.

Fuller, L. J. (B), Development Engineer, Welding Engineers, Inc., 4655 Stenton Ave., Philadelphia, Pa.

Furguson, Clyde A. (C) Asst. Metallurgist, Ladesh Drop Forge Co., Cudahy, Wis.

Fyke, F. C. (B), Materials Engineer Supt., Standard Oil Development Co., Elizabeth, N. J.

G

Gaedy, Clarence (D), Welder, 3147 Pillsbury Ave., Minneapolis, Minn.

Gage, Arthur (C), Chief Structural Engineer, Hetherington & Briner, Inc., 701 Kentucky Ave., Indianapolis, Ind.

Gagnow, Jos. O. (C), Director of Welding School, Providence School of Practical Training, 97 Fountain St., Providence, R. I.

Gailor, C. F. (C), Consulting Engineer, 50 Church St., New York City.

Gaines, John M., Jr. (B), Asst. Supt. of Lab., The Linde Air Products Co., Tonawanda, N. Y.

Galbreath, Emil (C), Distributor of Hobart Welders; (Res.) 1338 West Ave., Buffalo, N. Y.

Gallagher, Chas. J. (C), Owner of School, Electric Welding Inst.; (Res.) 3 W. Lombard St., Baltimore, Md.

Gallagher, C. S. (B), Production Manager, The Bettendorf Co., Bettendorf, Iowa.

Gallagher, S. A. (B), Manager, Liberty Coppersmithing Co., 1708 North Howard St., Philadelphia, Pa.

Gallegly, Ray (D), 343 S. Clarkson St., Denver, Colo.

Galley, John (C), Partner, Cozine & Galley Mach. Co., 320 E. 21st St., Wichita, Kans.

Gallo, Francis A. (B), Welding Technician, J. L. White Co.; (Res.) 33 Wildemere Ave., Waterbury, Conn.

Galloway, R. Theron (B), Structural Engineer, Consolidated Edison Co. of N. Y., Inc., Room 1500-A, 4 Irving Place, New York, N. Y.

Galloway, William M. (C), Welder, U. S. Engineers, 4407 Blackwater St., Fort Peck, Montana.

Gänder, Aksel (D), Oxyacetylene and Electric Arc Welder, Northern Pacific Railroad Co.; (Res.) 137 Bedford St. S. E., Minneapolis, Minnesota.

Gannett, H. E. (B), Supt. of Welding, C. B. & Q. R. R. Co., 547 Jackson Blvd., Chicago, Ill.

Gannett, J. K. (B), Vice-President & Manager, Eastern Dist., The Austin Co., 19 Rector St., New York, N. Y.

Garaventa, Ermano (C), Project Engineer, Hamilton Standard Propellers; (Res.) 57 Oak St., Manchester, Conn.

Garberson, D. D. (D), Service Operator, K. O. Lee Electric Co.; (Res.) 902 Perry St., Wichita, Kansas.

Garcya, P. (B), Departmento de Maquinarias, Ministerio de Obras Publicas, Catia-Caracas, Venezuela, S. S. A.

Gardner, E. P. S. (B), Chief Constr. Engineer, The Quasi-Arc Co. Ltd., 15 Grosvenor Gardens, Victoria, London S. W. 1, England.

Gardner, James J. (F), 16620—10th St., Columbus, Ohio.

Gardner, T. H. (B), Structural Engineer, Florida East Coast Railway, 71 Carrera St., St. Augustine, Fla.

Garner, F. R. (C), Sales Representative, The Linde Air Prods. Co., 4228 Forest Park Blvd., St. Louis, Mo.

Garrett, Gairald H. (B), Chief Engineer, The Thompson Manufacturing Co., 3001 Larimer, Denver, Colo.

Garriott, F. E. (C), Chief Metallurgist, J. D. Adams Co.; (Res.) 2702 W. Glendale Ave., Apt. 5, Milwaukee, Wis.

Garrison, Paul R. (C), Owner, Garrison Welding Service, 211 Walnut St., Ottawa, Kansas.

Garten, Wm. Ray (C), President, Sutton-Garten Co., 401 W. Vermont St., Indianapolis, Ind.

Gasiorek, John C., Jr. (C), Experimental Research in Welding, Oxweld Acetylene Co., 646 Frelinghuysen Ave., Newark, N. J.

Gassaway, Frank (D), % Southwest Ice & Dairy Co., Stroud, Okla.; (Res.) 729 West Grand, Okla. City, Okla.

Gast, R. A. (C), Engineer, The Lincoln Electric Co., Cleveland, Ohio.

Gathmann, Emil (B), President, The Gathmann Engrg. Co., P. O. Box 8, Catonsville, Md.

Gaul, Rufus H. (C), Arc Welding, Sun Shipbldg. & Dry Dock Co., (Res), 1543 Glen Avenue, Folcroft, Pa.

Gaylord, T. S. (B), Welding Supervisor, Eastman Kodak Co.; (Res.) 854 Winona Blvd., Rochester, N. Y.

Gaynor, A. E. (B), John A. Roebling Sons Co., 19 Rector St., New York, N. Y.

Geisler, Geo. L. (C), Sales Rep., The Linde Air Prods. Co.; (Res.) 75 W. Weber Road, Columbus, Ohio.

Gelderblom, A. J. (C), Engineer, Phillips Eindhoven, Holland, % L. P. Graner, 40 E. 49th St., New York, N. Y.

Gemmell, Robert D. (B), Leading Hand Welder, South African Railways Co.; (Res.) 39 Wrensch Road, Observatory, Cape Rondebosch, South Africa.

Gentry, W. W. (D), Welder, Simplicity System Co.; (Res.) 3915 Gleason Drive, Chattanooga, Tenn.

George, Stanley L. (D), Electric Welder, Chicago Bridge & Iron Co.; (Res.) Route 1, Box 74 B, Irondale, Ala.

Gergits, William (D), Electric Welder, Steel Warehousing Corp.; (Res.) 5329 So. Wells St., Chicago, Ill.

Gesner, Hobart E. (C), Welding Foreman, Farrell Birmingham Co.; (Res.) 55 Chatfield St., Derby, Conn.

Getsug, Bert (C), 1912 E. Jordan St. Pensacola, Fla.

Gettinger, Ralph (C), District Service Manager, Westinghouse Electric & Mfg. Co., 717 So. 12th St., St. Louis, Mo.

Getz, Harold (D), Machinist, Sommer Prod. Co., Peoria, Ill.

Getz, Robert (D), Machinist, Sommer Prod. Co.; (Res.) 711 W. Maywood, Peoria, Ill.

Gezelius, R. A. (B), Taylor-Wharton Iron & Steel Co., High Bridge, N. J.

Gibb, W. H. (B), Thomson-Gibb Electric Welding Co., 101 N. 33rd St., Philadelphia, Pa.

Gibbs, Louis T. (D), Welding Engineer, General Electric Co. River Works, 920 Western Ave., Lynn, Mass.

Gibson, A. E. (A), President, Wellman Engineering Co., 7000 Central Ave., Cleveland, Ohio.

Gibson, C. C. (D), Apprentice Welder, 40th Ord. Co., Raritan Arsenal, Metuchen, N. J.

Gibson, C. D. W. (C), Vice-President, Air Reduction Sales Co., 60 E. 42nd St., New York City.

Gibson, Glenn J. (C), Welding Engr., Pitts-Des Moines Steel Co.; (Res.) 120 Sprague Ave., Bellevue, Pa.

Gibson, W. Herbert (C), Consulting Engineer, 2110 Architects' Bldg., Philadelphia, Pa.

Giduz, Fred J. (D), Teacher, Rindge Tech. School; (Res.) 31 Boylston St., Jamaica Plain, Mass.

Gieszl, Ray (C), Service Manager, R. G. Le Tourneau, Inc., Peoria, Ill.

Gifford, R. A. (B) Shop Supt., Southern Boiler & Tank Works, R. F. D. 1, Raleigh, Tenn.

Gilbert, Carl R. (D), Welder, Link-Belt Co.; (Res.) 128 Bickley Ave., Glenside, Pa.

Gilbert, Jesse W. (C), Shop Planning Room, Link-Belt Co.; (Res.) Mt. Carmel Ave., North Hills, Pa.

Gilbert, John C. (D), Welder, Machinist, Gilbert's Mach. Shop, Rutland, Vt.

Gilbert, Ralph A. (C), Resistance Welding Engineer, General Electric Co.; (Res.) 91 Broadview Ter., Pittsfield, Mass.

Gilbert, Yowland (D), Welder, Jeffrey Mfg. Co.; (Res.) 2480 Indianola Ave., Columbus, Ohio.

Gilbride, J. T. (B), Wilson Welder & Metals Co., 60 E. 42nd St., New York, N. Y.

Gilbride, Thomas J. (B), Supt., Federal Pipe & Supply Co., 900 So. Campbell Ave., Chicago, Ill.

Gilcreas, Walter F. (D), Office Manager, Gas Weld Equip. Co.; (Res.) 70 Lafayette Park, Lynn, Mass.

Gill, Ed. (B), Chief Engineer, National Butane Gas Co., 1142 Union Ave., Memphis, Tenn.

Gill, Harry (B), Draftsman, Missouri Boiler & Sheet Iron Works, 908 S. 23rd St., St. Louis, Mo.

Gillespie, A. J. (B), Asst. Chief Engineer, Thew Shovel Co., Lorain, Ohio.

Gillespie, J. W. (C), Supervisor, Engineering, Air Reduction Sales Co.; (Res.) 730 Grant St., Buffalo, N. Y.

Gillette, Robert T. (C), Resistance Welding Engineer, General Electric Co., Works Laboratory, Schenectady, N. Y.

Gilliam, G. L. (F), Box 403, Shamrock, Okla.

Gilpin, W. S. (B), District Manager, Hollup Corp., 1718 Vine St., Philadelphia, Pa.

Gilson, Wm. E. (D), R. F. D. 5, Salem, Ohio.

Gleich, W. J. (D), Welder, Link-Belt Co., 6505 Stewart Ave., Chicago, Ill.

Glenn, E. M. (D), Route 11, Box 549, Dayton, Ohio.

Glenn, Mark Twain (C), Chief Inspector, Hedges-Walsh-Welder Div., Combustion Engrg. Co.; (Res.) 2420 Oak St., Chattanooga, Tenn.

Glover, Geo. M. (C), Hawaiian Gas Prods. Co., 106 Haili St., Hilo, Hawaii, T. H.

Glover, James Bolan (B), Secretary & Treasurer, Glover Machine Works, P. O. Box 85, Marietta, Ga.

Glover, Victor (F), 2914 Marborough Ave., Detroit, Mich.

Glover, W. B. (D), Foreman, Wyatt Metal & Boiler Works, Box 3052, Houston, Texas.

Glover, Walter G. (D) Welder, Massey-Harris Co., 25 Vine St., Batavia, N. Y.

Gobus, Alexander (B), Metallographist & Radiographist, Lucius Pitkin, Inc., 47 Fulton St., New York, N. Y.

Godfrey, Vincent H. (B), Assistant to General Sales Manager, Am. Chain & Cable Co., Page Steel & Wire Division, Monessen, Pa.

Godwin, J. M. (D), Foreman, Erection Dept., Goslin Birmingham Mfg. Co.; (Res.) 1620 No. 25 St., Birmingham, Ala.

Goeltz, Philip (C), Fabrication Engineer, Gleason Works, 1000 University Ave., Rochester, N. Y.

Goetz, Herman (D), Electric Welder, Central Iron & Steel Co.; (Res.) 1710 Penn St., Harrisburg, Pa.

Goldsmith, Lester M. (A), Chief Engineer, Atlantic Refining Co., 260 S. Broad St., Philadelphia, Pa.

Good, W. Phillips (B), Shop Manager, Northwestern Auto Parts Co.; (Res.) 3317 Dupont Ave. S., Minneapolis, Minn.

Gooden, Elwood O. (D), Welder, Thompson Mfg. Co.; (Res.) 1225—30th St., Denver, Colo.

Gooderham, R. M. (B), Director, The Lincoln Electric Co., Welwyn Garden City, Herts, England.

Goodford, Jack A. (D), Welder, Crucible Steel Co., 27 Ralph St., Belleville, N. J.

Goodman, Robert (B), 5115—90th St., Elmhurst, Long Island, N. Y.

Goodnick, C. C. (D), Welder, Olson Drilling Co.; (Res.) 2220 N. W. 30th, Oklahoma City, Okla.

Goodrich, Charles F. (C), Chief Engineer, American Bridge, Frick Bldg., Pittsburgh, Pa.

Goodspeed, Elvin S. (C), Welding Engineer, E. S. Goodspeed & Co., 18300 Oak Drive, Detroit, Mich.

Goodwin, J. L. (B), President, The Whitlock Coil Pipe Co., P. O. Drawer 390, Hartford, Conn.

Goolsbee, C. C. (C), Welding Engineer, Reed Roller Bit Co.; (Res.) 3402 Wichita St., Houston, Texas.

Goppoldt, P. R. (B), Asst. Civil Engr., N. Y. S. Dept. of P. W.; (Res.) 9 Shaw Avenue, Babylon, N. Y.

Gordon, Charles W. (C), Mechanical Engineer, Combustion Engineering Co.; (Res.) 697 Park Blvd., Glen Ellyn, Ill.

Gordon, John D. (A), General Manager, The Taylor-Winfield Corporation, Mahoning Avenue, Warren, Ohio.

Gordon, Leo (C), District Manager, Hobart Bros. Co., 49 D St., So. Boston, Mass.

Gore, Burney F. (C), Welding Supervisor, Standard Oil Co.; (Res.) 1733 Northern Blvd., Independence, Mo.

Gorman, Wilbur R. (D), Sales Engineer, Allis-Chalmers Mfg. Co.; (Res.) 1527 S. 60th St., West Allis, Wis.

Gornall, E. (D), Welding Supervisor, Littleford Bros.; (Res.) 33 Park Ave., Erlanger, Kentucky.

Gorzelnik, Edward M. (D), 322 Elm St., Newark, N. J.

Gosney, James (B), Head of Service Dept., Industrial Machinery Co., 641 S. W. Blvd., Kansas City, Kansas.

Gould, Leo J. (B), Asst. Chief Engineer of Construction, Bethlehem Steel Co.; (Res.) 1803 Easton Ave., Bethlehem, Pa.

Gour, Philip (D), Gas & Arc Welder, Dufresne Ing. Comp. Ltd.; (Res.) 236—2nd Ave., Noranda, Quebec, Canada.

Gove, Lewis P. (C), Engineer Mutual Boiler Ins. Co.; (Res.) Outlook Road, Wakefield, Mass.

Govi, Joe (D), Welder, Buehler Tank & Welding Works; (Res.) 6372 A Arbutus St., Huntington Park, Calif.

Gowing, J. C. (C), Distributor, Hobart Bros. Co., 644 E. Florence Ave., Los Angeles, Calif.

Grabe, C. G. (B), Chief Engineer, National Valve & Mfg. Co., 3101 Liberty Ave., Pittsburgh, Pa.

Grable, Godfrey B. (C) Engineer, Research in Welding, A. O. Smith Corp., Milwaukee, Wis.

Graf, Otto (C), Professor, Techn. Hochschule Spittlerstrasse 30 C, Stuttgart, Germany.

Graham, Arch (B), Foreman, Wyatt Metal & Boiler Works, Box 3052, Houston, Texas.

Graham, John (C), American Steel & Wire Co., Rockefeller Bldg., Cleveland, Ohio.

Graham, Marshall (B), Owner, Lance Machine & Welding Co., 208 Mill St., Watertown, N. Y.

Graham, R. R. (C), Engineer, American Bridge Co., 71 Broadway, New York City.

Graneman, F. W. (C), Supervisor, Applied Engrg. Dept., Air Reduction Sales Co., 327—25th St. S. E., Minneapolis, Minn.

Grant, C. E., Jr. (C), Supervisor, Applied Engrg. Dept., Air Reduction Sales Co., Park & Halleck Sts., Emeryville, Calif.

Grant, C. O. (D), Welder, Jaeger Machinery Co.; (Res.) 823 Gilbert St., Columbus, Ohio.

Grant, Edmund G. (C), Marine Engineer, Consolidated Steel Corp., Ltd.; (Res) 556 Alta Vista Ave., South Pasadena, Calif.

Grant, Leland E. (B), Metallurgist & Welding Engineer, Chicago, Milwaukee, St. Paul & Pac. R. R., Milwaukee Shops, Milwaukee, Wis.

Grant, R. (C), Foreman, Machine Shop, B. & L. Opt. Co., 635 St. Paul St., Rochester, N. Y.

Gratton, Yvon (C), Welding Instructor, École des Arts et Métiers de Maisonneuve; (Res.) Labelle, Qué., Canada.

Graves, E. R. (A), Welding Engineer, International Harvester Co., 180 N. Michigan Ave., Chicago, Ill.

Graves, R. L. (C), Supervisor of Welding, Union Oil Co. of Calif.; (Res.) 1930 W. 64th Place, Los Angeles, Calif.

Gray, Henry F. (C), Instructor of Mech. Engrg. M. I. T.; (Res.) 92 Packard Ave., W. Somerville, Mass.

Gray, H. W. (C), National Cylinder Gas Co.; 3965 Jennings Road, Cleveland, Ohio.

Gray, Joseph G. (C), Inspector, American Brass Co.; (Res.) 133 No. Main St., Ansonia, Conn.

Graybill, J. S. (C), Welder & Burner, M. H. Pagenhart & Co.; (Res.) Leola, Pa.

Grebe, Karl A. (C), Welding Supervisor, Lehigh Structural Steel Co., Foot of Allen St., Allentown, Pa.

Green, George J. (B), Engineer, Pittsburgh Welding Corp.; (Res.) 266 Traymore Ave., Pittsburgh, 16, Pa.

Green, G. W. (B), Northwestern Utilities, Edmonton, Alberta, Canada.

Green, V. Wayne (C), New England Representative, Taylor-Winfield Corp.; (Res.) 340 Main St., Worcester, Mass.

Greene, P. C. (B), Welding Engineer, Norfolk Navy Yard; (Res.) 203 Maryland Ave., Apt. 2, Portsmouth, Va.

Greene, T. W. (B), Welding Engineer, The Linde Air Products Co., 30 E. 42nd St., New York, N. Y.

Greenewald, E. L. (B), Patent Attorney, Union Carbide & Carbon Res. Labs., 30 E. 42nd St., New York, N. Y.

Greenfield, Clyde (D), Welding, R. G. Le Tourneau; (Res.) 216 Wisconsin, Peoria, Ill.

Greenspon, A. (C), Treasurer, Jos. Greenspon & Son Pipe Corp., National Stock Yards, St. Clair Co., Ill.

Greer, W. H. (C), Construction Inspector, Southwestern Laboratories; (Res.) 1206½ Preston Ave., Houston, Texas.

Gregory, E. T. (C), 2458 Queenston Road, Cleveland Heights, Ohio.

Griesel, George (B), Insurance Engineer, Fidelity & Casualty Co. of N. Y., 530 Pierce Building, St. Louis, Mo.

Griffith, Harry S. (D), Welder, U.S. Engineers, Box 245, Wheeler, Mont.

Griffith, Howard A. (D), Welding Supervisor, Lenape Hydraulic Pressing & Forging Co.; (Res.) 112 Sharpless St., West Chester, Pa.

Griffith, W. G. (C), Manager, Philadelphia Branch, National Cylinder Gas Co., 1300 Pine St., Camden, N. J.

Griffith, Walter M. (D), Welder, Griffs Welding Shop, 634 E. Lincoln Highway, Coatesville, Pa.

Grimshaw, G. E., Jr. (C), Student Engineer, Combustion Engrg. Co., 1032 W. Main St., Chattanooga, Tenn.

Griser, John M. (B), Vice-President, Alabama Drydock & Shipbldg. Co., Mobile, Ala.

Grissom, E. L. (D), Electric & Gas Welder, Milton Bowers Welding Co.; (Res.) 1110 Azalia, Memphis, Tenn.

Groebler, Dr. Ing Hans (C), Adolf Hitlerstr. 41, Finsterwalde, N. L., Germany.

Grohs, Wm. H. (B), Distributor, Am. Chain Co., Page Steel & Wire Div.; (Res.) 1378 Blair St., St. Paul, Minn.

Grot, Arnold S. (C), Metallurgist, Taylor Forge & Pipe Works; (Res.), 4346 W. Barry, Chicago, Ill.

Grove, Wm. G. (B), Professional Engineer—Bridges; (Res.) 409 Harrison Ave., Westfield, N. J.

Grover, LaMotte (C), Structural Welding Engineer, Applied Engineering Dept., Air Reduction Sales Co., 60 E. 42nd St., New York, N. Y.

Grow, H. J. (C), 1 Winthrop Terrace, East Orange, N. J.

Grubbs, Charles F. (D), Welder, Mid-Continent Petroleum Co.; (Res.) General Delivery, West Tulsa, Okla.

Grubbs, Vernon (D), Welding Operator, National Tank Co.; (Res.) 1016½ N. Delaware, Tulsa, Okla.

Gruenberg, J. W. (D), 16 West Second St., Dunkirk, New York.

Grundell, Leonard E. (C), Chief Inspector, Hartford Steam Boiler Insp. & Ins. Co., 114 Sansome St., San Francisco, Calif.

Guillot, A. H. (A), Roadway Engineer, New Orleans Public Service, Inc., New Orleans, La.

Guillot, Edward (B), District Manager, The Linde Air Products Co., 282 Spring St., N. E., Atlanta, Ga.

Guirl, H. P. (C), Engineer, The Superheater Co., East Chicago, Ind.

Gumm, Eugene C. (C), Salesman, Air Reduction Sales Co., R. 4 Box 254, Louisville, Ky.

Gusho, Joseph (D), Foreman, Lakeside Bridge & Steel Co.; (Res.) 4673 N. 7th St., Milwaukee, Wis.

Gust, Louis E. (C), Service Operator, The Linde Air Products Co., 828-32 Howard Ave., New Orleans, La.

Gustafson, Arthur R. (B), Proprietor, Moline Welding Service; (Res.) 840—3rd Ave., Moline, Ill.

Gwynne, G. R. (C), Chile Exploration Co., Chuqicamata, Via Antofagasta, Chile, S. A.

H

Haag, J., Jr. (C), President, Todd Construction Equipment Co., 1 Broadway, New York, N. Y.

Haas, Robt. E. (C), Sales, Lincoln Electric Co., P. O. Box 1350, Warren, Ohio.

Habel, B. R. (C), District Engineer, Ramapo Ajax Div. of The American Brake Shoe & Foundry Co.; (Res.) 200 Gage Road, Riverside, Ill.

Habel, Edward G. (C), Assistant Foreman, Eastman Kodak Co.; (Res.) 2603 Mt. Reed Blvd., Rochester, N. Y.

Haertlein, Albert (B), Associate Professor of Civil Engineering, Harvard University, Pierce Hall, Oxford St., Cambridge, Mass.

Haggard C. R. (D), Operator, Phelps Dodge Corp., Ajo, Ariz.

Haggard, Henry W. (C), Supervisor, Air Reduction Sales Co., BMA Bldg., 215 W. Pershing Road, Kansas City, Mo.

Hagstrom, T. (B), Mechanical Engineer, F. L. Smidth & Co., 225 Broadway, New York, N. Y.

Hahn, Fred C. (B), Proprietor, Capitol Welding Co., 246 Aborn St., Providence, R. I.

Hahn, Stephen J. (C), City Line Welding Co., 80 Hathaway St., Providence, R. I.

Haile, William M. (B) Manager, The Linde Air Products Co., 30 E. 42nd St., New York, N. Y.

Hainer, Linton (F), Iowa State College, Mechanical Engineering Department, Ames, Iowa.

Haines, Mark (B), Owner, Mark Haines Welding Shop, Box 457, Santa Rosa, Calif.

Haislup, Geo. F. (C), Metallurgist, Electric Steel Castings Co.; (Res.) 5032 E. 10th St., Indianapolis, Ind.

Halas, John (D), Blacksmith & Welder, Halas Welding & Forging, 910 W. Pratt St., Baltimore, Md.

Hale, I. J. (D), Boiler Room Engineer, Okla. Gas & Electric Co., Harrah, Okla.

Hale, R. S. (C), 3869 Alabama Ave., S. E., Washington, D. C.

Hall, A. J. (D), Arc & Acetylene Welder, Imperial Oil Ltd.; (Res.) 272 Wellington St., Sarnia, Ont., Canada.

Hall, J. A. (B), Engineer of Plate Construction, Kansas City Structural Steel, Kansas City, Kansas.

Hall, Paul B. (B), Manager Welding Divison, J. D. Adams Co.; (Res.) 2468 Madison Ave., Indianapolis, Ind.

Hall, Preston M. (B), President, Taylor-Hall Welding Corp., 99 Hope Ave., Worcester, Mass.

Hall, R. E. (C), Chief Draftsman, Wyatt Metal & Boiler Works, Box 3052, Houston, Texas.

Hall, Richard F. (D), Electric Welder, York Safe & Lock Co.; (Res.) 651 Colonial St., York, Pa.

Hall, Thomas C. (C), Welder, Bahrain Petroleum Co. Ltd., Bahrain Island, Persian Gulf, Persia.

Hallen, M. (B), President, Hallen Welding Service, Inc., 4524—37th St., Long Island City, N. Y.

Haller, P. Alfred (C), Owner of Welding School, Haller Welding School, 522 Bergen St., Brooklyn, N. Y.

Halliburton, M. R. (C), In Charge of Navy Qualification Tests, Federal Ship Building & Dry Dock Co.; (Res.) 1136 Hillside Ave., Plainfield, N. J.

Hallum, J. D. (B), Secy.-Treas., Seattle Oxygen Co., 5510 E. Marginal Way, Seattle, Wash.

Halsey, Wm. D. (B), Asst. Chief Engineer, Boiler Division, Hartford Steam Boiler Inspection & Insurance Co.; (Res.) 44 Westland Ave., W. Hartford, Conn.

Hamby, P. W. (D), Welder, White Eagle Oil Co.; (Res.) % Castle Hotel, Omaha, Nebr.

Hamer, T. M. (C), Industry Survey Specialist, Applied Engrg. Dept., Air Reduction Sales Co.; (Res.) 18 Vinton St., Long Beach, N. Y.

Hamill, Thomas E. (C), Metallurgist, Naval Gun Factory, Washington, D. C.

Hamilton, Chas. J. (D), Welder, Canadian Oil Co.; (Res.) Pearl St., Petrolia, Ontario, Canada.

Hamilton, Chester B. (C), President, Hamilton Gear & Machine Co., 76 Van Horne St., Toronto 4, Ont., Canada.

Hamilton, V. E., Jr. (B), General Supt., Phoenix Eng. Corp., Box 1702, Houston, Texas.

Hamlet, L. M. (C), Supt. of Shop, St. Joseph Structural Steel Co., 8th & Atchison St., St. Joseph, Mo.

Hammett, Fred W. (B), General Sales, Frick-Reid Supply Corp., Tulsa, Okla.

Hammett, H. R. (B), Manager, Western Pipe & Steel Co., Box 1706, Bakersfield, Calif.

Hammon, Geo. L. (D), Manager, National Welding Equipment Co., 223 Main St., San Francisco, Calif.

Hammond, A. H. (D), Welder, J. D. Pittman Tractor Co.; (Res.) 4025—39th Ave. No., Birmingham, Ala.

Hammond, Charles F. (C), Metallurgist, Winchester Repeating Arms Co., New Haven, Conn.

Hammond, W. R. (B), Salesman, Williams Hardware Co., P. O. Box 540, Minneapolis, Minn.

Hampton, G. J. (C), Foreman, R. G. Le Tourneau, Inc.; (Res.) 805 Monroe St., Peoria Heights, Ill.

Hampton, H. D. (D), Welding Instructor, Ordnance Dept., U. S. A., Raritan Arsenal, Metuchen, N. J.

Hampton, J. H. (D), Relief Operator, Okla. Gas & Elec. Co.; (Res.) 817½ N. W. 11th, Oklahoma City, Okla.

Hampton, Wm. T. (C), Welder, U. S. Arsenal, Rock Island, Ill.; (Res.) 809 West 6th St., Davenport, Iowa.

Hand, Everett L. (B), Methods Engineer, Gleason Works, 1000 University Ave., Rochester, N. Y.

Handley, A. B. (D), Welder, Goslin Birmingham Mfg. Co.; (Res.) 11-17—23rd St. No., Birmingham, Ala.

Hanley, John L. (B), Foreman, A. Lucas & Sons; (Res.) 1007 S. Jefferson Ave., Peoria, Ill.

Hanna, Richard (C), Salesman, Stuart Oxygen Co., 2217 Scott St., San Francisco, Calif.

Hansen, Carl (D), Welding Leader, Electro-Motive Corp.; (Res.) 3701 Wesley, Berwyn, Ill.

Hansen, George (C), Foreman Welder, Honolulo Iron Works; (Res.) 1427 Whitney St., Honolulu, T. H.

Hansen, Hans (C), 318 Washington St., Toledo, Ohio.

Hansen, K. L. (A), Consulting Electrical Engineer, 2916 N. Prospect Ave., Milwaukee, Wis.

Hansen, Ralph E. (D), Welder, 2916 N. Prospect Ave., Milwaukee, Wis.

Hansen, Roy E. (C), 1924 Compton Ave., Los Angeles, Calif.

Hanson, Carl R. (D), 11322 Forest Ave., Chicago, Ill.

Hanson, Vernon E. (C), Welder, Keystone Steel & Wire Co.; (Res.) 318 W. Lake, Peoria Hts., Ill.

Hanson, Vernon, Jr. (D), Operating Welder, Keystone S. & W. Co., Peoria, Ill.

Harber, G. P. (B), Proprietor, Harber Welding Works, Route 1, Albany, Ore.

Harbin, Peter L. (D), Welder, Commonwealth Edison Co.; (Res.) 5340 W. 24th Place, Cicero, Ill.

Harcourt, Robert H. (D), Instructor, Stanford Univ.; (Res.) 918 Cowper St., Palo Alto, Calif.

Hardbarger, Lyle H. (C), Special Welding, R. G. Le Tourneau, Inc.; (Res.) 1702 N. Madison St., Peoria, Ill.

Hardy, J. B. (B), Design Engineer, Southern California Gas Co.; (Res.) 839½ So. Hobart Blvd., Los Angeles, Calif.

Hare, L. Fletcher (D), Proprietor & Welder, Lincoln Welding & Mach. Co., 800 Helena Ave., Helena, Mont.

Hare, M. P. (C), Dist. Manager, Kane Boiler Works, Inc.; (Res.) 401 Atlantic St., Corpus Christi, Texas.

Harkins, Robert (D), Operator, Mid-Continent Petroleum Co., Tulsa, Okla.

Harmon, W. R. (C), Welder, Freeport Sulphur Co., Port Sulphur, La.

Harp, L. M. (D), Boiler Maker, Mid-Continent Petroleum Corp., 1110 N. Detroit, Tulsa, Okla.

Harris, A. M. (B), Supt., Littleford Bros., 453 E. Pearl St., Cincinnati, Ohio.

Harris, A. W. (B), Plant Engineer Western Cartridge Co., Box 85, E. Alton, Ill.

Harris, Hugh H. (C), Foreman, John Deere Harvester Co.; (Res.) 2216—5th Ave., Moline, Ill.

Harris, James C., Jr. (B), 748 E. Venango St., Philadelphia, Pa.

Harris, John E. (C), Supt., Electrode Dept., The McKay Co., Grantley Road, York, Pa.

Harris, Lee W. (B), General Supt., Link-Belt Co., 2045 W. Hunting Park Ave., Philadelphia, Pa.

Harrison, E. F. (D), Welder, General Petroleum Corp., 218 Olive Ave., Taft, Calif.

Harrison, E. R. (C), Instructor, Welding & Heat Treating, Georgia School of Tech., Atlanta, Ga.

Hart, F. H. M. (C), Frederick Hart & Co., Poughkeepsie, N. Y.

Hart, Otto (B), President, Hart Welding Supply Co., 409 W. California, Oklahoma City, Okla.

Hartman, H. I. (B), Des. Engr., S. Morgan Smith Co., 638 W. King St., York, Pa.

Harvey, S. M. (C), Owner, Electric Welding Machine Co., 1440 E. Larned St., Detroit, Mich.

Haskel, Ben (C), Welder, Dilts Machine Works; (Res.) Care of Lewis Hotel, Fulton, New York.

Hasler, Thomas (B), President, Wilson Welder and Metals Co., Inc., 60 E. 42nd St., New York, N. Y.

Haslinger, Wm. (D), Operator, A. O. Smith Corp.; (Res.) 771 N. Cass St., Milwaukee, Wis.

Hasse, Frank C. (B), General Manager, The Oxweld Railroad Serv. Co., 230 N. Michigan Ave., Chicago, Ill.

Hastings, B. F. (C), District Engineer, American Institute of Steel Construction, 1717 Chestnut St., Room 308, Philadelphia, Pa.

Hatke, H. (D), 15203 Wood St., Harvey, Ill.

Haugh, Thomas M. (D), Electric Welder, York Safe & Lock Co.; (Res.) 703 Hallam St., Wrightsville, Pa.

Haughton, F. A. (C), Consulting Mechanical, General Electric Co.; (Res.) 1033 Avon Road, Schenectady, N. Y.

Havens, Harry L. (B), President, Havens Structural Steel Co., 1713 Crystal Ave., Kansas City, Mo.

Havens, Wm. W. (C), Box No. 5, Tremont Sta., New York, N. Y.

Hawkins, Jesse N. (C), Salesman, Air Reduction Sales Co.; (Res.) 1812 Eastern Pkway., Louisville, Ky.

Hawley, A. L., Jr. (C), Salesman, Columbia Steel Co., 1426 Russ Bldg., San Francisco, Calif.

Hawley, R. G. (B), Weld Foreman, Oilgear Co.; (Res) 1550 W. Mineral St., Milwaukee, Wis.

Hayden, Joe (D), Welding Operator, R. G. Le Tourneau, Inc.; (Res.) 2005 N. Adams, Peoria, Ill.

Hayes, Anson (B), Director, Research Laboratories, The American Rolling Mill Co., Middletown, Ohio.

Hayes, George, Jr. (D), Welder, C. C. Mo. Oil Co.; (Res.) Box H, Fellows, Calif.

Hayes, William M. (C), District Manager, Air Reduction Sales Co., 7991 Hartwick St., Detroit, Mich.

Haygood, Jas. W. (B), Service Supervisor, The Linde Air Products Co., 311 Ross St., Pittsburgh, Pa.

Haynes, A. R. (C), A. P. I.-A. S. M. E. Inspector, Gen. Petroleum Corp.; (Res.) 1029 So. Montebello Blvd., Montebello, Calif.

Haynes, Hanford (B), President, Commercial Boiler Works, 53 West Lander St., Seattle, Wash.

Haynes, John H. (D), Fitter & Welder, Youngstown Welding & Engrg. Co., P. O. Box B, West Side Station, Youngstown, Ohio.

Hays, Athel V. (D), Welding Foreman, Sinclair Refining Co.; (Res.) Harrison Ave., Givinhurst, Wilmington, Del.

Hays, D. S. (B), Partner, Hays Supply Co., 269 S. Front St., Memphis, Tenn.

Hays, G. B. (B), Hays Supply Co., 269 S. Front St., Memphis, Tenn.

Hayward, C. L. (B) Salesman, Williams & Co., P. O. Box 540, Minneapolis, Minn.

Haywood, James E. (B), Metallurgist & Chief Welder, Pan American Airways Inc., The Western Division, Brownsville, Texas.

Heacock, E. E. (B), 709 E. Windsor Road, Glendale, Calif.

Headrick, B. C. (B), Master Mechanic, Kansas Gas & Electric Co., Ripley Station, Wichita, Kansas.

Hearn, W. W. (C), Foreman, The Iraq Petroleum Co., Box 309, Haifa, Palestine.

Hebert, Roy F. (D), Electrical Welder, T. & N. O. Ry. Co., 324 S. P. Bldg., Houston, Texas.

Heckman, Homer H. (C), 14048 Welland Ave., Detroit, Mich.

Hecox, Emory (F), 1569 Lincoln Road, Columbus, Ohio.

Heddell, Douglas (C), Junior Mech. Engr., Bureau Water Works & Supply, 410 Ducommun St., Los Angeles, Calif.

Hedrick, Leslie C. (D), Welder, Michigan State College; (Res.) 127 Center St., East Lansing, Mich.

Heffner, Edwin J. (C), District Manager, Scully Steel Products Co., 1394 E. 39th St., Cleveland, Ohio.

Heilman, A. B. (C), Welding Foreman, Read Machine Co., Inc.; (Res.) 189 S. Highland Ave., York, Pa.

Heiman, Raymond M. (C), 214 Cedar Ave., Peoria, Ill.

Heinle, W. C. (B), Chief Engineer, The Cleveland Crane & Eng. Co., Wickliffe, Ohio.

Heinmiller, P. M. (C), Supt., Utility Trailer Mfg. Co., Box 1407, Arcade Annex, Los Angeles, Calif.

Heitman, Chas E. (B), Chief Electrical Engineer, Edw. G. Budd Mfg. Co.; (Res.) School House Road, Blue Bell, Montgomery County, Pa.

Heldermon, J. E. (D), Inspector, Black, Sivalls & Bryson, Inc.; (Res.) 1108½ So. W. 29th St., Oklahoma City, Okla.

Heller, J. M. (C), Supervisor, Applied Engineering Dept., Air Reduction Sales Co.; (Res.) 121 Columbia Ave., North Bergen, N. J.

Heller, R. W. (B), Director, Duquesne Light Co., 435 Sixth Ave., Pittsburgh, Pa.

Hellstern, Earl (D), Welder, Truman Bowen Co.; (Res.) Barracks 4901, Fort Peck, Montana.

Helm, David F. (C), Fellow, Mellon Inst., Industrial Research, Pittsburgh, The McKay Co., Grantley Road, York, Pa.

Helmick, Robert J. (C), Supt., Helmick Foundry-Machine Co., Box 1159, Fairmont, W. Va.

Helmkamp, R. F. (C), Specialist, Applied Engrg. Dept., Air Reduction Sales Corp., 60 E. 42nd St., New York, N. Y.

Helper, H. H. (B), District Manager, The Linde Air Products Co., 828–32 Howard Ave., New Orleans, La.

Helsel, W. G. (B), Manager, Pullman-Standard Car Manufacturing Co., P. O. Box 71, Butler, Pa.

Heltzel, Carl (B), Engineer, Heltzel Steel Form & Iron Co., Warren, Ohio.

Heltzel, J. N. (B), President, Heltzel Steel Form & Iron Co., Warren, Ohio.

Helvey, Oren C. (C), Serviceman, Air Reduction Sales Co.; (Res.) 1528 N. Topeka, Wichita, Kansas.

Henderson, H. F. (C), Boiler Shop Foreman, The Portland Co., 58 Fore St., Portland, Maine.

Henderson, L. P. (C), Salesman, Lincoln Electric Co., 866 Folsom St., San Francisco, Calif.

Henderson, Robert (C), 9902 Heath Ave., Cleveland, Ohio.

Hendricks, M. S. (C), Promotion Manager, The Welding Engineer, 506 So. Wabash Ave., Chicago, Ill.

Henegar, Herbert B. (C), Chief Engineer, Tennessee Copper Co., Copperhill, Tenn.

Henke, E. J. (B), President, American Electric Fusion Corp., 2610 Diversey Ave., Chicago, Ill.

Henley, Richard (D), Welder, Bethlehem Steel Co., Fore River Plant, Quincy, Mass.

Henn Philip F. (C) Manager, Cedar Welding Service, Inc., 3732 Cedar Ave., Cleveland, Ohio.

Hennessey, John W. (C), Supt., York Safe & Lock Co., York, Pa.

Henricks, Harold H. (B), Vice-President, Charge of Manufacture, Youngstown Steel Door Co., Youngstown, Ohio.

Henriques, H. F. (C), Sales Manager, Air Reduction Sales Co., 60 East 42nd St., New York, N. Y.

Henry, J. Robert (B), Welding Process Engineer, Clearing Machine Corp.; (Res.) 38 No. Ashland, La Grange, Ill.

Henry, Lester H. (C), Process Engineer, Frigidaire Div., General Motors Corp., 440 Wiltshire Blvd., Dayton, Ohio.

Henry, Michael O. (D), Electric Welder, York Safe & Lock Co.; (Res.) 747 Fremont St., Lancaster, Pa.

Henshaw, Richard A. (B), 1409 Stratton Ave., Nashville, Tenn.

Henson, Vergil A. (D), Welder, U. S. Engineers; (Res.) 1630 So. Wellington St., Memphis, Tenn.

Heppel, Fred C. (C), Vice-President, National Cylinder Gas Co., McCall Bldg., Memphis, Tenn.

Herbert, Frank R. (C), Salesman, Edgecomb Steel Co., D St. Below Erie Ave., Philadelphia, Pa.

Herlt, Paul A. (C), Welder, West Virginia Pulp & Paper Co.; (Res.) 1301 Washington Ave., Tyrone, Pa.

Heron, S. D. (B), Research Engineer, Ethyl Gasolene Corp., 723 E. Milwaukee Ave., Detroit, Mich.

Herren, George S. (B), Welding Supervisor, The Seagrave Corp., Columbus, Ohio.

Herrick, Clayton B. (C), Sales Representative, Lincoln Electric Co.; (Res.) 628 So. Pershing Ave., York, Pa.

Hershey, Donald (C), Resident Manager, Ingalls Iron Works Co., Verona, Pa.

Herzstein, Albert (B), Secretary, Houston Oxygen Co., 505 M. & M. Bldg., Houston, Texas.

Heseltine, A. (B), Chief Inspector, Electro-Motive Corporation; (Res.) 345 So. Kensington Ave., La Grange, Ill.

Hess, E. A. (C), Welding Engineer, The Lincoln Elec. Co., 600 W. 41st St., Chicago, Ill.

Hess, Wendell, F. (B), Associate Professor of Metallurgical Engineering, Head, Welding Laboratory, Rensselaer Polytechnic Institute, Ricketts Laboratory, Troy, New York.

Hessdoerfer, Geo. M. (C), Sales Engineer, American Electric Fusion Corp., 117 N. 3rd St., Phila., Pa.

Hester, H. N. (C), District Manager, Hollup Corp., 80—26th St., Pittsburgh, Pa.

Hettelsater, Henry C. (B), Chief Engineer-Excavator Div., Harnischfeger Corp., 4400 W. National Ave., Milwaukee, Wis.

Hettler, Wm. M. (C), Union Carbide Co., Niagara Falls, N. Y.

Heudelot, C. J. (C), Science et Industrie S. A., 29 Rue de Derri, Paris, 8, France.

Hewett, Rexford J. (D), 100 Armagh Drive, Baltimore, Md.

Hexter, Irving B. (A), President, Industrial Publishing Co., 812 Huron Road, Cleveland, Ohio.

Hibbard, F. Gardiner (C), Assistant Engineer of Way & Structures, The Milwaukee Electric Ry. & Transport Co., Transport Bldg., 940 W. St. Paul Ave., Milwaukee, Wis.

Hicks, Gus H. (D), R. F. D. 5, Box 555, Louisville, Ky.

Hicks, Jesse F. (D), Rate Setter, General Electric Co.; (Res.) 2101 Colgate Place, Schenectady, N. Y.

Hiecke, W. K. (C), Raritan Arsenal, Metuchen, N. J.

Hiemke, Hugo W. (B), Sr. Materials Engineer, Bureau of Engineering, Navy Dept., Washington, D. C.

Higbee, Clarence W. (A), Manager, Wire Sales, U. S. Rubber Product Co., 1790 Broadway, New York, N. Y.

Higgins, E. C. (B), Marine Engineer, Higgins Industries, Inc.; (Res.) 1755 St. Charles Ave., New Orleans, La.

Higgins, Nathan B. (B), Chief Engineer, Penna. Water & Power Co., 1611 Lexington Bldg., Baltimore, Md.

Hila, John (D), Layout Man, Foster Wheeler Corp.; (Res.) 46 Wheeler Ave., Carteret, N. J.

Hilberg, Fred E. (B), Madison Ave., Painesville, Ohio.

Hilbinger, C. N. (C), Sales Engineer, T. A. Canty, Inc., 1023 Cathedral St., Baltimore, Md.

Hile, Leo (D), Welder, Phillips Petroleum Co.; (Res.) General Delivery, Borger, Texas.

Hiles, R. E. (C), Asst. Structural Engineer, U. S. Bureau of Yards & Docks, Navy Dept.; (Res.) 2118 Key Blvd., Apt. 930, Arlington, Va.

Hill, Gale E. (D), 35 Railroad St., Keene, New Hampshire.

Hill, Gladstone (B), Salesman, Carnegie Illinois Steel Corp., W1352—1st National Bank Bldg., St. Paul, Minn.

Hill, H. O. (B), Asst. Chief Engineer—Fabricated Steel Construction, Bethlehem Steel Co., Bethlehem, Pa.

Hill, Joe H. (C), Engineer, Truscon Steel Co., 700 Investment Bldg., Washington, D. C.

Hill, Lockwood (B), Proprietor of Hill Equipment Engineering Co., 3431 Chouteau Ave., St. Louis, Mo.

Hilpert, M. G. (C), Asst. Engineer, Bethlehem Steel Co.; (Res.) 33 West Church St., Bethlehem, Pa.

Hilse, Fred (D), 852 Bergen St., Newark, N. J.

Hinkefent, Emil C. (B), President, Allied Steel Prod. Corp., Box 1020, Tulsa, Okla.

Hinkle, W. R., Jr. (B), Compressed Industrial Gases, Inc., 673 N. Orleans St., Chicago, Ill.

Hirner, F. J. (C), Asst. Sales Manager, Welder Div., Harnischfeger Corp., 4400 W. National Ave., Milwaukee, Wis.

Hirt, E. L. (C), Mech. Engineer, Canadian & General Finance Co., 25 King St., W., Toronto, Ont., Canada.

Hjulin, Paul (D), Welder, Crane Co., 10318 Indiana Ave., Chicago, Ill.

Hlusicka, J. (C), Welding Foreman, Caribbean Petroleum Corp., C. P. C. Maracaibo, Mene, Grande, Venezuela, South America.

Hoar, William S. (C), In Charge of Welding, McClintic-Marshall Co., Pottstown, Pa.

Hobart, Chas. J., Sr. (C), Mechanical Engineer, Link-Belt, 300 West Pershing Road, Chicago, Ill.

Hobart, H. M. (B), Consulting Engineer, General Electric Co., Schenectady, N. Y.

Hobart, W. H. (B), President, Hobart Bros., Co., Hobart Square, Troy, Ohio.

Hodell, Louis R. (B), Research Engineer, Carter Oil Co., National Bank of Tulsa Bldg., Tulsa, Okla.

Hodes, Isidore (C), Jr. Welding Engineer, Naval Gun Factory; (Res.) 1404 C St., N. E., Washington, D. C.

Hodge, Frank N. (C), Welding, U. S. Naval Gun Factory, Washington D. C.; (Res.) 1535—18th St., North Arlington, Va.

Hodge, J. C. (B), Chief Metallurgist, Babcock & Wilcox Co., Barberton, Ohio.

Hodge, Thomas (D), Pipe Welder, Heyden Chemical Corp.; (Res.) 70 Kip Avenue, Rutherford, N. J.

Hodges, Charles (C), Welding Supt., Standard Oil Co. of N. J., Bayway Refinery, P. O. Box 16, Elizabeth, N. J.

Hodges, Earle C. (B), District Branch Manager, Detroit Steel Products Co., 1713 K St., N. W., Washington, D. C.

Hodgson, Harry (C), Welding Foreman, Layne-Bowler Co., Chelsea Ave., Memphis, Tenn.

Hoelzel, E. C. (C), General Supt., Youngstown Steel Door Co., P. O. Box 568, Youngstown, Ohio.

Hoenighausen, John (C), Master Mechanic, Socony Vacuum Oil Co.; (Res.) 400 Kingsland Ave., Brooklyn, N. Y.

Hoern, J. H. (B), Gen. Master Mechanic, Wilcox-Rich, Div. of Eaton Mfg. Co., 9771 French Road, Detroit, Mich.

Hoffberg, William Arnold (B), Consulting Engineer, 67 West 44th St., New York, N. Y.

Hoffman, C. H. (B), Mfrs. Manager, Seattle Boiler Works, 5237 East Marginal Way, Seattle, Wash.

Hoffman, Edward H. (C), Sales Engineer, Foster Wheeler Corp.; (Res.) 417 Tremont Ave., Orange, N. J.

Hoffman, Forrest A. (B), Supt., Tulsa Boiler & Mach. Corp., 2020 South Union, Tulsa, Okla.

Hoffman, Lester O. (C), Eng. Draftsman, Monsanto Chemical Co.; (Res.) 4115 Sacramento Ave., St. Louis, Mo

Hoffman, Ray (D), Welder, General Machinery Corp., R. R. 1, Hamilton, Ohio.

Hogaboom, Allen G. (D), Welder, Bethlehem Steel Co. (Shipbuilding Div.); (Res.) 35 Upland Road, Quincy, Mass.

Hoglund, Clarence E. (D), Welder, C. W. Olson Mfg. Co.; (Res.) 1300 Quincy St., N. E., Minneapolis, Minn.

Hoglund, Gustav O. (B), Welding Engineer, Aluminum Company of America, New Kensington, Pa.

Hoier, Wm. V. (B), President, Robert Gordon, Inc., 19 W. Jefferson St., Chicago, Ill.

Holder, G. C. (C), Metallurgist, Foster Wheeler Co.; (Res.) 210 Elm Court, Elizabeth, N. J.

Holderby, E. O. (D), Welder, Jaeger Machine Co.; (Res.) 867 Gilbert St., Columbus, Ohio.

Holland, S. Emil (C), General Manager, Simon Holland & Son Inc., 825 Stone Ave., Brooklyn, N. Y.

Holley, E. C. (B), Production Supt., St. Joseph Ry. Light, Heat & Power Co., 520 Francis St., St. Joseph, Mo.

Hollingshead, W. E. (A), Designing Engineer, S. Morgan Smith Co., Lincoln & Hartley Sts., York, Pa.

Hollister, Prof. S. C. (B), Cornell University, Ithaca, N. Y.

Holman, Arnold C. (D), Welder & Flame Hardener, Wesley Steel Treating Co., 1333 W. Pierce St., Milwaukee, Wis.

Holmes, D. O. (D), Welder Foreman, American Cast Iron Pipe Co., Birmingham, Ala.

Holslag, C. J. (B), Engineer, Electric Arc Cutting & Welding Co., 152 Jelliff Ave., Newark, N. J.

Holt, Joseph (B), Welder & Publisher, State of Wash. Dept. of Highways; (Res.) 1917 Ferry Ave., S. W., Seattle, Wash.

Holt, M. L. (C), Sales Engineer, Chattanooga Welding & Machine Co., Chattanooga, Tenn.

Holt, R. W. (B), Research Work, Chicago Steel & Wire Co.; (Res.), 10257 Torrence Ave., Chicago, Ill.

Holtzhouse, Guy. A. (C), Prop., Superior Welding & Brazing Co.; (Res.) 6538 Russell St., Detroit, Mich.

Holwill, Claude N. (D), Specialist, Applied Engrg. Dept., Air Reduction Sales Co., 60 E. 42nd St., New York, N. Y.

Holzbaur, Arthur A. (C), Engineer, Sun Shipbldg. & Dry Dock Co.; (Res.) 25 W. Parkway Ave., Chester, Pa.

Homann, F. C. (C), Assoc. Prof. Mech. Eng., Montana State College, Bozeman, Mont.

Homrighaus, A. H. (B), Genl. Manager & President, The Welder Service Co., 2058 Canton Ave., Toledo, Ohio.

Honey, Edward M. (C), Structural Designer, Detroit Edison Co.; (Res.) 5379 Seneca Ave., Detroit, Mich.

Honhart, Jack C. (C), Engineer, Metal & Thermit Corp., 5721 Lincoln Ave., Detroit, Mich.

Hoobler, P. R. (D), Welder, Mid-Continent Petroleum Co.; (Res.) 1634 E. Admiral Place, Tulsa, Okla.

Hook, Ira T. (B), Research Engineer, The American Brass Co., Ansonia, Conn.

Hooper, Wyllys G. (B), Vice-President, Midwest Piping & Supply Co., Inc., 1450 South Second St., St. Louis, Missouri.

Hopkins, H. E. (C), Arcos Corp., 401 N. Broad St., Phila., Pa.

Hopkins, R. K. (B), Director Metallurgical Research and Development, M. W. Kellogg Co., Foot of Danforth Ave., Jersey City, N. J.

Hopley, David (D), Foreman, General Electric Co.; (Res.) 951 Dalton Ave., Pittsfield, Mass.

Hopwood, N. P. (D), Arc Welding, A. B, Farquhar Co., Ltd.; (Res.) 326 E. Poplar St., York, Pa.

Horgan, Patrick J. (C), Engineer, General Electric Co.; (Res.) 33 Bertram St., Beverly, Mass.

Horlock, Jack (C), Treasurer, Searles Electric Welding Works, 1850 Fulton St., Chicago, Ill.

Horn, A. M. (C), Struct. Engineer, Design Div., Bureau of Docks & Yards; (Res.) 4 Carvel Rd., Westmoreland Hills, Friendship Sta., Washington, D. C.

Horn, T. L. (C), President, Kanawha Welding Rod Co., 1308 Kanawha Valley Bldg., Charleston, W. Va.

Horne, L. V. (C), Mech. Engineer, Union Oil Co.; (Res.) 617 W. 17th St., Los Angeles, Calif.

Horne, Marvin (C), Sales Engineer, I-T-E Circuit Breaker Co., 22187 W. Michigan Ave., Dearborn, Mich.

Horrigan, J. R. (B), Vice-President, Williams Bros. Corp. & Apex Const. Co., 2114 Second Natl. Bank Bldg., Houston, Texas.

Horton, George Terry (A), President, Chicago Bridge & Iron Co., 1305 West 105th St., Chicago, Ill.

Hoskins, G. W. (B), Sales, A. O. Smith Corp.; (Res.) 236 Washington Ave., Manoa, Pa.

Hosler, H. C. (D), Grandview, Missouri.

Hosted, E. G. (C), Instructor Drexel Inst., % Mach. Shop Office, Mech. Engrg. Dept. Philadelphia, Pa.

Hough, C. T., Jr. (C), District Sales Rep., Walworth Co., 810—18th St., Washington, D. C.

House, Alan (B), Welding Engineer, R. C. A. Mfg. Co.; (Res.) 14 E. Greenwood Ave., Oaklyn, N. J.

House, Charles W. (D), Welder, General Petroleum Corp.; (Res.) 1450 W. 70th St., Los Angeles, Calif.

House, H. T. (D), Welder, Coleman Lamp & Stove Co., 328 N. Minn., Wichita, Kansas.

Houser, K. O. (B), Industrial Sales Engineer, Kansas Gas & Electric Co., Sedgwick Bldg., Wichita, Kansas.

Hovey, O. E. (B), Director, The Engineering Foundation. Consulting Engineer, 11 W. 42nd St., New York, N. Y.

Hovey, Walter F. (B), Engineer, Sanderson & Porter, 52 William St., New York, N. Y.

Howard, Frank O. (B), Manager of Sales, American Steel & Wire Co., 1403 Liberty Bank Bldg., Buffalo, N. Y.

Howard, Wayne A. (C), Asst. Supervisor, Engrg. Section, Socony Vacuum Oil Corp., 412 Greenpoint Ave., Brooklyn, N. Y.

Howard, W. F. (C), Tool Division, Jones & Lamson Mach. Co., Springfield, Vt.

Howell, A. J. (D), Arc Welder, American Seating Co.; (Res.) 844 Franklin St., S. E., Grand Rapids, Mich.

Howell, W. H. (C), Union Carbide Co., Niagara Falls, N. Y.

Howery, Theodore E. (D), Combination Welder, Monsanto Chemical Co.; (Res.) 3763 Potomac, St. Louis, Mo.

Howland, Owen L. (B), General Sales Manager, Hollup Corp., 3357 W. 47th Place, Chicago, Ill.

Hruska, John H. (B), Metallurgical Engineer, Electro-Motive Corp. (Subsidiary of General Motors Corp.), La Grange, Ill.

Hubach, K. G. (B), Demonstrator, Page Steel & Wire Div. of American Chain & Cable Co., Second & Diamond Sts., Philadelphia, Pa.

Hubbard, Ronald P. (C), 243 Live Oak Ave., Pascogoula, Miss.

Huber, George L. (B), Vice-President, W. K. Mitchell & Co., Inc., 2940 Ellsworth St., Phila., Pa.

Hudnall, John W. (D), Welder, Consolidated Gas & Electric Co.; (Res.) First Ave., Ferndale, Md.

Hudoba, Louis (B), Foreman, Welding Dept., 1121 Jackson St., N. E., Minneapolis, Minn.

Hudson, D. D. (D), Welding Operator, Fisher Body Corp.; (Res.) 14012 Lake Shore Blvd., Cleveland, Ohio.

Hudson, James A. (C), Supervisor, Process Service, The Linde Air Products Co., 230 N. Michigan Ave., Chicago, Ill.

Hudson, Wm. T. (B), Asst. Manager of Operations for Erection, Chicago Bridge & Iron Co., 1305 W. 105th St., Chicago, Ill.

Huester, H. J. (C), Materials Eng. (Metals), Bureau of Aeronautics, Navy Dept.; (Res.) 4621 N. 36th St., Arlington, Va.

Huet, G. O. (B), Naval Architect & Marine Engineer, Higgins Industries, Inc., 1755 St. Charles Ave., New Orleans, La.

Huffman, Shelton B, 202 E. Cedar Ave., Bellflower, Calif.

Hughen, J. M. (B), President, The Petroleum Iron Works Co., Beaumont, Texas.

Hughes, A. H. (C), Plant Supt., Edge Moor Iron Works, Inc., Edge Moor, Delaware.

Hughes, Grover A. (B), 419 Almyra Ave., Youngstown, Ohio.

Hughes, Paul B. (D), Welder, Soto Welding Works; (Res.) 1708 So. Soto St., Los Angeles, Calif.

Hughes, Thomas P. (C), Asst. Prof. Mech. Eng., Univ. of Minn., Mech. Eng. Bldg., Minneapolis, Minn.

Humberstone, J. H. (C), 2900 Dunran Road, Apt. A 2, Dundalk, Md.

Humble, C. E. (C), Vice-President, Burke Electric Co., Erie, Pa.

Humble, John S. (B), Welding Inspector, E. B. Badger & Sons Co.; (Res.) 714 Commonwealth Ave., Boston, Mass.

Hume, K. B. (C), Foreman, Steel Pipe Works, % Hume Steel Co., P. O. Box 4534, Melbourne, C. 1, Australia.

Humm, E. F. (C), Erection Manager, Eastern Division, Chicago Bridge & Iron Co., 119 Long Ave., Hillside, N. J.

Humphrey, Stanley M. (B), Electric Engineer, Taylor-Winfield Corp., Warren, Ohio.

Hunt, Duard, B. (B), Welding Engineer, Southern Railway System; (Res.) 484 Brentwood Drive, N. E., Atlanta, Ga.

Hunt, G. C. (C), Welding Engineer, G. C. Hunt Co., Room 15, Atlanta Terminal Station, Atlanta, Ga.

Hunt, Leon (C), Secretary & Treasurer, G. C. Hunt & Co., Room 15, Atlanta Terminal Station, Atlanta, Ga.

Hunt, S. D. (B), Vice-President, Moorlane Co., Box 1679, Tulsa, Okla.

Huntley, W. B. (C), Vice-President in Charge of Sales, Brace-Mueller-Huntley, Inc., 963 Lyell Ave., Rochester, N. Y.

Hurcomb, F. A. (B), Asst. General Manager, Federal Mach. & Welding Co.; (Res.) 148 Kenmore, S. E., Warren, Ohio.

Hurd, James L. (D), Mahoning Ave. Ext., Warren, Ohio.

Hurlbutt, Robert H. (C), Junior Engineer, U. S. Engineers, Clock Tower Bldg., Rock Island, Ill.

Hurley, Jack (D), Welding Operator, 12917 St. Clair Ave., Cleveland, Ohio.

Hurley, Wallace J. (D), Welder, Edison General Electric Appl. Co.; (Res.) 1638 N. Linder Ave., Chicago, Ill.

Hurley, Wm. (D), Welder, Sound Welding, Inc., 905 East 134th St., Bronx, N. Y.

Hurst, William I. (C), Supervisor, American Thermometer Co., 2917 Clark Ave., St. Louis, Mo.

Hurt, A. S., Jr. (D), Foreman, A. J. Mfg. Co.; (Res.) 533 S. Hardy, Kansas City, Mo.

Hurt, R. J. (C), District Sales Manager, National Cylinder Gas Co., 205 W. Wacker Dr., Chicago, Ill.

Hus, S. F. (D), Stockton, Kansas.

Husaczka, Stephen (D), Welder and Burner, Anaconda Wire and Cable Co.; (Res.) 335 Warburton Ave., Yonkers, New York.

Huse, Harold M. (C), Engineer, Union Carbide Co., Niagara Falls, N. Y.

Husky, L. D. (D), Maintenance Dept., Okla. Gas & Electric Co.; (Res.) P. O. Box 463, Britton, Okla.

Hussion, Wm. T. (D), 2225 Gaylord St., Denver, Colo.

Hutchins, W. C. (C), Industrial Control Sales, Ind. Dept., General Electric Co., Schenectady, N. Y.

Hutchison, Robert (D), Welder, Staynew Filter Corp.; (Res.) 137 Linden St., Rochester, N. Y.

Hyler, L. L. (B), Engrg. Supervisor, R. G. Le Tourneau, Inc.; (Res.) 537 Martin St., Peoria, Ill.

I

Idell, Percy C. (C), District Manager, The Babcock & Wilcox Co., 49 Federal St., Boston, Mass.

Immer, W. L. (C), Welding Specialist, General Electric Co., 230 So. Clark St., Chicago, Ill.

Imperati, Joseph (C), Welding Engineer, American Brass Co.; (Res.) 1598 Blvd., New Haven, Conn.

Inglesby, Chas. A. (B), Manager, Structural & Plate Depts., The Steel Products Co., Inc., P. O. Box 1007, Savannah, Ga.

Ireland, B. N. (D), Elect. Welder, Vernon Tool Co.; (Res.) 210 E. Cedar, Bellflower, Calif.

Ireton, Samuel R., Jr. (B), Engineer of Tests, U. S. Pipe & Fdy. Co., Burlington, N. J.

Irons, James D. (C), Associate Metallurgist, Navy Yard, Portsmouth, N. H.; (Res.) York Village, Maine.

Irvin, J. L. (C), Asst. General Supt., Gulf Oil Corp.; (Res.) 6418 Auden Ave., Houston, Texas.

Irvine, Vess E. (B), Marine Engineer, Federal Shipbldg. & D. D. Co., 21 West St., New York, N. Y.

Irwin, George W. (C), District Manager, Air Reduction Sales Co., 327—25th Ave., S. E., Minneapolis, Minn.

Isaacson, T. E. (F), 1431 W. 8th St., Ashtabula, Ohio.

Isenburger, H. R. (C), President, St. John X-ray Service Corp., 30–20 Thomson Ave., Long Island City, N. Y.

Isgren, Elmer (B), Plant Supt., R. G. Le Tourneau, Inc., Peoria, Ill.

Isgren, Vernon R. (B), Dept. Supt., R. G. Le Tourneau, Inc.; (Res.) 4201 Prospect St., Peoria, Ill.

Iwasaki, Massahide (B), Commander I. J. N., The Japanese Naval Inspector Office, 1 Madison Ave., New York, N. Y.

J

Jacks, C. G. (C), The Linde Air Products Co., 6119 Harrisburg Blvd., Houston, Texas.

Jackson, Chas. (C), Sun Oil Co., Marcus Hook, Pa.

Jackson, C. E. (C), Metallurgist, Naval Research Laboratory, Anacostia Station, Washington, D. C.; (Res.) 314 Poplar Ave., Takoma Park, Md.

Jackson, Edward C. (C), Supervisor of M. of W. Equipment & Scales, Southern Pacific Railroad; (Res.) 913 Franklin Ave., Houston, Texas.

Jackson, Earl T. (D), Welder, Independent Packing Co.; (Res.) 6212 Reichman Ave., St. Louis Co., Mo.

Jackson, H. A. (A), President, The Jackson Electrode Holder Co., 15122 Mack Ave., Detroit, Mich.

Jackson, H. J. (B), Field Supt., Thompson Mfg. Co., 3001 Larimer St., Box 2347, Denver, Colo.

Jackson, J. O. (B), Chief Engineer, Pittsburgh-Des Moines Steel Co., Neville Island P. O., Pittsburgh, Pa.

Jackson, L. F. (B), Sales Rep., The Linde Air Prods. Co., 441 Stuart St., Boston, Mass.

Jackson, R. H. (C), General Electric Co., 187 Spring St., N. W., Atlanta, Ga.

Jackson, Thomas M. (A), Electrical Engineer, Sun Shipbldg. & Dry Dock Co., Chester, Pa.

Jacob, F. B. (C), District Manager, Thomson-Gibb Electric Welding Co.; (Res.) 2010 East 102nd St., Cleveland, Ohio.

Jacobs, David, S. (C), Service Engineer, Canadian Liquid Air Co. Ltd., 16 Boler St., West Toronto, Ont., Canada.

Jacobsen, Carl (D), Welder, U. S. Engineers Shop, Gohagen, Montana.

Jacobsen, Odd (B), Naval Arch. & Welding Engineer, Trosvik Verksted A/S, Brevik, Norway.

Jacobus, David Schenck (B), Advisory Engineer, The Babcock & Wilcox Co., 85 Liberty St., New York, N. Y.

Jalowy, H. C. (D), Elect. Welder, T. & N. O. Ry. Co., 324 S. P. Bldg., Houston, Texas.

James, H. Rees (C), Metallurgist, Blaw-Knox Co., 1525 Penn Ave., Pittsburgh, Pa.

James, Louis (F), 524 Knoblock, Stillwater, Okla.

James, Paul W. (C), Sales Engineer, Lincoln Electric Co., 17 Lake View Pk., Rochester, N. Y.

Jankov, Paul V. (B), Naval Arc, Ship Surveyor, % Det Norske Veritas, Raadhusgaten 25, V, Oslo, Norway.

Jankowiak, Leo (D), Electric Welder, Bethlehem Steel Co.; (Res.) 230 So. Robinson St., Baltimore, Md.

Janovec, Edward (D), Arc Welder, Brown Steel Tank Co., (Res.) 1792 Carroll Ave., St. Paul, Minn.

Jansen, Charles (D), Welder, Delco Appliance Corp.; (Res.) 283 Laburnum Crescent, Rochester, New York.

Janzer, Jos. M. (C), Welder, Link-Belt Co.; (Res.) 3327 N. Almond St., Philadelphia, Pa.

Jarman, Donald (B), Welding Engineer, Wickes Boiler Co., Saginaw, Mich.

Jasper, Wm. J. (D), Welder, Standard Oil Co. of Calif., P. O. Box 1500, Bakersfield, Calif.

Jedlick, Charles L. (C), Designer, Chevrolet Motor Co.; (Res.) 2630 Mallory, Flint, Mich.

Jefferson, John A. (B), Manager, A. F. Robinson Boiler Works, 200 Second Ave., Cambridge, Mass.

Jefferson, T. B. (C), Design Engineer, Marine Design Section Office, Chief of Engineers, War Dept., Washington D. C.; (Res.) 415 Garfield Ave., Hyattsville, Md.

Jeffries, W. J. (C), Sr. Materials Eng., Bureau C. & R. Navy Dept., Washington, D. C.

Jenkins, Alex F. (B), Alex Milburn Co., 1416–28 W. Baltimore St., Baltimore, Md.

Jenkins, Clyde (C), Elect. Engineer, Youngstown Steel Door Co., P. O. Box 568, Youngstown, Ohio.

Jenkins, Osce O., Jr. (B), Welding Supt., O Dell & Riney Const. Co., Jet, Okla.; (Res.) 924 East Cherokee, Enid, Okla.

Jenks, Glen F. (B), Colonel, Ordnance Dept., U. S. Army Ordnance Office, Army Munitions, Bldg., Washington, D. C.

Jennings, Chas. H. (B), Research Engineer, Westinghouse Electric & Mfg. Co., Research Labs., East Pittsburgh, Pa.

Jennings, Jack (D), Operator, Contract Welders, Inc.; (Res.) 7714 Lawn Ave., Cleveland, Ohio.

Jennings, Wm. L. (C), Arc Welding Inspector, Cuyahaga County; (Res.) 1076 E. 171st St., Cleveland, Ohio.

Jensen, Carl E. E. (C), Waiahia Agricultural Co., Waiahia, T. H.

Jensen, Cyril D. (C), Assoc. Prof. of Civil Engineering, Lehigh University, Bethlehem, Pa.

Jensen, Holger (A), Manager—Boiler & Machinery Division, Maryland Casualty Co.; (Res.) 203 East 32nd St., Baltimore, Md.

Jensen, Sigurd B. (D), Aircraft Welder, Hawaiian Air Depot, Lukefield, T. H.

Jerabek, T. E. (C), Metallurgist, Lincoln Electric Co.; (Res.) 13608—5th Ave., E. Cleveland, Ohio.

Jesperson, Chas. (D), Welder, General Electric Co.; (Res.) 2508 Campbell Ave., Schenectady, N. Y.

Jeswald, Joseph (D), Machinist Foreman, Youngstown Welding & Engrg. Co.; (Res.) 41 N. Meridian Ave., Youngstown, Ohio.

Jewell, Alpha (C), Engineer, Caixa Postal 2814 Sao Paulo, Brazil, S. A.

Johansen, Edwin B. (B), Jr. Mechanic Engineer, Dept. of Water & Power, 6100 Barrows Dr., Los Angeles, Calif.

Johansen, E. K. (B), Chief Engineer, Clearing Machine Corp.; (Res.) 1843 No. Natchez Ave., Chicago, Ill.

Johnson, A. (D), Welder Foreman, Campbell Steel & Iron Works; (Res.) 352 Bell St., Apt. 2, Ottawa, Canada.

Johnson Amos (C), 3708—41st Ave., So. Minneapolis, Minn.

Johnson, A. B. (B), Treasurer, C. G. Johnson Boiler Co., 1445 No. 11th St., Omaha, Neb.

Johnson, Albert T. (C), 202 Twin Oaks Road, Apt. 27, Akron, Ohio.

Johnson, Ashmore C. (A), Vice-President, Downingtown Iron Works, Downingtown, Pa.

Johnson, Carl C. (D), Welder, Oak St., Westborough, Mass.

Johnson, E. J. (D), General Welding Co.; (Res.) 1200 S. W. 17th St., Birmingham, Ala.

Johnson, Geo. R. (C), Dist. Manager, % Lincoln Electric Co., 401 North Broad St., Philadelphia, Pa.

Johnson, Gustave (C), Supervisor, Applied Engrg. Dept., Air Reduction Sales Co., 818 W. Winnebago St., Milwaukee, Wis.

Johnson, J. H. (A), President, Johnson Supply Co., 1414 Wazee St., Denver, Colo.

Johnson, John M. (B), Factory Supt., Wailuku Sugar Co., Box 155, Wailuku, Hawaii

Johnson, Lloyd C. (F), 20 Pershing Ave., Milltown, N. J.

Johnson, Leon H. (B), Chief Engineer, Struthers-Wells Co., Warren, Pa.

Johnson, Seth (B), Assistant Engineer, The Safety Car Heating & Lighting Co., P. O. Box 904, New Haven, Conn.

Johnson, W. E. (C), Mech. Engineer, Dorr Co., Inc., Westport, Conn.

Johnston, A. P. (B), Johnston Welding Rods, 1845 E. 57th St., Los Angeles, Calif.

Johnston, Bruce G. (B), Assistant Director in charge of Research, Fritz Engineering Laboratory, Lehigh University, Bethlehem, Pa.

Johnston, C. (D), Welding Operator, Milton Bowers Welding Co., 346 Madison Ave., Memphis, Tenn.

Johnston, Merkle C. (D), Welder, Lockheed Aircraft, Inc.; (Res.) 2245 Cloverdale Ave., Los Angeles, Calif.

Johnston, Robert S. (B), Director of Research, The John A Roeblings Sons Co.; (Res.) 6 Alton Road, Delavue Manor, Yardley, Pa.

Jolly, J. T. (D), Maintenance Operator, Firestone Tire & Rubber Co.; (Res.) 1155 Central, Memphis, Tenn.

Jones, A. B. (B), Assistant Foreman, Jeffrey Manufacturing Co.; (Res.) 1459 W. 6th Ave., Columbus, Ohio.

Jones, C. S. (C), Asst. Chief Engineer, The Missouri Valley Bridge & Iron Co., Leavenworth, Kansas.

Jones, Ed. C. (C), Shop Supt., Wyatt Metal & Boiler Works, P. O. Box 3052, Houston, Texas.

Jones, Edward J. (C), Machinist Welder, Oahu Railway & Land Co.; (Res.) 1631 Poloma St., Honolulu, T. H.

Jones, E. L. (D), Welding Foreman, American Rolling Mill Co., 208 Monroe St., Middletown, Ohio.

Jones, Emanuel (B), Foreman, Yawman & Erbe Mfg. Co.; (Res.) 257 Congress Ave., Rochester, N. Y.

Jones, Harold O. (C), Supervisor, Applied Engineering Dept., Air Reduction Sales Co., Dayton Industries Bldg., Dayton, Ohio.

Jones, Henry W., Jr. (B), President, American Tube Bending Co., 5 Lawrence St., New Haven, Conn.

Jones, Howard (D), Welder, Colgate-Palmolive-Peet Co., Packers Station, Kansas City, Kansas.

Jones, James (C), Welder, Grinnell Co.; (Res.) 325 Elm Road, Warren, Ohio.

Jones, Jonathan (C), Chief Engineer, Bethlehem Steel Co., Bethlehem, Pa.

Jones, L. E. (C), Instructor, Mech. Engrg. Dept., W. Virginia Univ., Morgantown, W. Va.

Jones, Paul (D), Operator, Paul Jones Welding & Mach. Shop, Eureka, Kansas.

Jones, Russell, Jr. (D), Welder, Ingalls Iron Works; (Res.) 1136 Tenth Place So., Birmingham, Ala.

Jones, Russell E., Sr. (C), Chief Engineer, Birmingham Tank Co. Division of the Ingalls Iron Works Co.; (Res.) 1136 Tenth Place South, Birmingham Ala.

Jones, R. P. (D), Welding Foreman, Luscombe Airplane Co.; (Res.) Box 113, W. Trenton, N. J.

Jones, Tom (B), Welding Supt., South Works of Carnegie-Illinois Steel Co.; (Res.) 8244 East End Ave., Chicago, Ill

Jones, Warren (C), Mech. Engineer, 171 Pond St., Sharon, Mass.

Jones, S. O. (B), Welding Engineer, Newport News Shipbldg. & Dry Dock Co., Newport News, Va.

Jones, Thomas A. (C), Proprietor, Arcway Equipment Co., 3717 Filbert St., Phila., Pa.

Jones, Wm. (D), 15234 Paulina Ave., Harvey, Ill.

Josephian, Wm. (C), Pacific Oxygen Co., 2205 Magnolia St., Oakland, Calif.

Joslin, E. W. (C), Sales Demonstrator, Hill Equip. Eng. Co., 3429 Chouteau Ave., St. Louis, Mo.

Joublanc, J. C. (C), Director of Metallurgy & Research, R. G. Le Tourneau Inc., Peoria, Ill.

Joyce, E. (C), 149 S. Rolling Road, Springfield, Delaware Co., Pa.

Joyce, Edw. M. (C), Western Sales Manager, Champion Rivet Co., East Chicago, Ind.

Judelsohn, Fred (C), Supervisor, Applied Engineering Dept., Air Reduction Sales Co.; (Res.) 200 Weldy Ave., Oreland, Pa.

Juedeman, R. F. (D), Welder, International Refining Co., Box 283, Sweet Grass, Montana.

Jumbalo, S. J. (D), Welder, Western Electric Co.; (Res.) 5304 W. 22nd Place, Cicero, Ill.

K

Kalix, Robert L. (B), Kalix Welding School, 439 N. 11th St., Philadelphia, Pa.

Kamschulte, E. J. (B), Mech. & Structural Engineer, 5548 Franklin Ave., Los Angeles, Calif.

Kandel, Charles (B), President, Craftsweld Equipment Corp., 2727 Jackson Ave., Long Island City, N. Y.

Kane, J. J. (B), President, Kane Boiler Works, Inc., 2715 Ave. C, Galveston, Texas.

Kastman, Lawrence (D), Arc Welder, American Boiler & Tank Co.; (Res.) 500 Kraker Ave., Joliet, Ill.

Kauffmann, Eugene A. (B), Manager, Genesee Welding Works; (Res.) 9–11 N. Washington St., Rochester, N. Y.

Kauffmann, Gene (C), Engineer, Aluminum Français, 23rd Rue Balzac, Paris, 8, France.

Kaukeinen, R. M. (D), Welder, New York Central R. R. Co.; (Res.) 171 Sly Ave., Corning, N. Y.

Kazoroski, W. (D), Welding Operator, Dolomite Marine Corp., 52 Linden Ave., E. Rochester, N. Y.

Keating, Leonard M. (B), Manager Special Products Division, A. O. Smith Corp., Milwaukee, Wis.

Keatley, Gordon M. (D), 621 Colonial Ave., Norfolk, Va.

Keeler, Nelson Herbert (D), Electric Welder, Winchester Repeating Arms Co.; (Res.) 518 Ocean Ave., New London, Conn.

Keeney, Ray (D), Welder, Thompson Mfg. Co.; (Res.) 42 B St., Vallejo, Calif.

Kegg, Thomas J. (B), Sales, Champion Rivet Co., Box 42, Houston, Texas.

Kehl, R. J. (B), The Linde Air Products Co., 30 E. 42nd St., New York, N. Y.

Keir, James M. (C), Chemical Engineer, The Linde Air Products Co., 30 East 42nd St., New York City.

Kelleher, T. F. (C), Chief Engineer, Maryland Casualty Co.; (Res.) 287 Harrison Ave., St. Paul, Minn.

Keller, R. B. (C), (Steel Mill), Specialist, Applied Engrg. Dept., Air Reduction Sales Co., 60 E. 42nd St., New York, N. Y.

Keller, R. J. (C), Production Engineer, A. O. Smith Corp.; (Res.) 528 No. 19th St., Milwaukee, Wis.

Kelling, Scott (B), Owner, Capital City Welding Works, 428 West Main St., Jefferson City, Mo.

Kellogg, Wm. Pitt (C), District Manager, Air Reduction Sales Co., 630 S. Second St., St. Louis, Mo.

Kelly, William (B), Supt., Pittsburgh Coal Co., Elisabeth Marine Ways, Box No. 209, Elizabeth, Pa.

Kelsey, Howard C. (B), Vice-President, Machinery and Welder Corp., 1414 McGee St., Kansas City, Mo.

Kelsey, Walter (B), 1410 M St., Washington, D. C.

Kelso, George W. (C), Welder, Gellatly Const. Co.; (Res.) 295 East Washington Ave., Bridgeport, Conn.

Kemper, Luther, H. (C), Welding Supervisor, Frick Co., Inc.; (Res.) 109 Fairview Ave., Waynesboro, Pa.

Kennedy, A. M. (B), Vice-President & General Manager, Hunter Steel Co., P. O. Box 9339, Neville Island, Pa.

Kennedy, Clarence J. (C), Plant Engineer, American Bridge Co., Gary, Ind.

Kennedy, R. E. (C), Otis Elevator Co., 260—11th Ave., New York City.

Kenney, A. A. (C), Manager, Chicago Bridge & Iron Works, 250 Stuart St., Boston, Mass.

Kenney, L. T. (B), General Manager, United Welding Co., Middletown, Ohio.

Kennon, Lorenzo (B), Welding Engineer Lockheed Aircraft Corp.; (Res.) 1927 Evergreen St., Burbank, Calif.

Kenny, John F. (B), Acety. Welder, Washington Gas Light Co.; (Res.) 4226 Ellicott St., N. W., Washington, D. C.

Kenny, L. T. (B), Manager, Kenny Boiler & Mfg. Co., 423 E. 6th St., St. Paul, Minn.

Kenrick, Ralph S. (B), Editor, The Welding Engineer, 506 So. Wabash Ave., Chicago, Ill.

Kenworthy, J. W. (C), Welding Engineer, Mack Mfg. Corp.; (Res.) 825 Linden St., Allentown, Pa.

Keogh, A. F. (C), President, Sound Welding Co., 905 E. 134th St., New York City.

Keplinger, John C. (A), Vice-President, Hercules Motors Corp., Canton, Ohio.

Keptner, Thomas C. (C), Foreman, Buehler Tank & Welding Works; (Res.) 9212—6th Ave., Inglewood, Calif.

Kerbey, E. A. (C), Eastern Manager, Midwest Piping & Supply Co., Inc., 30 Church St., New York City.

Kerbs, L. E. (D), Box 11, Otis, Kansas.

Kerchner, C. E. Lewis (B), Sales Rep., Southern Oxygen Co.; (Res.) 290 W. Cottage Pl., York, Pa.

Kerr, Edward J. (C), Welding Demonstrator, Williams & Co., Inc.; (Res.) 10721 Hathaway Ave., Cleveland, Ohio.

Kerr, Edward L. (C), Steel Worker, Eastman Kodak Co.; (Res.) 580 Post Ave., Rochester, N. Y.

Kerr, Geo. M. (B), Treasurer & General Manager, Kerr Welding Co., 19 Jackson St., Worcester, Mass.

Kerr, S. Logan (B), Chemical Engineering Division, United Engineers & Constructors Inc., 111 W. Washington St., Chicago, Ill.

Kerrigan, Joseph A. (B), Inspector of Steel, Board of Transportation, 250 Hudson St., N. Y. C.; (Res.) 213 South Jefferson Ave., Canonsburg, Pa.

Kerry, Frank George (B), Engineering Dept., Canadian Liquid Air Co., Ltd., 1111 Beaver Hall Hill, Montreal, P. Q., Canada.

Kessler, F. W. (C), Welding Demonstrator, Lincoln Electric Co.; (Res.) 1904 Parkway, Cleveland Heights, Ohio.

Ketchum, M. S., Jr. (B), Asst. Prof. of Structural Eng., Case School of Applied Science, University Circle, Cleveland, Ohio.

Keyes, J. W. (C), Salesman, Weldit Acetylene Co., 638 Bagley Ave., Detroit, Mich.

Keyser, Cares C. (B), Welding Supervisor, Bethlehem Steel Co., Steelton Plant, 115 West Main St., Shiremanstown, Pa.

Kice, M. S., Jr. (C), Chief Engineer, American Blower Corp., 6000 Russell St., Detroit, Mich.

Kicherer, Harry J. (B), Mech. Engineer, International Harvester Co., 99 Groveland Ave., Riverside, Ill.

Kick, Karl H. (C), Plant Engineer, Citizens Gas & Coke Utility, Prospect St. Plant, Indianapolis, Ind.

Kidd, Alexander (B), Asst. Works Manager, The M. W. Kellogg Co., Foot of Danforth Ave., Jersey City, N. J.

Kihlgren, Theo. E. (B), Metallurgist, International Nickel Co., Research Lab., Bayonne, N. J.

Kilbane, James, Jr. (D), Railroad Welder, Great Northern R. R.; (Res.) 1329 Topping St., St. Paul, Minn.

Kilgore, Gordon (D), Welder, Burkhardt & Sons Steel & Iron; (Res.) 140 S. Hazel Court, Denver, Colo.

Kilian, J. O. (C), Salesman, A. M. Castle Co.; (Res.) 2311 Midvale Ave., W. Los Angeles, Calif.

Kimball, C. E. (C), Welding Engineer, Chattanooga Welding & Machine Co., Chattanooga, Tenn.

Kimball, Don (A), President, Kimball Safety Products Co., 7314 Wade Park Ave., Sta. B., Cleveland, Ohio.

Kimmel, Fred (B), Supt. of Way & Structures, Indianapolis Railway, 1150 W. Washington St., Indianapolis, Ind.

Kincaid, John H. (C), Engineer, Wellman Engineering Co., 7000 Central Ave., Cleveland, Ohio.

Kinder, G. P. (D), Welder, Kansas Gas & Electric Co.; (Res.) 246 North Dodge, Wichita, Kansas.

Kindsvater, E. F. (B), Test Engineer, Phillips Petroleum Co., Bartlesville, Okla.

King, Alfred B. (B), Owner, Alfred B. King & Co., 194 Chapel St., New Haven, Conn.

King, E. Doyle (D), Welding Operator, R. G. Le Tourneau, Inc.; (Res.) R. R. 1, McCarty Acres, Peoria, Ill.

King, Frederick J. (B), Chief Engineer, The Linde Air Products Co., 30 E. 42nd St., New York, N. Y.

King, Harold (B), Metallurgist, The Pfaudler Co., Lincoln Park Factory, Rochester, N. Y.

King, Harry W. (D), Welder, Frick Co., Inc.; (Res.) R. R. 4, Waynesboro, Pa.

King, K. V. (A), Engineer, Standard Oil Co. of Calif., 225 Bush St., San Francisco, Calif.

King, Lloyd Monroe (D), Arc Welding, A. B. Farquhar Co. Ltd.; (Res.) 215 S. Beaver St., York, Pa.

King, Robert (D), Owner, 2nd & Broadway Welding Co., 2nd & Broadway, Cincinnati, Ohio.

Kingsbury, Jesse A. (C), Senior Materials Engineer, Washington Navy Yard; (Res.) 5 Winston Drive, Country Club Village, Bethesda, Md.

Kinkead, Robt. E. (B), Consulting Engineer, 3441 Lee Road, Shaker Heights, Cleveland, Ohio.

Kinser, Roy (D), Shop Inspector, Wyatt Metal & Boiler Works, Box 3052, Houston, Texas.

Kinzel, A. B. (B), Chief Metallurgist, Union Carbide & Carbon Res. Labs., 30 E. 42nd St., New York, N. Y.

Kirby, R. L. (C), Selling, Hobart Brothers Co.; (Res.) Box 131, R. F. D. 1, Sharonville, Ohio.

Kirk, James P. (C), Salesman, Air Reduction Sales Co.; (Res.) 442 W. 62 Terr, Kansas City, Mo.

Kirk, L. E. (C), Chemical Engineer, Vickers Petroleum Co.; (Res.) 835 S. Madison, Wichita, Kansas.

Kissock, Alan (C), Vice-President in charge of Production, Climax Molybdenum Co., 500 Fifth Ave., New York, N. Y.

Kitchen, Norman A. (C), Welding Supervisor, York Lock & Safe Co.; (Res.) R. D. 7, York, Pa.

Kjeldgaard, Andrew, Jr. (D), Electric Welder, Lackawanna Steel Constr. Corp.; (Res.) Klesot Road, Clarence Center, N. Y.

Klass, Fred (B), Salesman, General Elec. Co., 630 Plymouth Bldg., Minneapolis, Minn.

Klauberg, W. E. (B), Shop Engineer, X-Ray Technician, Wyatt Metal & Boiler Works, Box 3052, Houston, Texas.

Klein, A. A. (C), Manager, National Cylinder Gas Co.; (Res.) 9149 Larke Ellen Circle, Los Angeles, Calif.

Klein, Wm. (B), J. B. Klein Iron & Fdy. Co., Oklahoma City, Okla.

Klevens, J. A. (B), President, J-K Welding Co., Inc., 4319—37th St., Long Island City, N. Y.

Klick, Alfred J. (D), Welder, American Laundry Machine Co.; (Res.) 45 Bartlett St., Rochester, N. Y.

Kline, Harry J. (C), Foreman, Penn Sugar Co., 1037 N. Del. Ave., Phila., Pa.

Kling, Fred E. (B), Assistant Chief Engineer, Carnegie-Illinois Steel Corp., 1318 Carnegie Bldg., Pittsburgh, Pa.

Klinke, H. O. (B), Asst. Welding Engineer, U. S. Navy Yard, Industrial Dept., Philadelphia, Pa.

Klos, Clifford H. (B), Inspector, M. W. Kellogg Co.; (Res.) 99-81—211th Place, Bellaire Park, L. I., New York.

Kluge, Leroy E. (D), Welder & Boilermaker, Utah Oil Ref. Co.; (Res.) 3230 So. 23rd, E., Salt Lake City, Utah.

Knabe, Martin C. (B), Manager, Keystone Engrg. Co., 909 Bessemer Bldg., Pittsburgh, Pa.

Knable, G. Elkins (B), Special Representative, Carnegie-Illinois Steel Corp., Carnegie Bldg., Pittsburgh, Pa.

Knachstedt, M. L. (B), President, Heintz Steel & Mfg. Co., 100 Clinton St., St. Louis, Mo.

Knapheide, E. W. (B), Welder, Sinclair Refg. Co., P. L. Dept.; (Res.) Grandview, Missouri.

Kneen, H. F. (C), Factory Supt., Lincoln Electric Co., 12818 Coit Road, Cleveland, Ohio.

Knepp, Peter W. (D), Instructor, Pennsylvania State College, R. D. 1, State College, Pa.

Knight, A. Rhodes (B), President, American Welding Co., Inc., 708 S. Caroline St., Baltimore, Md.

Knopf, Paul (D), Electric Arc Welding, Allan Iron & Welding Works; (Res.) 476 Garson Ave., Rochester, N. Y.

Knowles, A. M. (B), Assistant Engineer of Structures, Erie Railroad Co., 101 Prospect Avenue, Cleveland, Ohio.

Knox, Charles (B), Chief Engineer, Baker Ice Machine Co., Inc., 3601 No. 16th St., Omaha, Neb.

Knox, George (C), Architectural Engineer, U. S. Navy Dept., Navy Department, Bureau of Yards and Docks, Washington, D. C.

Knox, J. Winslow (D), Chief Instructor, Springfield School of Practical Training, 33 Winter St., Springfield, Mass.

Knudson, M. N. (B), Maintenance Supt., Minnesota & Ontario Paper Co., International Falls, Minn.

Koch, Bernard A. (C), Supt. of Welding, The Falk Corp., 3001 W. Canal St., Milwaukee, Wis.

Koerner, Harry (C), Sales, Air Reduction Sales Corp.; (Res.) 31 Landers Road, Kenmore, N. Y.

Kohlbry, F. P. (C), President, Machinery & Welder Corp., 1474 S. Vandeventer Ave., St. Louis, Mo.

Kohlbry, R. L. (B), Vice-President and Chicago Manager, Machinery and Welder Corp., 312 N. Loomis St., Chicago, Ill.

Kolasky, J. C. (C), Production Manager, The Youngstown Welding & Engineering Co.; (Res.) Box B, West Side Sta., Youngstown, Ohio.

Konetchy, Edward V. (B), Foreman Pipe Fabricating Dept., Walworth Co.; (Res.) 7 Brookfield St., Roslindale, Boston, Mass.

Koons, R. E. (B), Manager, Puritan Compressed Gas Corp., 2012 Grand Ave., Kansas City, Mo.

Kopper, H. D. (C), Welders Supply Co., 502 So. Main, Hutchinson, Kansas.

Kordic, J. F. (C), Welder, Electric Steel Foundry, 2115 N. W. 32nd Ave., Portland, Oregon.

Koresh, George (C), Research Engineer, A. O. Smith Corp.; (Res.) 4365 N. 25 St., Milwaukee, Wis.

Korn, Kenneth E. (D), Electric Welder, Southern Pacific Co.; (Res.) 2558—18th St., Sacramento, Calif.

Korn, M. P. (B), Consulting Engineer, 726 Washington St., Buffalo, N. Y.

Korner, C. E. (C), Welding Instructor, Alabama School of Trades, Gadsden, Ala.

Kortz, Howard R. (D), Foreman, General Electric Co.; (Res.) R. D. 7, Schenectady, N. Y.

Kozar, Leon C. (C), Niagara Welding & Boiler Works, Inc.; (Res.) 1731 Niagara St., Niagara Falls, N. Y.

Kraus, Rudolf (C), Welding Engineer, Stacey Bros. Gas Construction Co., Elmwood Place Sta., Cincinnati, Ohio.

Krause, Fred J. (C), Asst. General Manager, Combustion Engrg. Co., 1031 West Main St., Chattanooga, Tenn.

Krejci, E. L. (C), Acting Works Manager, % American Steel Foundries, 4831 Hohman Ave., Hammond, Ind.

Krisman, J. M. (C), Salesman, Hart Welding Supply Co., Oklahoma City, Okla.

Kriz, Robert J. (B), Chief Inspector, James H. Herron Co., 1360 W. 3rd St., Cleveland, Ohio.

Kroll, Victor J. (D), Electric Welder, Bethlehem Steel Co.; (Res.) 65 Rossmore Road, Jamaica Plain, Mass.

Krumholz, Alfred F. (B), President, Chicago Boiler Works, 1965 Clybourn Ave., Chicago, Ill.

Kruse, Harvey H. (D), Welder, American Locomotive Co.; (Res.) 312 Riverside Ave., Scotia, N. Y.

Kuehn, Ernest (B), Mechanical Manager, Electro-Motive Corp., Box M, Lagrange, Ill.

Kugler, Arthur N. (C), Mechanical Engineer, Applied Eng. Dept., Air Reduction Sales Co., 60 E. 42nd St., New York City, N. Y.

Kuhlberg, Carl F. (D), Welder & Cutter, General Electric Co.; (Res.) 516 Riverside Place, Scotia, N. Y.

Kuhn, H. C. (B), Engineer, Sinclair Refining Co., East Chicago, Ind.

Kumiega, Walter J. (C), Welder, Edward Valve & Mfg. Co., 1200—145th St., East Chicago, Ind.

Kunert, Max J. (C), 57th & Blue Ridge, R. R. 2, Kansas City, Mo.

Kunze, A. E. (B), Chief Metallurgist, Tennessee Coal, Iron & R. R. Co., Department Met. Insp. & Research, Ensley, Ala.

Kurtz, W. H. (C), Supt. of Maintenance, Sheffield Steel Corp., Kansas City, Mo.

Kutuchieff, Ivan (D), Operator, A. O. Smith Corp.; (Res.) 1436 N. 26th St., Milwaukee, Wis.

Kvasnicka, Wesley (C), Operator, Standard Iron & Wire Works; (Res.) 4822—30 Ave. So., Minneapolis, Minn.

Kyle, Peter, E. (C), Asst. Prof. of Mech. Eng., Mass. Inst. of Technology, Cambridge, Mass.

Kyler, Donald (D), Welder & Flame Cutter, Perfection Stone Co.; (Res.) 215 R. Middlebury St., Elkhart, Ind.

L

Lacey, Jim (C), Oakshire Apt. Hotel 12 W, Washington Bldg., Oak Park, Ill.

Lackey, Walter (D), Combination Welder, Bridgeport Mach. Co.; (Res.) 2408 East 9th, Wichita, Kansas.

Lacy, Walter P. (B), Supt., Lacy Mfg. Co., 1000 N. Main St., Los Angeles, Calif.

Ladd, R. J. (C), Welding Instructor, Kansas State College, Dept. Shop Practice, Manhattan, Kansas.

Ladd, Robert N. (C), Draftsman, Bureau of Yards & Docks Navy Dept., Wash., D. C.; (Res.) 30 Carroll Ave., Takoma Park, Md.

Ladd, Wendell (D), % F. O. Ladd, Paton, Iowa.

LaForce, Jean (C), Manager, Process Service, The Linde Air Products Co., 114 Sansome St., San Francisco, Calif.

La France, B. A. (D), 47 Center St., Jersey City, N. J.

La Grange, D. (B), President, Soudometal, S. A.; (Res.) 101A Avenue Du Prince D'Orange Uccle-Bruselles, Belgium.

Lahn, Sydney (B), Director, Maryland Welding Inst., 1022 Cathedral St., Baltimore, Md.

Laidley, C. D. (B), Adv. Manager, National Tank Co.; (Res.) 312 W. Queen St., Tulsa, Okla.

Laine, Frank (F), 2244 Morris Ave., Bronx, N. Y.

Lair, W. B. (B), Welding Engineer, York Safe and Lock Co.; (Res.) 1109 So. Queen St., York, Pa.

Lamach, John (C), Mech. Welder, Collison & Dolvon, Billings, Mont.

Lamb, H. C. (C), Partner, Elwyn E. Seelye & Co., 101 Park Ave., New York, N. Y.

Lambert, J. R. (B), Chief Engineer, Phoenix Bridge Co., Phoenxville, Pa.

Lamond, Nestor T. (B), Weld Foreman, United Engineering & Foundry Co., 1425 Hillman St., Youngstown, Ohio.

Landis, George G. (C), Chief Engineer, Lincoln Electric Co.; (Res.) 2559 Dysart Road, South Euclid, Ohio.

Landis, Houston E., Jr. (C), Apparatus Research & Development Dept., Air Reduction Sales Co., 181 Pacific Ave., Jersey City, N. J.

Lane, Arthur L. (C) Sales Engineer, Detroit Edison Co., 2000 Second Ave., Detroit, Mich.

Lane, Harry A. (D), Welder, 1122 W. 107 St., Los Angeles Calif.

Lane, Ralph S. (C), Welder, Lucey Boiler Corp., 951 E. 13th St., Chattanooga, Tenn.

Lang, Elmer (C), District Manager, American Brass Co., Investment Bldg., Washington, D. C.

Lang, P. A. (B), Foreman, Carrier Corp.; (Res.) R. D. 2, Jamesville, N. Y.

Lang, Philip George (B), Engineer of Bridges, The Baltimore and Ohio Railroad Co., 1301 Baltimore and Ohio Railroad Bldg., Baltimore and Charles Streets, Baltimore, Md.

Lange, Edward C. (C), Engine Draftsman, American Ship Building Co., Foot of W. 54 St., Cleveland, Ohio.

Lange, Frank (D), Arc Welder, Western Electric Co.; (Res.) 711 South Second Ave., Maywood, Ill.

Langen, Wm. E. (C), Welding Engineer, Massey-Harris Co.; (Res.) 12 Center St., Batavia, N. Y.

Langquepin, Jacques E. (C), President, La Société La Soudure Electrique Paris, 20 Rue Toulouse Lautree, Paris XVII, France.

Largent, Jack (B), M. O. P. Lines, 204 Union Station, Houston, Texas.

Larsen, B. W. (D), Engineer, Taylor-Winfield Corp.; (Res.) 736 Mahoning Ave., Warren, Ohio.

Larsen, Elmer (C), Shop Foreman, American Machine Works, 1210 Jackson St., Omaha, Nebr.

Larsen G. S. (A), Chief Engineer, Pittsburgh Piping & Equip. Co., 10—43rd St., Pittsburgh, Pa.

Larsen, Robert (D), Electric Welder, Bethlehem Steel Co.; (Res.) 250 Patapsco Ave., Dundalk, Md.

Larson, C. (D), Warehouse Manager, The Linde Air Products Co., 729 N. Penn. St., Indianapolis, Ind.

Larson, Ivan E. (C), Welding Foreman, General Railway Signal Co.; (Res.) 639 Avenue D, Rochester, N. Y.

Larson, Louis J. (B), Consulting Welding Engineer, 843 East Birch Ave., Milwaukee, Wis.

Lasaponara, George (D), Electric Welder, Pfaudler Co.; (Res.) 260 Independent St., Rochester, N. Y.

Lashbrook, C. A. (C), Vice-President, Okla. Testing Labs.; (Res.) 1145 N. W. 2nd St., Oklahoma City, Okla.

Lasher, E. R. (D), Electric Welder, General Fire Extinguisher Co.; (Res.) 267 Atlantic N. W., Warren, Ohio.

Laske, Frank (D), Welding Operator, American Laundry Machine Co.; (Res.) 194 Sunset Street, Rochester, N. Y.

Laszloffy, A., Jr. (D) Welder, Ornamental Iron Works; (Res.) 1113½ Manzanita St., Los Angeles, Calif.

Latiff, M. A. (F), Apprentice Engineer, Technical School, Indian Apprentice Engineer's Hostel Jamalpur, E. I. Ry., India.

Latta, A. L. (B), Welding Engineer, D. G. Latta, Ltd., Edmonton, Alberta, Canada.

Laughlin, G. C. (C), Field Engineer, Commonwealth Edison Co., 72 W. Adams St., Chicago, Ill.

Laulhere, Bernard M. (C), Technical Supervisor, Southern California Gas Co., 1700 Santa Fe Ave., Los Angeles, Calif.

Laurinatis, John, Jr. (C), Welding Supervisor, L. O. Koven & Bro., 80 West 55 St., Bayonne, N. J.

Lauterbach, E. W. (C), Chief Draftsman, Sun Oil Co., 1608 Walnut St., Philadelphia, Pa.

Lautner, W. F. (D), Supervisor Applied Engineering, Air Reduction Sales Co., Hawley Bldg., Wheeling, W. Va.

Lavallie, A. J. (D), Welder, W. A. Pope Co.; (Res.) 8259 Ingleside Ave., Chicago, Ill.

Lavine, G. (B), Craftsmen Welders, Inc., Bryant & Court Sts., Brooklyn, N. Y.

Lavoy, Louis (C), President, Louis Lavoy, Inc., Knapp St. & Harkness Ave., Brooklyn, N. Y.

Lawler, Dan (D), Welder, Chicago Bridge & Iron Co., 213 Michigan Ave., Albion, Mich.

Lawless, Thos. J. (C), Sales Manager, Champion Rivet Co., E. 108th & Harvard Ave., Cleveland, Ohio.

Lawrence, G. R. (C), Structural Engineer, Wellman Engineering Company; (Res.) 3401 Cedarbrook Rd., Cleveland Heights, Ohio.

Lawson, H. W. (B), Engineer, Bethlehem Steel Co.; (Res.) 1616 Millard St., Bethlehem, Pa.

Layfield, Elwood (B), Chief Engineer, Bechtel, McCone, Parsons Corp., 230 Edison Bldg., Los Angeles, Calif.

Layman, R. D. (C), District Manager, Lincoln Electric Co., 716 S. Division Ave., Grand Rapids, Mich.

Layton, M. S. (C), Chem. Engineer, The Steel Co. of Canada, Ltd., 525 Dominion St., Montreal, Canada.

Leach, Donald A. (C), Oklahoma District Sales Manager, Chicago Bridge & Iron Co., 1404-5 Hunt Bldg., Tulsa, Okla.

Leach, Robert H. (A), Vice-President, Handy & Harmon, Bridgeport, Conn.

Leahy, Wm. C. (C), Field Supt., Wyatt Metal & Boiler Works Co., Box 3052, Houston, Texas.

Leaveau, Victor A. (C), Partner, Liberty Welding & Iron Works, New Orleans, La.

LeBaron, R. P. (B), Estimator & Welding Engineer, Midwest Steel & Iron Works Co.; (Res.) 1426 So. Vine St., Denver, Colo.

Lebedeff, Michael N. (C), Plant Engineer, Chicago Bridge & Iron Co.; (Res.) 10501 So. Hamilton Ave., Chicago, Ill.

Le Bel, Everett M. (C), Welder Walworth Mfg. Co.; (Res.) 12 Amherst St., Nashua, New Hampshire.

Le Cain, G. H. (C), Instructor, Mech. Eng. Dept., Rochester Athenaeum and Mechanics Institute; (Res.) 55 Plymouth Ave., So., Rochester, N. Y.

Ledbetter, R. H. (C), Manager, Dept. of Metallurgy Insp. & Research, Tenn. Coal, Iron & R. R. Co., Birmingham, Ala.

Ledet, E. J. (C), Welder, Freeport Sulphur Co., Port Sulphur, La.

Lee, Daniel A. (C), President, Gas Weld Equipment Co.; (Res.) 496 Pleasant St., Malden, Mass.

Lee, Earle S. (C), Welding Engineer, Buda Company, Harvey, Ill.; (Res.) Box #92, Cedar Lake, Ind.

Lee, H. J. (D), Chief Clerk, D. B. Hunt Arc Welding Equip. Co.; (Res.) 834 Cascade Ave., S. W., Atlanta, Ga.

Lee, H. T. (B), Co-Owner and Manager, Pacific Welding & Machine Works, 551 So. St., Honolulu, T. H.

Lee, Marvin L. (D), Salesman, National Cylinder Gas Co., 238 S. Front St., Memphis, Tenn.

Lee, R. J. (B), Supt. of Erection, Pittsburgh-Des Moines Steel Co., Neville Island Station, Pittsburgh, Pa.

Leeper, A. W. (B), Welder, Mid-Continent Petroleum Corp.; (Res.) Box 577, Sand Springs, Okla.

Leeper, James A. (B), Foreman, Westinghouse Air Brake Co., Wilmerding, Pa.

Le Grand, Rupert (B), Detroit Editor, American Machinist, 2-144 General Motors, Detroit, Mich.

Leiby, Geo. L. (B), Arc Welder, General Electric Co.; (Res.) P. O. Box 2665, Station C, Philadelphia, Pa.

Leigh, A. G. (C), Marine Engineer & Naval Architect, The Ingalls Iron Works Co., Birmingham, Ala.

Leigh, W. K. (B), Works Manager, Industrial Service Engrs. Ltd.; (Res.) 212 Whitehall St., Footscray W. 11, Victoria, Australia.

Le Jeune, L. L. (D), Operator, Minneapolis Brewing Co.; (Res.) 3932 Xenia Ave., Robbinsdale, Minn.

Le Master, R. M. (C), Sales Engineer, Harnischfeger Sales Corp.; (Res.) 4439 Santa Fe Ave., Los Angeles, Calif.

Lenahan, Vincent (D), Factory Worker, Delco Appliance Div.; (Res.) 149 Cameron St., Rochester, N. Y.

Lengerhuis, Otto (D), Welder, The John B. Adt. Co., 326 N. Holiday St., Baltimore, Md.

Lenz, John J. (D), Welder, Cities Service Oil Co.; (Res.) 751 Johnson, Gary, Ind.

Leonard, B. H. (B), Welding Supt., Midwest Piping & Supply Co., 1450 S. 2nd St., St. Louis, Mo.

Leonard, Roy (C), Foreman, Electric Steel Castings Co.; (Res.) Brownsburg, Ind.

Leonhauser, A. O. (C), Welding Inspector, Gath Mach. Works; (Res.) 437 A. Tehama St., San Francisco, Calif.

Le Tourneau, Richard (B), Personnel Manager, R. G. Le Tourneau, Inc., Peoria, Ill.

Leveen, Laurence R. (B), Designing Engineer, Wire Dept., General Electric Co., 1 River Road, Bldg. 96, Schenectady, N. Y.

Levin, Nathan, Welder, Federal Shipbldg. & Dry Dock Co.; (Res.) 11 Armstrong Ave., Jersey City, N. J.

Levyn, Stanley M. (C), Manager, Acme Electric Welder Co., 5621 Pacific Blvd., Huntington Park, Calif.

Lewis, Alfred (D), Welder, Frigidaire Corp., 49 Fluhart Ave., Dayton, Ohio.

Lewis, Alfred H. (B), Chief Engineer, Swift Electric Welder Co., 6560 Epworth Blvd., Detroit; (Res.) 8507 Hendrie Blvd., Huntington Woods, Mich.

Lewis, Frank (C), Welding Foreman, Detroit Edison Co.; (Res.) 11050 Sanford Ave., Detroit, Mich.

Lewis, Harry R. (B), Chief Metallurgist, Ohio Seamless Tube Co., Shelby, Ohio.

Lewis, James T., Jr. (C), President, Lewis Welding & Eng. Corp., 881 Addison Road, Cleveland, Ohio.

Lewis, N. H. (D), Electric Welder, Goslin-Birmingham Mfg. Co.; (Res.) 829½ Graymant Ave., Birmingham, Ala.

Lewis, Robert B. (C), District Sales Manager, Stulz Sickles Co.; (Res.) 1324 Prospect Ave., Bethlehem, Pa.

Lewis, Theodore (D), Serviceman, Applied Engineering Dept., Air Reduction Sales Co.; (Res.) Route 3, Box 255, Louisville, Ky.

Lewis, Wm. W. (B), 1126 N. New St., Bethlehem, Pa.

Ley, Harry J. (C), Instructor, San Pedro High School, Alma & 15th Sts., San Pedro, Calif.

Libby, H. H. (C), Owner, Libby Welding Co., 2700 E. 15th St., Kansas City, Mo.

Lichtenwalter, T. R. (C), Metallurgical Dept., Republic Steel Corp., Massillon, Ohio.

Light, A. T. (A), Method Dept., York Ice Machinery Corp., York, Pa.

Lilienthal, F. W. (D), 31–28—91st St., Jackson Heights, Queens, L. I., N. Y.

Lincoln, J. F. (A), Lincoln Electric Co., Coit & Kirby Aves., Cleveland, Ohio.

Lincoln, P. M. (C), Lead Man, Reed Roller Bit Co.; (Res.) 4603 Wayne St., Kashmere Gardens, Houston, Texas.

Lincoln, R. B. (B), Director, National Weld Testing Bureau, Pittsburgh Testing Labs., Stevenson & Locust Sts., Pittsburgh, Pa.

Lindeke, Harold H. (B), Salesman, Nicols Dean & Gregg, St. Paul, Minn.

Lindgren, Henry (B), Boiler Shop Foreman, Hilo Iron Works, Hilo, Hawaii.

Lindimore, C. O. (C), Welding Supervisor, U. S. Engineers Repair Station; (Res.) 108 Oakwood Ave., Marietta, Ohio.

Lindley, R. W. (B), Prof. Engrg. Shop Practice, Purdue Univ., Lafayette, Ind.

Lindquist, Albert K. (D), Welder, Bklyn. Navy Yard; (Res.) 10 Monroe St., Apt. J. D. 9, New York, N. Y.

Lindsay, Laverne (C), Welder, Consolidated Coal Co.; (Res.) 209 E. Pine St., Herrin, Ill.

Lindsey, I. T. (D), Welder, National Cast Iron Pipe Co., Birmingham, Ala.

Liner, W. R. (B), Shop Foreman, Wyatt Metal & Boiler Works, Box 3052, Houston, Texas.

Lippencott, L. E. (C), Compressed Industrial Gases, Inc., 2131 N. E. 10th St., Oklahoma City, Okla.

Lipschitz, William (C), Manager Shop, Pittsfield Iron Works & Coal Supply, 458 South St., Pittsfield, Mass.

Lipsky, Joseph J. (D), Supervisor, American Locomotive Co., Schenectady, N. Y.

Liston, Earl C. (C), Supt., Eaton Metal Prods. Co., 4800 York St., Denver, Colo.

Lisy, John W. (C), Elec. & Gas Welder, Electric Vacuum Co.; (Res.) 3617 Independence Rd., Cleveland, Ohio.

Litzenberger, W. M. (C), 12 Riverview Place, Rochester, N. Y.

Livingston, John (D), Welder, Western Electric Co.; (Res.) 4335 W. 17th St., Chicago, Ill.

Llewellyn, F. T. (E), Temp. address; % Dr. F. B. Llewellyn, 72 Afterglow Ave., Montclair, N. J.

Lloyd, D. S. (B), Service Engineer, Dominion Oxygen Co., Ltd., 159 Bay St., Toronto, Ont.

Lloyd, J. F. (B), 9A. Page Moss Parade, Huyton Near Liverpool, England.

Lloyd, S. Elvin (C), Supervising Welder, Scott Paper Co., Front & Market Sts., Chester, Pa.

Loane, P. M. (C), Foreman, Chicago Bridge & Iron Co., 119 Long Ave., Hillside Sta., Elizabeth, N. J.

Lockeman, Geo. F. (C), Procter & Gamble Co., Ivorydale, Ohio.

Lockman, Edward L. (C), Staff Engineer, Maintenance Dept., Boston Elevated Railway, 31 St. James Ave., Boston, Mass.

Locum, Edward (D), Welder, Link Belt Co., 11929 S. Union Ave., Chicago, Ill.

Loeffler, G. B. (B), Clearing Machine Corp.; (Res.) 3230 W. 62nd Place, Chicago, Ill.

Loesch, Al (B), Asst. Supt., R. G. Le Tourneau, Inc.; (Res.) 418 E. Republic St., Peoria, Ill.

Loeser, John C. (B), Marine Surveyor, Gebauhr & Loeser, 66 Beaver St., New York, N. Y.

Logmann, A. A. (B), General Supt., Pullman-Standard Car Mfg. Co., Wabash & 8th St., Michigan City, Ind.

Lohnes, Stearn (C), Vice-President & General Manager, A. Lucas & Sons, Peoria, Ill.

Loiselle, Edward (D), Welder, 111 E. 127 St., New York City.

Long, R. E. (B), Chief Engineer, Hollup Corp., 3357 W. 47th Place, Chicago, Ill.

Long, Richard (B), Inspector, Mutual Boiler Ins. Co., of Boston, 4 N. 8th St., St. Louis, Mo.

Long, Theodore S. (C), Asst. General Manager, Taylor-Winfield Corp., Warren, Ohio.

Long, V. K. (D), 4557 Diversey Ave., Chicago, Ill.

Longwell, R. C. (C), Link Belt Co., 300 W. Pershing Road, Chicago, Ill.

Lonsdale, Wm. (B), Vice-President, Charge of Mfg., Foster Wheeler Corp., Carteret, N. J.

Loomis, C. A. (C), Engineer (Naval Architect), Navy Dept., Bureau of Construction & Repair, Washington, D. C.

Looney, R. L. (B), Division Manager, Big Three Welding & Equip. Co., 102 S. Cheyenne, Tulsa, Okla.

Loos, C. E. (B), Manager Structural and Plate Bureau, Metallurgical Division, Pittsburgh, Carnegie-Illinois Steel Corp.; (Res.) 6529 Brighton Road, Ben Avon, Pa.

Lord, Arthur S. (B), National Inst. Inc., 93 Lafayette St., Newark, N. J.

Lorentz, R. E. (F), 550 Montclair Ave., Bethlehem, Pa.

Lorio, A. B. (B), Manager, Lorio Brothers, 11 Riversdale Ave., Pretoria, S. A.

Lothian, James (B), Master Mechanic, Ewa Plantation Co.; (Res.) P. O. Box 206, Ewa, Hawaii.

Loucks, F. B. (C), National Cylinder Gas Co., 2615 W. Greves St., Milwaukee, Wis.

Louis, Harry (C), c/o Hobart Bros., 228 Washington Ave., No., Minneapolis, Minn.

Louis, J. (C), Director, Babcock & Wilcox Co.; (Res.) 48, Rue La Boetie, Paris, 80, France.

Love, R. H. (D), Welding Operator, 711 So. Broadway, Wichita, Kansas.

Low, Albert, S. (B), Vice-President & Chief Engineer, The Austin Co., 6112 Euclid Ave., Cleveland, Ohio.

Lowe, L. D. (C), Sales Engineer, State Machinery Co., Inc.; (Res.) 509 So. 18th, Newcastle, Ind.

Lowener, V. (C), Westerbrogade 9 B, Copenhagen, Denmark.

Lowrie, William (B), John A. Roebling's Sons Co., 19 Rector St., New York, N. Y.

Lowry, Gilmore T. (D), Welder, Silver-Roberts Iron Works Inc.; (Res.) 2745 Irving St., Denver, Colo.

Lucas, John W. (C), President, Southwest Welding & Mfg. Co., 3201 W. Mission Road, Alhambra, Calif.

Luck, W. V. (D), Welder, Cincinnati Milling Mach. Co.; (Res.) Yale Ave., Terrace Park, Ohio.

Ludtke, Alfred (D), 14752 Perry Ave., Harvey, Ill.

Luetke, J. A. (D), Supervisor, Applied Engrg., Air Reduction Sales Co., 3623 E. Marginal Way, Seattle, Wash.

Luger, Karl E. (C), Metallurgical Engineer, Stainless Steel Division, Carnegie-Illinois Steel Corp., Box 159, Houston, Texas.

Lukas, J. C. (B), Sales Engineer, Ransome Concrete Machine Co., Dunellen, N. J.

Lum, S. Clark (B), Service Engineer, Commercial Acetylene Supply Co.; (Res.) 983 Colonial Ave., Union, N. J.

Lumadue, C. H. (D), Dresser Ave. & Rancocas Road, Springside, N. J.

Lundgren, James E. (D), Mechanic, Raytheon Mfg. Co.; (Res.) 93 Lowden Ave., Somerville, Mass.

Lush, Chas. B. (D), Welder, Stanley Garden, Plow Works, Box 244, Earlham, Iowa.

Lutes, E. B. (C), 7004 Mornington Road, Dundalk, Md.

Luther, Wm. F. (D), Inspector, Delco Appliance Co.; (Res.) 172 Stenson St., Rochester, N. Y.

Luthy, S. F. (C), Treasurer, Camp & Luthy, Inc., 1924 Baltimore Ave., Kansas City, Mo.

Lutz, John G. (C), Salesman, Ingersoll Rand Co., 2327 Locust, St. Louis, Mo.

Lynch, D. E. (C), Asst. Prof. Shop Practice, Kansas State College; (Res.) 1519 Pierre, Manhattan, Kansas.

Lyons, L. (D), Welder, American Stove Co.; (Res.) 240 E. 171st St., Hazel Crest, Ill.

Lyons, W. E. (C), Sales Engineer, Williams & Co., Inc., 901—37 Pennsylvania Ave., N. S., Pittsburgh, Pa.

Lyster, H. M. (B), Manager, Dominion Welding Engineering Company, Ltd., P. O. Box 494, Montreal, Canada.

Lytikainen, Elmer John (D), Electric & Gas Welder, Federal Pipe & Supply Co.; (Res.) 2115 S. 2nd Ave., Maywood, Ill.

Lytton, Charles W. (C), District Manager (Oil City Office), The Lincoln Electric Co., Miller Park, Franklin, Pa.

M

MacCutcheon, Edward M., Jr. (C), Asst. Naval Arch., Bureau of Marine Insp. & Nav., Dept. of Commerce Bldg., Rm. 1060, Washington, D. C.

MacDonald, Roderick (C), Inspector, R. W. Hunt Co.; (Res.) 30 W. Harriet Ave., Palisade Park, N. J.

MacDonald, Robert H. (C), Sales Engineer, Steel Co. of Canada, Ltd., 67 Yonge St., Toronto, Ont.

MacGuffie, Charles I. (B), Welding Specialist, General Electric Co., 1405 Locust St., Philadelphia, Pa.

MacIntosh, C. D. (C), Sales Engineer, Copperweld Steel Co.; (Res.) 1910—37th St., N. W., Washington, D. C.

MacKenzie, F. W. (C), Sales Engineer, The Federal Mach. & Welder Co.; (Res.) 149 Washington St., N. E., Warren, Ohio.

MacKenzie, James T. (B), Chief Metallurgist, American Cast Iron Pipe Co., Box 2603, Birmingham, Ala.

Mackey, Glenn C. (C), Supervisor of Welding, Fate Root Heath Co., 30 Clairmont Ave., Mansfield, Ohio.

MacKinney, John J. (C), Welding Research, Edw. G. Budd Mfg. Co.; (Res.) 5224 Wayne Ave., Philadelphia, Pa.

MacKinnon, Norman J. (A), Supt., Hull Construction, Bath Iron Works, Bath, Maine.

MacMillin, Howard F. (B), President, Hydraulic Press Mfg. Co., Mt. Gilead, Ohio.

MacPhee, G. Y. (D), Owner & Operator, P. O. Box 2, Camrose, Aita., Canada.

MacPherson, Kenneth (D), Welder, Boom Boiler and Welder; (Res.) 1625 Waterbury Road, Lakewood, Ohio.

MacQueen, Ernest (D), Welder, General Electric Co.; (Res.) Ocean St., Nahant, Mass.

MacSorely, Wm. W. (C), Manager, Wm. W. MacSorely Inc., 1079 Woodycrest Ave., Bronx, N. Y.

Maddox, W. A. (B), Supervisor of Welding, Cincinnati Milling Machine Co., Oakley, Cincinnati, Ohio.

Madsen, K. K. (B), Welding Engineer, Esab-Kjellberg Co., Trekronergade, 92, Copenhagen, Denmark.

Madson, Arthur E. (B), Welding Instructor, The Lincoln Electric Co.; (Res.) 13903 Shaw Ave., E. Cleveland, Ohio.

Maeurer, F. J. (C), Specialist, Applied Engrg. Dept., Air Reduction Sales Co., 60 E. 42nd St., New York, N. Y.

Magan, Thomas I. (B), President, Thomas Conlin Co., 450 Oakwood Blvd., Chicago, Ill.

Magee, C. M. (C), Research Engineer, Engrg. Div., Assn. of American Railroads, 59 E. Van Buren, Chicago, Ill.

Magrath, Joseph G. (C), Engineer, Air Reduction Sales Co., 60 East 42nd St., New York, N. Y.

Mahan, B. (D), Chief Shopfitter, Diving School, Navy Yard, Washington, D. C.

Maher, J. William (C), Welding Foreman, The Maryland D. D. Co., P. O. Box 6306, So. Station, Baltimore, Md.

Maine, J. F. (C), Chief Engineer, Republic Structural Iron Works; (Res.) 12003 Scottwood Ave., Cleveland, Ohio.

Mainwaring, Norman, R. (B), Marine Dept., Standard Oil Company of Calif., 425 Kentucky Ave., Berkeley, Calif.

Maire, Stephen F. (C), Process Service Dept., The Linde Air Products Co., 6—240 General Motors Bldg., Detroit, Michigan.

Majewski, Paul M. (D), Orrick, Missouri.

Male, Milton (B), Asst. Research Engineer, U. S. Steel Corp. of Delaware, 436 Seventh Ave., Pittsburgh, Pa.

Malek, Henry (C), Proprietor, Iowa Electric Welding Co., 215 First St. N. E., Cedar Rapids, Iowa.

Maley, Geo. D. (D), Welder, Henry C. Grebe & Co.; (Res.) 4526 Cedar Ave., Hammond, Ind.

Mallett, E. A. (B), Vice-President in Charge of Engrg., The Taylor-Winfield Corp., 1052 Mahoning Ave., N. W., Warren, Ohio.

Mallett, John E. (C), Sales Engineer, The Denton & Anderson Co.; (Res.) 1745 Wymore Ave., #7, E. Cleveland, Ohio.

Mallory, Walter S. (C), Associate Engineer, U. S. Navy Dept., Glen Echo, Md.

Malsh, Fred O. (D), Foreman, Cutler & Hammer Mfg. Co.; (Res.) 1834 No. 56th St., Milwaukee, Wis.

Mann, G. T. (C), Representative, The Lincoln Electric Co., 600 West 41st St., Chicago, Ill.

Mann, Vernon E. (C), Foreman, General Electric Co., Bldg. 12, Schenectady, N. Y.

Mansfield, Roy A. (C), Manager, Southern Oxygen Co. Inc.; (Res.) 4106 Penhurst Ave., Baltimore, Md.

Mant, Frank M. (C), General Inspection, American Potash & Chemical Corp.; (Res.) 2921 Kansas Ave., South Gate, Calif.

Manternach, A. S. (B), General Supt., American Welding & Mfg. Co., Warren, Ohio.

Manz, J. W. (C), Junior Field Engineer, Commonwealth Edison Co., Construction Dept., 72 W. Adams St., Chicago, Ill.

Maradudin, A. P. (C), Standard Oil Co., El Segundo, Calif.

Marble, A. E. (B), Metallurgical Engineer, Jones & Laughlin Steel Corp., J. & L. Building, Pittsburgh, Pa.

Marchinek, Fred E. (F), Glasgow, Montana.

Mardick, John K. (C), Vice-President, Islands Welding & Supply Co., Ltd., P. O. Box #94, Honolulu, Hawaii.

Mariette, E. H. (D), Welder Foreman, Mid West Piping & Supply Inc.; (Res.) 2902 Paola Ave., Los Angeles, Calif.

Marini, Louis G., Jr. (C), The McKay Co., York, Pa.

Markee, Arthur A. (C), Sales Representative, The Linde Air Products Co., 2604 Kerwick Road, University Heights, Ohio.

Markel, Orville (C), Asst. Master Mechanic, Commonwealth Edison Co., 22nd & Fisk St., Chicago, Ill.

Markham, Robert (D), Welder, Pa. York Gas Corp., Box 214, Scio, N. Y.

Marking, Lowell Paul (C), Co. 630 CCC, Camp Brandon-Morris, Joliet, Ill.

Marks, Raymond (C), Layout & Welder, Oilgear Co.; (Res.) 1459 So. 76 St., West Allis, Wis.

Marland, Arthur (A), Plant Manager, John Wood Mfg. Co., Inc., Conshohocken, Pa.

Maroni, Angel A. (B), Importer, Maroni Hermanos & Cia., 24 de Noviembre No. 1763–69, Buenos Aires, Argentina.

Marr, A. Philip (D), Welder, General Motors Co.; (Res.) 93 Michigan St., Rochester, N. Y.

Marran, Vincent P. (B), Vice-President & Chief Engineer, Walsh, Holyoke Steam Boiler Works, Inc.; (Res.) 1801 Northampton St., Holyoke, Mass.

Marsh, Chas. T. (B), Service & Dem., Taylor-Winfield Corp.; (Res.) 10219 S. Racine Ave., Chicago, Ill.

Marsh, H. G. (B), Asst. Manager Specialty Sales, Carnegie Steel Co., 316 Carnegie Bldg., Pittsburgh, Pa.

Martin, Charles E. (D), Instructor of Welding & Machine Shop, Billings Polytechnic Institute, Polytechnic, Mont.

Martin, Chas. W. (C), 974 Pine St., San Francisco, Calif.

Martin, Clyde K. (D), Welder, Taylor Instrument Co.; (Res.) 64 Walzer Road, Rochester, N. Y.

Martin, John E. (B), Welding Foreman, C. C. C. & St. Louis R. R.; (Res.) R. R. 12, Box 577, Indianapolis, Ind.

Martin, N. N. (B), Welding Foreman, Foster Wheeler Corp., Dansville, N. Y.

Martin, Otto V. (D), Welder, Mid-Continent Petroleum Corp.; (Res.) 417 S. 58th St., W. Ave., Tulsa, Okla.

Martin, S. B. (D), Supervisor, Applied Engineering Dept., Air Reduction Sales Company; (Res.) 1823 Sils, Louisville, Ky.

Martin, Wm. (D), Welder, Western Electric Co.; (Res.) 1319 S. 50th Court, Cicero, Ill.

Martindale, Lloyd R. (D), Electric Welding Dolomite Marine Corp.; (Res.) 218 Magnolia St., Rochester, N. Y.

Martinson, Edwin O. (C), Mechanical Engineer, American Machine & Metals, Inc., East Moline, Ill.

Mason, Allan Shaw (B), Works Manager, J. Merryweather & Son; (Res.) 32 New Engand Road, Scottsville Mantsburg, Natal, S. Africa.

Mason, Frederick D., Jr. (D), Welding Clerk, General Electric Co.; (Res.) 319 Essex St., Salem, Mass.

Mason, J. H. (C), President, Universal Metal Welding Co., 2260 Bailey Ave., Buffalo, N. Y.

Mason, James H. (C), District Supervisor, Engrg. Dept., Fidelity and Casualty Co. of N. Y., 60 Sansome St., San Francisco, Calif.

Massa, William J. (C), Chief Engineer, Bergen Point Iron Works, West 5th St., Bayonne, N. J.

Matheney, Sherman (D), Operating Welder, Keystone S. & W. Co., 420 Knoxville Ave., Peoria, Ill.

Matheny, James H. (D), Leadingman Welder, U. S. Naval Aircraft Factory; (Res.) 7160 Radbourne Road, Del. County, Upper Darby, Pa.

Matheny, Thomas (C), Welder Maintenance, Keystone Steel & Wire Co., 1516 First St., Peoria, Ill.

Mather, William (D), Electric Welder, Eastman Kodak Co.; (Res.) 83 Alpha St., Rochester, N. Y.

Mathers, Paul F. (C), Designer, Una Welding, Inc. 1615 Collamer, Cleveland, Ohio; (Res.) 14626 Ardenall Ave., E. Cleveland, Ohio.

Mathews, John M. (C), Secretary, The Harris Calorific Co., 5501 Cass Ave. N. W., Cleveland, Ohio.

Mathews, S. B. (C), Sales, Edgecomb Steel Co.; (Res.) 166 Glentay Road, Lansdowne, Pa.

Mathews, W. W. (B), Sales Engineer, Lacy Mfg. Co., 601 Washington Bldg., Los Angeles, Calif.

Mathy, E. L. (C), First Vice-President, Victor Welding Equipment Co., 844 Folsom St., San Francisco, Calif.

Matte, Joseph, Jr. (B), Chief Structural Engineer, Albert Kahn Inc., 345 New Center Bldg., Detroit, Mich.

Mattern, P. M. (B), Asst. to General Sales Manager, Wilson Welder & Metals Co. Inc.; (Res.) 39 Oxford St., Apt. G-3, Unit 3, Newark, N. J.

Matthies, Arthur (D), Apprentice, Gleason Works; (Res.) 43 Balsam St., Rochester, N. Y.

Maurath, George A. (A), President, Maurath, Inc., 7309 Union Ave., Cleveland, Ohio.

Maxwell, G. L. (B), Asst. Supt., Gas T. & D., Philadelphia Electric Co., Edison Bldg., 18th Floor, 9th & Sansom St., Philadelphia, Pa.

Maxwell, K. A. (A), National Tube Co., Christy Park Dept., McKeesport, Pa.

Maxwell, W. H. (C), Supt., Link-Belt Co., P. O. Box 85, Indianapolis, Ind.

Mayberry, Walter R. (B), Elect. Engineer, The Commonwealth Mfg. Corp., 4208 Davis Lane, Cincinnati, Ohio.

Mayer, Frederick C. (D), Welder, Chicago Rivet & Machine Co.; (Res.) 1532 N. Mason Ave., Chicago, Ill.

Mayer, Steve (D), Handyman, Arrochris Realty Corp.; (Res.) 678 Humboldt St., Brooklyn, N. Y.

Mayor, Wm. (D), Welder, 14113 Glenside Road, Cleveland, Ohio.

McAdams, Paul F. (C), Foreman of Jig Dept., R. G. Le Tourneau Inc.; (Res.) 132 Wyoming St., Peoria, Ill.

McAtee, F. E. (B), Welding Supervisor, Chicago Bridge & Iron Co., P. O. Box 277, Birmingham, Ala.

McAuliffe, J. J. (C), Engr. Mech., General Electric Co.; (Res.) 1626 Bradley St., Schenectady, N. Y.

McBride, Geo. Morgan (C), Dist. Manager, Air Reduction Sales Co., 3623-E. Marginal Way, Seattle, Wash.

McCaffrey, M. P. (C), President, M. P. McCaffrey Inc., 2121 E. 25th St., Los Angeles, Calif.

McCaghren, J. R. (C), Shop Supt., Crane Co., Birmingham, Ala.

McCain, W. W. (D), Elect. Welder, Goslin-Birmingham Mfg. Co.; (Res.) 1802—14th Ave., Bessemer, Ala.

McCandlish, D. F. (C), District Manager, Air Reduction Sales Co., Box 2457, Stockyard Sta., Oklahoma City, Okla.

McCarthy, H. P. (C), Lead Man, Reed Roller Bit Co., Rt. 11, Box 138, Houston, Texas.

McCarthy, Jim (C), Supervisor of Welding, Ingersoll Steel & Disc. Co.; (Res.) 303 Marshall St., St. Paul, Ill.

McCarthy, L. B. (B), Genl. Supt., General Fireproofing Co., Youngstown, Ohio.

McCarthy, R. E. (D), McCarthy Tank & Equip. Co., 3030 M. St., Bakersfield, Calif.

McCarthy, Wm. C. (D), Welder, Fore River Ship Yard; (Res.) R. F. D. Main St., Rockland, Mass.

McCartney, James W. (D), Instructor, Frank Wiggins Trade School; (Res.) 1908 Browning, Los Angeles, Calif.

McClara, W. H. (C), Engrg. Dept., Consolidated Steel Corp.; (Res.) 840 Burnside Ave., Los Angeles, Calif.

McClelland, R. L. (C), Salesman, The Linde Air Products Co.; (Res.) Powers Hotel, Rochester, N. Y.

McClintock, C. E. (D), Welder, Denver Tramway Corp.; (Res.) 4320 Tennyson St., Denver, Colo.

McClintock, Harvey C., Jr. (C), Student Engineer, M. W. Kellogg Co., 1435 Lexington Ave., New York, N. Y.

McCloud, F. A. (B), President, The Thornton Co., 6901 Morgan Ave., Cleveland, Ohio.

McClung, A. F. (D), Winfield, Kansas.

McClure, Clyde E. (C), Johnson Supply Co., 1414 Wazee St., Denver, Colo.

McClure, E. E. (C), Teacher, Glendale High School, 1440 E. Broadway, Glendale, Calif.

McConnell, Ray (C), Welding Foreman, Combustion Eng. Co., 1032 W. Main St., Chattanooga, Tenn.

McCord, H. C. (C), Salesman, Aluminum Co. of America, 605 Southern Bldg., Washington, D. C.

McCormick, Don E. (C), Salesman, The Lincoln Electric Company; (Res.) 2108 South Broadway, Pittsburg, Kansas.

McCormick, John B. (C), District Manager, Lincoln Electric Co., 1914 Utah Ave., Seattle, Wash.

McCormick, K. A. (B), Welding Inspector, Wyatt Metal & Boiler Works, P. O. Box 3052, Houston, Texas.

McCormick, V. B. (C), The Ohio Public Service Co., Lorain, Ohio.

McCoy, Arthur M. (B), Contracting Engineer, Calvert Iron Works, 1195 Mickleberry St., Atlanta, Ga.

McCoy, W. N. (C), Welder, Bethlehem Steel Co.; (Res.) 6347 Ingleside Ave., Chicago, Ill.

McCracken, Edwin D. (C), Salesman, John A. Roebling's Sons Co., 19 Rector St., New York, N. Y.

McCracken, R. Scott, Jr. (C), Sales, R. S. McCracken & Son, 636 N. 13th St., Philadelphia, Pa.

McCreery, H. L. (C), President, H. L. McCreery & Co., 2036 E. 22nd St., Cleveland, Ohio.

McCright, P. H. (D), Electric Welder, Chicago Bridge & Iron Co., R. D. 3, Greenville, Pa.

McCuen, Harper (D), Welder, Duquesne Light Co.; (Res.) P. O. Box 243, Imperial, Pa.

McCune, Charles A. (B), Secretary, Magnaflux Corp., 25 W. 43rd St., New York City.

McCune, J. C. (B), Director of Research, Westinghouse Airbrake Co., Wilmerding, Pa.

McCutcheon, W. P. (C), Indust. Sales Manager, Birmingham Electric Co., 2100 No. 1st Ave., Birmingham, Ala.

McDermid, J. A. (C), The Ohio Public Service Co., Mansfield, Ohio.

McDonald, E. L. (C), Inspector, Smith-Emery Co., 920 Santee St., Los. Angeles, Calif.

McDonald, Lewis (C), Chicago Bridge & Iron Co., 37 W. Van Buren St., Chicago, Ill.

McDonald, S. F. (D), Owner, McDonald Machine Shop, Livingston, Texas.

McDonald, V. G. (D), Foreman, Kansas City Structural Steel, 1742 S. 23rd St., Kansas City, Kansas.

McDonough, L. J. (B), Engineer, Clearing, Machine Co.; (Res.) 3247 Maple Ave., Berwyn, Ill.

McDonough, W. R. (C), Owner, W. R. McDonough & Co., 1404 E. 9th St., Cleveland, Ohio.

McElfish, P. D. (C), Materials Engineer, Standard Oil Co. of Calif., Box 1437 Arcade Annex, Los Angeles, Calif.

McFaddin, M. D. (D), Electric Welder, Christopher Iron Works; (Res.) Route 7, Wichita, Kansas.

McFarland, James R. (D), Applied Engineering Dept., Air Reduction Sales Co.; (Res.) 54 Lynwood Ave., Wheeling, W. Va.

McFarland, R. E. (A), Engineer (Welding), Western Electric Co., Hawthorne Station, Chicago, Ill.

McGhie, Roland W. (D), Welder, Lockheed Aircraft Inc.; (Res.) 3015 Sagamore Way, Eaglerock, Calif.

McGinley, Francis V. (D), 810 Terrace 49, Los Angeles, Calif.

McGinnis, C. E. (B), General Manager, Board of Mech. Engineers, Room M-85, City Hall, Los Angeles, Calif.

McGlasson, S. G. (D), Field Foreman, Wyatt Metal & Boiler Works, Box 3052, Houston, Texas.

McGonigle, J. L. (C), Inspector of Steel, New York Board of Transportation; (Res.) 522 Tilghman St., Allentown, Pa.

McGrath, G. E. (C), Welder, McGrath Welding Co., 4026 Nicholas, Omaha, Nebr.

McGredy, R. H. (B), Dist. Manager, Harnischfeger Corp., 1626 K St., N. W., Room 810, Washington, D. C.

McGregor, Carl J. (B), Asst. Manager, Manufacturers Sales, American Steel & Wire Co., 208 So. La Salle St., Chicago, Ill.

McGuire, H. K. (D), Welder Lockheed Aircraft Co.; (Res.) 1100 So. Kern Ave., Los Angeles, Calif.

McHose, K. W. (C), Manager of Sales, Kalman Steel Corp., 1047 New Broad St. Sta. Bldg., Philadelphia, Pa.

McIntire, David P. (C), Welding Inspector, York Safe & Lock Co.; (Res.) R. D. 5, York, Pa.

McIver, George W. (C), Chief Engineer, Toledo Edison Co., Toledo, Ohio.

McKay, Edwin O. (C), Industrial Engineer, Foster-Wheeler Corp., Carteret, N. J.

McKeel, G. M. (D), 1020 Seneca, Apt. 507, Seattle, Wash.

McKeever, R. S. (B), % The Parkersburg Rig & Reel Co., Box 730, Tulsa, Okla.

McKeighan, J. Stuart (C), Salesman, Lincoln Electric Co., 12231 Clifton Blvd., Lakewood, Cleveland, Ohio.

McKelvey, J. F. (B), President, Vulcan Steel Tank Corp., Box No. 1844, Tulsa, Okla.

McKelvey, L. N. (B), Chief Engineer, Vulcan Steel Tank Corp., Box No. 1844, Tulsa, Okla.

McKenzie, Wm. E. (C), Assistant Welding Engineer, Metallurgical & Testing Division, Washington Navy Yard; (Res.) 2909 5th St., S. E., Washington, D. C.

McKGreed, Wm. (B), 67 Summer Ave., Washington, Pa.

McKinley, J. C. (C), Apex Mach. & Mfg. Co., 113 S. Denver, Tulsa, Okla.

McKinney, L. T. (D), Layout Man, Goslin-Birmingham Mfg. Co.; (Res.) Route 1, Irondale, Ala.

McKinzie, Daniel J. (B), Asst. Chief Operating Engineer, Chicago District Electric Generating Corp., Post Office Box 65, Hammond, Ind.

McLain, A. R. (C), Welding Engineer, Combustion Eng. Co., Hedges-Walsh-Weidner Division; (Res.) 622 Georgia Ave., Apt. 110, Chattanooga, Tenn.

McLaren, L. G. (D), Shop Foreman, Wyatt Metal & Boiler Works; (Res.) 319 W. 34th St., Houston, Texas.

McLaughlan, O. B. (A), Mech. Engineer, Freeport Sulphur Co., Port Sulphur, La.

McLaughlin, W. A. (D), Welding Operator, Mid-Continent Refinery; (Res.) R. R. 6, Box 80, Tulsa, Okla.

McLean, D. G. (B), Welder, R. R. Howell Co.; (Res.) 616 Beacon St., S. E., Minneapolis, Minn.

McManus, S. M. (C), Muskogee Iron Works, Muskogee, Okla.

McMillan, Ainslie (D), Welding Foreman, The Falk Corp., Milwaukee, Wis.

McMillan, James D. (D), Harrah, Oklahoma.

McMillan, J. H. (C), Master Mechanic, Kansas Gas & Electric Co., Electric Plant, 3rd & Kelly, Wichita, Kansas.

McMillan, Robt. P. (B), Sales Manager, Southern Oxygen Co., Arlington, Va.

McMullen, L. A. (C), Supt., Day & Night Heater Co.; (Res.) 635 Hillcrest Blvd., Monrovia, Calif.

McMurtry, L. C. (C), Manager of Erection, Horton Steel Works, Ltd.; (Res.) Fort Erie, Ont., Canada.

McNamar, Chas. C. (B), Proprietor, McNamar Boiler & Tank Mfg. Co., P. O. Box 882, Tulsa, Okla.

McNutt, A. D. (B), International Business Machines Corp., Endicott, N. Y.

McNutt, Louis C. (C), Welding Engineer, M. W. Kellogg Co., Ft. of Danforth Ave., Jersey City, N. J.

McPhee, L. S. (B), Welding Supt., Whiting Corp., 157 Lathrop Ave., Harvey, Ill.

McVey, John W. (F), Willis Sweet, Moscow, Idaho.

McWhirt, T. B. (D), Welder, Mid-Continent Petroleum Co.; (Res.) 10 W. King, Tulsa, Okla.

Meacham, F. L. (B), Manager, Household Engineering Div., Frigidaire Div. of General Motors Corp., Dayton, Ohio.

Meacham, William A. (B), Engineer, Standard Steel Fab. & Boiler Works, Inc.; (Res.) 417 Smith St., Seattle, Wash.

Meadowcroft, Joseph W. (A), Asst. Works Manager, Edward G. Budd Mfg. Co., 24th & Hunting Park Ave., Philadelphia, Pa.

Mebs, Erwin H. (C), Chief Metallurgist, The Ohio Steel Foundry Co; (Res.) Dale Drive, Lima, Ohio.

Mechem, R. H. (B), Boss-Welder, Pittsburgh Steel Co., Monessen, Pa.

Meeker, Lincoln D. (B), Manager Electric Welding Sales, General Electric Co., 1 River Road, Schenectady, N. Y.

Mehaffey, Frank B. (C), Asst. Sales Mgr., Air Reduction Sales Co., 122 Mt. Vernon St., Uphams Corner, Boston, Mass.

Mehl, Walter A. (D), Electric Welder, Prosperity of Syracuse, N. Y.; (Res.) 104 Hathaway Road, East Syracuse, N. Y.

Meissner, C. E. (C), Sales Engineer, United States Steel Corp., Normandy Bldg., Washington, D. C.

Meister, W. F. (C), Supt. of Repairs, Pittsburgh Steamship Co., 1600 Rockefeller Bldg., Cleveland, Ohio.

Melby, F. C. (C), Commonwealth Edison Co., 3501 S. Pulaski Road, Chicago, Ill.

Mello, Gustav (C), Foreman, Bender Body Co.; (Res.) 112 Harvard Ave., Elyria, Ohio.

Mello, Peter (D), Welder, Bender Body Co.; (Res.) 10412 Madison Ave., Cleveland, Ohio.

Melzarek, C. A. (C), Salesman, The Linde Air Co.; (Res.) 412 S. Bluff, Wichita, Kansas.

Mentges, Frank (D), Machinist, Youngstown Welding & Engrg. Co.; (Res.) 511 Carroll St., Youngstown, Ohio.

Menzies, J. C. (C), District Manager, C. E. Phillips & Co., 332 So. Jefferson St., Chicago, Ill.

Mercier, Joseph (C), General Foreman, Montreal Tramways Co., 6060 St. Dennis, Montreal, Canada.

Meredith, A. B. (D), Inspector, Delco Appliance Corp.; (Res.) 123 Willis Ave., Rochester, N. Y.

Merkt, Joseph (C), Serviceman, Applied Engineering Dept., Air Reduction Sales Co.; (Res.) P. O. Box 840, Louisville, Ky.

Merritt, R. A. (B), Magnolia Airco Prods. Co., P. O. Box 319, Houston, Texas.

Mesta, L. W. (B), Vice-President, Mesta Machine Co., P. O. Box 1466, Pittsburgh, Pa.

Meston, John (B), Supervisor, The Pfaudler Co., Lincoln Park Factory, Rochester, N. Y.

Mettler, F. W. (B), Registered Engineer, 999 Rushleigh Road, Cleveland Heights, Ohio.

Metzger, William F. (C), President, H. O. Swoboda, Inc., P. O. Box 245, New Brighton, Pa.

Meurer, P. G. (C), Supt., Manitoba Bridge & Iron Works, Ltd., Winnipeg, Canada.

Meybin, Robt. J. (A), Vice-President & General Manager, Virginia Bridge Co., Roanoke, Va.

Meyer, Arnold (C), Mech. Engineer, Heil Co.; (Res.) 1848 Rocky Point, Pewaukee, Wis.

Meyer, Arthur (D), Route 2, Box 68, Bakersfield, Calif.

Meyer, Amel R. (D), Welder, General Plastics Inc.; (Res.) 237 Heath St., Buffalo, N. Y.

Meyer, C. G. (D), Welder J. & L. Steel Co.; (Res.) 878 Woodward Ave., Brooklyn, N. Y.

Meyer, Fred G. (B), Sales, Air Reduction Sales Co.; (Res.) 2535 East 18th St., Davenport, Iowa.

Meyer, G. F. (B), District Manager, Machinery & Welder Corp., 515 E. Buffalo, Milwaukee, Wis.

Meyer, Michael L. (C), Foreman, York Ice Machinery Corp., Thomasville, Pa.

Meyer, Paul K. (C), Salesman, Victor Equip. Co.; (Res.) Box 122, Bakersfield, Calif.

Meyer, William (D), Electric and Acetylene Welder, Pittsburgh Gravel Company; (Res.) 267—14th St., Ambridge, Pa.

Meyers, A. M. (B), Chief Engineer, Kansas City Structural Steel Co., 21st & Metropolitan Ave., Kansas City, Kansas.

Meyers, E. J. (C), General Foreman, General Electric Co., Dept. 107—Bldg. 14-1-B, Pittsfield, Mass.

Meyers, Fred H. (D), Texhoma, Okla.

Meyers, G. A. (B), Salesman, Magnolia Airco Prods. Co., P. O. Box 319, Houston, Texas.

Meyers, L. J. (C), Hull Inspector, Pittsburgh Steamship Co.; (Res.) 2019 E. 81 St., Chicago, Ill.

Meyers, Phil. (D), Welder Foreman & Operator, Deep Rock Oil Corp.; (Res.) 1143 E. Third St., Cushing, Okla.

Meyerson, Milton E. (B), President, St. Louis Testing Laboratory, Inc., 2317 Chouteau Ave., St. Louis, Mo.

Michaels, E. E. (A), Manager, Birmingham Plant, Chicago Bridge & Iron Co., Box 277, Birmingham, Ala.

Michel, Clarence H. (C), President, Maryland Metal Bldg. Co., Race & McComas Sts., Baltimore, Md.

Michel, H. A. (D), Goetz Brewing Co., St. Joseph, Mo.

Middleton, R. J. (B), Asst. Chief Engineer, C. M. St. P. & P., 898 Union Sta., Chicago, Ill.

Mideke, Joseph M. (C), Vice-President, Mideke Supply Co.; (Res.) 2505 N. W. 19, Oklahoma City, Okla.

Midnight, S. A. (C), Structural Designer, The American Shipbldg. Co., West 54th St., N. W., Cleveland, Ohio.

Mika, George (C), Supt., American Steel Works, 1211 W. 27th St., Kansas City, Mo.

Mika, H. L. (C), Welding Foreman, Standard Steel Co.; (Res.) 412 No. 13th St., Kansas City, Kansas.

Mikhalapov, George (B), Welding Engineer, Baldwin-Southwark Corp., Philadelphia, Pa.

Miklosi, Dr. Ing. Cornelius (C), Director, I. E. T. Electricity Supply and Tram, 11 Piata Coronini, Timisoara, Romania.

Mikulak, John (B), Mechanical Engineer, Electric Machinery Mfg. Co., 1331 Tyler St., N. E., Minneapolis, Minn.

Milaty, T. M. (D), Combination Welder, Eastman Kodak Co.; (Res.) 337 Bennington Drive, Rochester, N. Y.

Millar, C. S. (C), % Ingalls Iron Works Co., Birmingham, Ala.

Miller, Alexander (C), District Engineer, American Inst. of Steel Const., 2120 NBC Bldg., Cleveland, Ohio.

Miller, A. O. (A), President, The Petroleum Iron Works Co., Box 539, Sharon, Pa.

Miller, B. M. (B), Salesman, Southern Oxygen Co., Arlington, Va.

Miller, Cecil J. (C), 1816 N. Pennsylvania, Indianapolis, Ind.

Miller, Crosby (C), Bridge Engineer, Chesapeake & Ohio Ry. Co., Richmond, Va.

Miller, C. P. (D), Welder, W. H. Hough Co.; (Res.) 912 N. Jefferson, Seminole, Okla.

Miller, DaCosta R. (D), Electric Welder, Reading Co. Locomotive Shops; (Res.) 1122 Spring St., Reading, Pa.

Miller, David (D), Prop., Back Bay Welding Co., 678 Brookline Ave., Brookline, Mass.

Miller, Edward L. (D), Welder, Delco Appliance Co.; (Res.) 22 Dyson St., Rochester, N. Y.

Miller, Elmer L. (C), Chief Inspector, R. G. Le Tourneau, Inc., Ft. of Grant St., Peoria, Ill.

Miller, F. Harold (C), Stop 16½, Troy Road, Schenectady, N. Y.

Miller, George J. (C), Foreman, York Ice Machinery Co.; (Res.) 527 N. Hartley St., York, Pa.

Miller, Harry C. (D), Welding Foreman, The United Welding Co., Box No. 84, Trenton, Ohio.

Miller, H. J. (D), Welder, Monsanto Chemical Co.; (Res.) 10232 Lackland Road, Overland, Mo.

Miller, H. L. (A), Metallurgist, Republic Steel Corp., Massillon, Ohio.

Miller, Irvin (D), 20 W. Laurel St., Tremont, Pa.

Miller, J. Garfield (C), Assistant Supt., Frick Co., Waynesboro, Pa.

Miller, John S. (C), Supervisor of Welding, New York, New Haven & Hartford R. R., New Haven, Conn.

Miller, Dr. John W. (B), Metallurgist, Reid-Avery Co., Chesapeake & Cleveland Aves., Dundalk, Md.

Miller, Lee K. (C), Welder, Linderme Tube Co.; (Res.) 22570 Ivan Ave., Euclid, Ohio.

Miller, Ray (D), Welder, Buehler Tank & Welding Works; (Res.) 1517—76th Place, Los Angeles, Calif.

Miller, Robert O. (C), Supt., Bethlehem Foundry & Machine Co., 3rd & Ridge St., Emaus, Pa.

Miller, V. (D), Arcrods Corp., Sparrows Point, Md.

Miller, Vern V. (C), Sales Representative, Hobart Bros. Co.; (Res.) 2762 No. 53rd St., Milwaukee, Wis.

Miller, Wilber B. (B), Research Metallurgist, Union Carbide & Carbon Research Labs., P. O. Box 580, Niagara Falls, N. Y.

Miller, W. G. (C), Indust. Sales Dept., Kansas Gas & Electric Co., Wichita, Kansas.

Miller, W. Lloyd (C), Plant Supt., York Ice Machinery Corp., 914 Market Ave., S. Canton, Ohio.

Millette, Millard M. (C), Res. Engineer, A. O. Smith Corp.; (Res.) Y. M. C. A., Milwaukee, Wis.

Milligan, R. (C), Supervising Engineer, The Ocean Accident & Guarantee Corp. Ltd., 1 Park Ave., New York City.

Mills, Ellsworth L. (B), Vice-President, Bastian-Blessing Co., 240 E. Ontario St., Chicago, Ill.

Mills, Gordon (D), Welder, Winnipeg Electric Co.; (Res.) Ste. B. Karlston Apts., 537 Victor St., Winnipeg, Man., Canada.

Minard, Herbert (B), General Purchasing Agent and Asst. Treasurer, The Coleman Lamp and Stove Co., Wichita, Kansas.

Minch, W. T. (C), Repairman and Welder, Atlanta Gas Light Co.; (Res.) 569 Techwood Drive, Apt. 33, Atlanta, Ga.

Minehart, Glenn K. (C), Welding Instructor, Jourden Diesel Schools, Inc.; (Res.) 617 Hay St., Wilkinsburg, Pa.

Mingotte, Eugene (B), Proprietor, Middlesex Welding Co., 58 Washington St., Somerville, Mass.

Minnotte, Charles F. (B), Assistant Manager, Pacific Coast, H. H. Robertson Co., 691 Mills Building, San Francisco, Calif.

Minnotte, J. F. (B), Secretary-Treasurer, Minnotte Brothers Co., 1201 House Bldg., Pittsburgh, Pa.

Minter, H. E. (D), Harrah, Okla.

Miron, John (D), Operator, Electric Welder, Harris Struct. Steel of New York, Inc.; (Res.) Box 15, New Market, N. J.

Misener, G. L. (C), Union Carbide Co., Niagara Falls, N. Y.

Miser, Vernon (F), 312 Elm St., Stillwater, Okla.

Misker, Francis (C), Welder, Consolidated Coal Co.; (Res.) 706 So. 21st St., Mt. Vernon, Ill.

Miskoe, W. I. (C), Manager, Lincoln Electric Co., 307 E. State St., Peoria, Ill.

Mitchell, Anthony J. (B), Class B Welder, Jeffrey Mfg. Co.; (Res.) 720 Thomas Ave., Columbus, Ohio.

Mitchell, Fred E. (C), Asst. Engineer, The M. W. Kellogg Co.; (Res.) 440 E. Fourth Ave., Roselle, N. J.

Mitchell, H. F. (D), Welder, Delco Appliance Corp.; (Res.) 72 Frances St., Rochester, N. Y.

Mitchell, James (B), Director of Research and Technical Development, Stewarts and Lloyds Limited, Corby, Northants, England.

Mitchell, J. A. (B), Manager Prod., Belmont Iron Works, 22nd St. and Wash. Ave., Philadelphia, Pa.

Mitchell, James R. (C), Foreman, Farrel Birmingham Co., 13 Parket St., Ansonia, Conn.

Mitchell, William (C), Modelmaker & Animation Man, 790 Argyle Road, Brooklyn, N. Y.

Mivelaz, William A. (B), Salesman, Industrial Oxygen Co., 1377 So. 7th St., Louisville, Ky.

Mochel, Norman L. (B), Metallurgical Engineer, Westinghouse Electric & Mfg. Co.; (Res.) 606 Thayer St., Ridley Park, Pa.

Mohring, Robt. F. (C), American Allsafe Co.; (Res.) 64 N. Harlem Road, Snyder, N. Y.

Moir, Stewart G. H. (B), Instructor, Electric Welding, Bayonne Tech. H. S., 30th St.—Ave. A, Bayonne, N. J.

Moisseiff, Leon S. (B), Consulting Engineer, 99 Wall St., New York, N. Y.

Monahan, Joseph (B), Engineer, Joseph Monahan Co., 351 Indiana Ave., N. W., Grand Rapids, Mich.

Monroe, L. Chellis (C), Publisher, Welders' Digest; (Res.) 2228 East 70th Place, Chicago, Ill.

Monroe, R. P. (B), Hollup Corp., 3357 W. 47th Place, Chicago, Ill.

Monsarrat, Nicholas (C), Mech. Engineer, Pantex Pressing Mach., Inc., Central Falls, R. I.

Montebana, Joe (D), Electric Welder, Goslin-Birmingham Mfg. Co.; (Res.) 712 No. 20th St., Birmingham, Ala.

Moody, Chas. G. (B), Draftsman, Mass. Inst. of Tech.; (Res.) 16 Garden St., Cambridge, Mass.

Mooney, John A. (B), Treas., New England Gas Products Inc.; (Res.) 27 West St., Charlestown, Mass.

Moore, Alfred M. (D), Electric Welder, Bethlehem Shipbldg. Corp., 14 Pope St., Quincy, Mass.

Moore, Clarence H. (C), Treasurer, Aetna Iron & Steel Co.; (Res.) 2130 Laura St., Jacksonville, Florida.

Moore, Gladstone (D), Electric Welder, Semet Solovay Engrg. Co.; (Res.) Rt. 2, Box 199, Stoneville, N. C.

Moore, James H. (D), Welder, American Stove Co., Glenwood, Ill.

Moore, J. R. (B), Vice-President & General Manager, The Capital Iron Works Co., 7th & Adams Sts., Topeka, Kansas.

Moore, L. M. (B), Supt., Otis Elevator Co., Harrison, N. J.

Moore, O. F. (C), Engineer, M. & W. Minneapolis St. Rwy. Co., 1 S. 11 St., Minneapolis, Minn.

Moore, Ralph O. (C), Tank Mfg., Ralph O. Moore Co.; (Res.) 2811 E. Admiral, Tulsa, Okla.

Moore, R. S. (C), Dist. Manager, Air Reduction Sales Co., 1256 Logan St., Louisville, Ky.

Moore, Samuel (C), Welding Dept. Foreman, Aiton & Co. Ltd.; (Res.) 63 Otter St., Strutts Park, Derby, England.

Moore, Thomas J. (D), Electric Welder, General Electric Co.; (Res.) 1940 Passunk Ave., Philadelphia, Pa.

Moore, W. P. (D), Electric Welder, Coleman Lamp & Stove Co.; (Res.) 575 Hendryx Ave., Wichita, Kansas.

Moorer, Wm. D. (B), President, Moorlane Company, Box 1679, Tulsa, Okla.

Moran, Robert (B), Welding Supervisor, Missouri Pacific Rwy., St. Louis, Mo.

Moran, Thomas P. (C), Welding Engineer, General Motors Corp., 6307 W. Fort St., Detroit, Mich.

Morehead, F. H. (B), Vice-President, Walworth Co., Inc., 60 E. 42nd St., New York, N. Y.

Morelock, J. E. (C), Vice-President, Converse Bridge & Steel Co., 2408 Vance Ave., Chattanooga, Tenn.

Morenus, D. M. (D), Welder, General Electric Co.; (Res.) R. F. D. 7, Sunnyside Road, Schenectady, N. Y.

Morgan, G. H. (B), Welding Contractor, Service Electric Welding Co., 305 E. 11th St., Tacoma, Wash.

Morgan, Gus (C), Welder, Tulsa Boiler Machinery Co.; (Res.) 732 College St., Tulsa, Okla.

Morgan, J. (C), Groissier & Shlagler; (Res.) 148 Walnut St., Brookline, Mass.

Morgan, J. B. (B), Factory Supt., Jabez Burns & Sons, Inc., 563—11th Ave., New York, N. Y.

Morgan, Lester (C), Sales, Puritan Compressed Gas Corp., 2012 Grand Ave., Kansas City, Mo.

Morgan, Nathan W. (C), Highway Bridge Engineer, Public Roads Administration, Washington, D. C.

Morgan, I. W. (C), Arc Welding Supervisor, Chicago Surface Lines; (Res.) 1426 E. 65th St., Chicago, Ill.

Morgan, W. Trefor (B), Chief Metallurgist, Taylor Instrument Companies, 95 Ames St., Rochester, N. Y.

Morrill, Guy (B), Manager, Mid West Piping & Supply Co., 520 Anderson St., Los Angeles, Calif.

Morrin, J. W. (C), Prop., Somerset Welding & Spring Co.; (Res.) 210 W. Patriot, Somerset, Pa.

Morris, Dr. Alan (B), Chief Metallurgist, Bridgeport Brass Co., Bridgeport, Conn.

Morris, A. P. (D), Welding Business, New England Welding Lab., 88 St. Stephen St., Boston, Mass.

Morris, E. D. (C), Engineer, Process Development Dept., The Linde Air Products Co., 30 E. 42nd St., New York, N. Y.

Morris, Ernest J. (D), Welder, Pas Lumber Co.; (Res.) Reserve Sask., Canada.

Morris, F. H. (B), Press Room Foreman, R. B. M. Mfg. Co., Logansport, Ind.

Morris, James F. (B), Testing Engineer & Inspector, Pittsburgh Testing Laboratory; (Res.) 1009 Columbus St., Houston, Texas.

Morris, Ned (C), Sales Engineer, The Aluminum Co. of America, 605 Southern Bldg., Washington, D. C.

Morris, T. C. (B), Manager of Engineering, The Solvay Process Co., Nitrogen Division, Hopewell, Va.

Mortensen, Leonard R. (C), Production Clerk, Federal Shipbuilding & Dry Dock Co.; (Res.) 458—14th St., Brooklyn, N. Y.

Morton, A. F. (D), Oxyacetylene Welder, Dept. of Sanitation, City of New York; (Res.) 3803 Snyder Ave., Brooklyn, N. Y.

Moses, A. J. (A), General Manager, Hedges-Walsh-Weidner Div., Combustion Engineering Co., Chattanooga, Tenn.

Mosher, Leland C. (D), Mosher Garage, Cattaraugus, N. Y.

Mosley, A. Robert (D), 26 Henry Ave., Palisade Park, N. J.

Moss, Herbert H. (B), Development Dept., The Linde Air Products Co., 686 Frelinghuysen Ave., Newark, N. J.

Moss, N. F. (B), Welding Supt., Wagner Electric Corp.; (Res.) 7508 Wayne, St. Louis, Mo.

Mossholder, Meredith G. (C), Partner, Okla. City Machine Works, 1633 W. Main St., Oklahoma City, Okla.

Mostyn, Henry J. (D), Welder, Eastman Kodak Co.; (Res.) 353 So. Clinton Ave., Rochester, N. Y.

Mott, Clement (D), 97-11—118th St., Richmond Hill, N. Y.

Mottar, R. F. (B), International Harvester Co., Fort Wayne, Ind.

Motter, Harry W. (C), 25 N. Duke St., York, Pa.

Moul, Raymond H. (D), Arc Welding, A. B. Farquhar Co. Ltd.; (Res.) 248 S. Pershing Ave., York, Pa.

Moulder, A. W. (A), Vice-President, Grinnell Company, Inc., 260 W. Exchange St., Providence, R. I.

Mowatt, Theodore A. (C), Elec. Engineer, U. S. Navy Yard, Box 224 College Park, Md.

Mower, D. A. (B), Master, Acting Head of Dept., Metalwork, Mechanic Arts High School; (Res.) 40 Walnut St., Sharon, Mass.

Moxley, T. R. (C), Master Mechanic, Wheeling Steel Corp., Steubenville, Ohio.

Moyer, Daniel E. (F), Rt. 1, Box 1405, Tujunga, Calif.

Moynahan, George B. (B), Publicity Dept., The Oxweld Railroad Service Co., 230 No. Michigan Ave., Chicago, Ill.

Muckleroy, J. M. (D) Salesman, United Welding Supply Co., 3118 Harrisburg Blvd., Houston, Texas.

Mueller, George C. (C), Foreman, Cullen Friestedt Co.; (Res.) 4301 Potomac Ave., Chicago, Ill.

Muir, Jas. H. (C), District Manager, Taylor-Winifield Corp., 230 Park Ave., New York City, N. Y.

Mullane, Clinton W. (C), Welding Supervisor, Union Pacific R. R., 4733 So. 13 St., Omaha, Nebr.

Muller, Jules (B), 10743 S. Wood St., Chicago, Ill.

Mulligan, C. A. (B), President, Shamrock Welding Service Corp.; (Res.) 803 Heights Blvd., Houston, Texas.

Mullin, Alfred N. (B), President, Islands Welding & Supply Co. Ltd., P. O. Box 94, Honolulu, T. H.

Mummey, L. R. (D), Mt. Shasta, Calif.

Munro, Wm. (D), 535 E. 102nd St., Chicago, Ill.

Munschauer, George E. (C), Chief Engineer, Niagara Machine and Tool Works, 683 Northland Ave., Buffalo, N. Y.

Munz, Hermann J. (C), Engineer, Neu Brunnenstrasse 14-IV, Mainz a. Rhein, Germany.

Murcell, U. C. (B), President and General Manager, U. C. Murcell, Inc., 5701 E. Leneve St., Los Angeles, Calif.

Murphey, H. Bruce (C), Dist. Sales Manager, Chicago Bridge & Iron Co., 1033 Liberty Bank Bldg., Dallas, Texas.

Murphy, C. (D), 10856 Wabash Ave., Roseland Station, Chicago, Ill.

Murphy, Charles P. (C), Assoc. Naval Architect, Bureau of Marine Insp. & Navigation; (Res.) 4501 Iowa Ave., Washington, D. C.

Murphy, W. E. (B), Manager, Power Sales Dept., Northern States Power Co., Minneapolis, Minn.

Murray, J. J. (B), Supt., Heltzel Steel Form & Iron Co., Warren, Ohio.

Murray, Stephen W. G. (D), Electric Arc Welder, Navy Yard; (Res.) 12 Mousam St., Sanford, Maine.

Murrell, Eric H. (B), Chief Inspector, United Engineers & Constructors, Inc., 1401 Arch St., Philadelphia, Pa.

Murta, Clarence (D), Arc Welder, Massey Harris Co., Alexander, N. Y.

Myhontre, L. O. (C), Manager of Mfg., Westinghouse X-ray Co., 21-16—43rd Ave., Long Island City, N. Y.

N

Naegele, W. R. (D), Operator, Kirk & Blum Mfg. Co.; (Res.) 1952 Sterling Ave., N. College Hill, Cincinnati, Ohio.

Naes, Birger (C), Assistant Marine Engineer, Inspector of Machinery U. S. N., 6902 Ridge Blvd., Brooklyn, N. Y.

Nation, Robt. B. (B), Manager, Wash. Office, International Nickel Co.; 915 Shoreham Bldg., Washington, D. C.

Neal, Frank C., Jr. (C), Application Engrg., General Electric Co.; (Res.) 120—12th St., Schenectady, N. Y.

Near, A. E. (D), Welding Supervisor, General Electric Co.; (Res.) R. D. #1, Rosendale Road, Schenectady, N. Y.

Neary, Arthur T. (D), Welder, Boston Steel Co.; (Res.) 18 St. Mary's St., Malden, Mass.

Neaveill, Chester E. (C), Welding Inspector, R. G. Le Tourneau, Inc.; (Res.) R. F. D. #2, Peoria, Ill.

Neel, A. O. (C), Plate Shop Foreman, Goslin-Birmingham Mfg. Co.; (Res.) General Delivery, Gardendale, Ala.

Neeld, R. S. (D), 2812 Neeld Ave., Pittsburgh, Pa.

Neely, Arthur C. (D), Supt. Field Mechanical Equip. Lihue Plantation Co., Lihue-Kauai-Hawaii.

Neet, J. G. (C), Salesman, The Linde Air Products Co., National Bank of Tulsa Bldg., Tulsa, Okla.

Nel, Gert Jacobus (B), % Chief Engineer, South African Railways & Harbours, Sta. Bldgs., Pretoria, S. Africa.

Nellans, H. M. (C), Ethyl Treater, Vickers Petroleum Co., Potwin, Kansas.

Nelson, Charles S. (D), Welder, Mid-Continent Petroleum Corp.; (Res.) 425 S. 58 West Ave., Tulsa, Okla.

Nelson, Fred (C), Welding Supervisor, Southern Calif. Gas Co., 11131 Pine St., Lynwood, Calif.

Nelson, John (B), Vice-President, Butler Manufacturing Co., 900—6th Ave. S. E., Minneapolis, Minn.

Nelson, Julius (D), Welder, U. S. Engineers, Rio Vista, Calif.

Nelson, J. T. (C), Asst. Sales Manager, Air Reduction Sales Co., 3623 E. Marginal Way, Seattle, Wash.

Nelson, T. H., Jr. (C), Sales, Arcos Corp.; (Res.) 156 W. King St., Malvern, Pa.

Nelson, T. Holland (B), Consulting Engineer, T. Holland Nelson Metallurg. Lab., Villanova, Pa.

Nemecek, A. A. (D), Supervisor-Cutter, Butler Mfg. Co.; (Res.) 2823 E. 10th St., Kansas City, Mo.

Nesbitt, W. S. (C), Chicago Bridge & Iron Co., Greenville, Mercer Co., Pa.

Netchvolodoff, V. V. (C), Sales Engineer, Hill Equip. Eng. Co., 3431 Chouteau Ave., St. Louis, Mo.

Netherwood, Joseph S. (C), Asst. Supt. Motive Power & Equipment, Southern Pacific Lines in Texas & Louisiana, 913 Franklin Ave., Houston, Texas.

Neumann, Dr. Ing. A. J. (C), Welding Equipment, International Welding Co. Ltd., 52a, Goldhawk Road, London W. 12., England.

Neumann, Othon G. (C), Ave. Juares No. 60, Mexico, D. F.

Nevin, K. S. (B), Secretary and Treasurer, Railway & Industrial Engrg. Co., P. O. Box 98, Greensburg, Pa.

Newby, H. L. (B), Supt., American Pipe & Steel Corp.; (Res.) 1210 Pine St., So. Pasadena, Calif.

Newell, H. D. (B), Chief Metallurgist, The Babcock & Wilcox Tube Co., Beaver Falls, Pa.

Newhall, M. H. (C), Chief Draftsman, J. B. Klein Iron & Fdy. Co., 1401 N. W. 3rd St., Oklahoma City, Okla.

Newton, B. E. (C), Welding Insp., Raymond G. Osborne Lab., Rieves Strong Bldg., Los Angeles, Calif.

Newton, G. H. (B), Operating Manager, Graver Tank & Mfg. Corp., East Chicago, Ind.

Newton, Robert (B), District Manager, The Lincoln Electric Co., 2101 N. E. Kennedy St., Minneapolis, Minn.

Niblack, J. D. (D), Operator, Oklahoma Gas & Electric Co.; (Res.) 1917 N. W. 28th, Oklahoma City, Okla.

Nichols, C. R. (C), Engineer, The Cleveland Electric Illmtg. Co., 75 Public Sq., Cleveland, Ohio.

Nichols, Leonard E. (C), Welding Engineer, National Electric Welding Mach. Co., 6-255 General Motors Bldg., Detroit, Mich.

Nickell, K. V. (C), Salesman, Air Reduction Sales Co., Birmingham, Ala.

Nickerson, J. F. (C), President, Nickerson & Collins Co., 435 N. Waller Ave., Chicago, Ill.

Nickum, William C. (B), Naval Architect, W. C. Nickum & Sons, 400 Polson Building, Seattle, Wash.

Niebanck, Richard J. (C), Plant Engineer, National Carbide Corp., Box 97, Ivanhoe, Va.

Nigriny, Victor (D), Welder, General Electric Co.; (Res.) 1101 Willett St., Schenectady, N. Y.

Nihlen, Arvid (C), Supervising Engineer, American Heat Reclaiming Corp.; (Res.) 1216—28th St. No., Birmingham, Ala.

Noe, William A. (D), Rt. 3, Box 404, Racine, Wis.

Noll, Paul E. (B), Contracting Engineer, Consolidated Steel Corp., Ltd., Box 1348 Arcade Sta., Los Angeles, Calif.

Noonan, Thos. J. (B), Asst. Supt., The East Ohio Gas Co., Foot of E. 62nd St., Cleveland, Ohio.

Nordahl, Noble (C), Field Supt., Okla. Natural Gas Co.; (Res.) Box 987, Oklahoma City, Okla.

Nordine, A. (D), Welder, Link Belt Co.; (Res.) 306 W. 108th Place, Chicago, Ill.

Noret, C. C. (C), Compressed Industrial Gases Inc.; (Res.) R. F. D. 2, Box 248-D, Oklahoma City, Okla.

Norris, Edward W., (C), Mechanical Engineer, Stone & Webster Eng. Corp., 49 Federal St., Boston, Mass.

Northcutt, B. D. (D), Operator, Okla. Natural Gas Co.; (Res.) Route 8 Box 58, Oklahoma City, Okla.

Northcutt, Harold W. (B), Supt. of New Construction, Bethlehem Steel Co., Shipbuilding Div., Mariner Harbor, Staten Island, N. Y.

Norton, Walter (D), Owner & Manager, Norton's Welding Shop, 910 W. 3rd St., Tulsa, Okla.

Norwood, Edgar A. (C), Structural Engineer, J. R. Worcester & Co., 79 Milk St., Boston, Mass.

Noyes, Mason S. (C), Marine Engineer, Bureau of Engineering, Navy Dept., 4321 Navy Building, Washington, D. C.

Nutt, Buell W. (C), President, The Safety Equipment Service Co., 1228 St. Clair Ave., Cleveland, Ohio.

Nutter, Joseph T. (D), Layout & Fitter, Youngstown Welding & Engrg. Co.; (Res.) 119 Beechwood Dr., Youngstown, Ohio.

Nystrom, Carl (D), Machinist, E. E. Johnson, Inc.; (Res.) 640 No. Fairview Ave., St. Paul, Minn.

Nystrom, K. F. (B), Mechanical Assistant to Chief Operating Officer, C. M. St. P. & P. Railroad Co., Milwaukee, Wis.

Nystrom, Karl T. (B), Asst. Mech. Engineer, Standard Railway Equip. Mfg. Co.; (Res.) 10533 So. Maplewood Ave., Morgan Park Sta., Chicago, Ill.

O

Obert, C. W. (B), Consulting Engineer, Engrg. Dept., The Linde Air Products Co., 30 E. 42nd St., New York, N. Y.

Obreiter, Robert B. (C), Eastern Representative, Electroloy Co.; (Res.) P. O. Box 244, Bloomfield, N. J.

O'Callaghan, J. J. (C), Salesman, The Linde Air Products Co., 2305 E. 52nd St., Los Angeles, Calif.

Ochsner, Edward (D), Welder Solvay Process Co.; (Res.) R. D. 2, Jamesville, N. Y.

O'Connor, Michael (B), Plate Shop Foreman, Heltzel Steel Form & Iron Co., Warren, Ohio.

O'Day, L. W. (C), Welding Engineer, The Lincoln Electric Co., 600 W. 41st St., Chicago, Ill.

Odell, Aden G. (B), Director of Welding Instruction, Opportunity School, Denver Public Schools,; (Res.) 3011 Lafayette St., Denver, Colo.

Odom, J. Carl (C), Welder, Pacific Gas & Electric Co.; (Res.) 5127 Shattuck Ave., Oakland, Calif.

Oechsle, S. John (C), President, Metalweld, Inc., 26th & Hunting Park Ave., Philadelphia, Pa.

Oehler, A. G. (B), Editor, Simmons-Boardman Pub. Co., 30 Church St., New York, N. Y.

Oehmig, Edward W. (C), General Manager, Cavalier Corp., Chattanooga, Tenn.

Oelhaf, Carl (C), Secretary, Meier & Oelhaf, 177 Christopher St., New York, N. Y.

Oesterlein, W. (B), Chief Engineer, Harnischfeger Corp., 4400 W. National Ave., Milwaukee, Wis.

Ogilivie, C. L. (D), Mechanic Foreman, R. S. Jewett Shop; (Res.) Route 1, Box 109, Arwin, Calif.

O'Halloran, Thos. S. (C), Assoc. Mech. Engineer, Bur. of Ordnance—U. S. Navy Dept., U. S. Naval Gun Factory, Wash. Navy Yard,; (Res.) 313 No. Irving St., Arlington, Va.

O'Hara, Eric (C), Sales Engineer, Cincinnati Gas & Electric Co., 4th & Main Sts., Cincinnati, Ohio.

Ohlssen, Wm. (C), Plant Engineer, Federal Shipbuilding & Dry Dock Co., Kearny, N. J.

Ojima, J. (C), Mech. & Welding Engineer, Hitachi Engrg. Works, Kameido Factory, Tokyo, Japan.

Oka, Horace Y. (D), Box 237, Waialua Oahu, T. H.

Olcott, Floyd B. (B), Member of Firm Shirley, Olcott & Nichols, 220 Mills Bldg., Washington, D. C.

Oliver, Frank J. (C), Associate Editor, The Iron Age, Chilton Co., 239 W. 39th St., New York, N. Y.

Olivo, W. L. (D), Welder, Olson Drilling Co.; (Res.) 2118 N. Prospect, Oklahoma City, Okla.

Olson, Herbert (D), Welder, Youngstown Sheet & Tube Co.; (Res.) 7842 Stewart Ave., Chicago, Ill.

O'Neil, Edgar (B), Bldg. Design, Eastman Kodak Co.; (Res.) 79 Argyle St., Rochester, N. Y.

O'Neil, John J. (D), Arc Welder, Foster Wheeler Corp.; (Res.) 44 Main St., Dansville, N. Y.

O'Neill, John (D), Welding Operator, R. G. Le Tourneau, Inc.; (Res.) 1125 N. Madison, Peoria, Ill.

O'Neill, J. J. (D), Electric Welder, U. S. Government; (Res.) Box 2288, Cristobal, C. Z.

O'Quinn, John D. (D), Foreman of Welders & Boiler Makers, Pan-American Ref. Co.; (Res.) P. O. Box 805, Texas City, Texas.

O'Reilly, Eugene J. (C), Welder, Eastern Steel Tank Co.; (Res.) 2471 Grand Ave., New York, N. Y.

Organ, C. J. (D), Welding Operator, U. S. N., Box No. 42, South Denver Sta., Denver, Colo.

O'Rorke, Joseph (C), Welder, Eastman Kodak Co.; (Res.) 17 Belford Drive, Rochester, N. Y.

Orr, Nelson J. (B), Sales Engineer, Harnischfeger Corp.; (Res.) 39 Highview Ave., Park Ridge, N. J.

Orso, M. P. (C), Boiler Maker Foreman & Welder, Oahu Railway & Land Co., Honolulu, T. H.

Ortman, Edw. F. (C), Secretary & General Manager, American Elec. Welding Co., Inc.; (Res.) 3225 Abell Ave., Baltimore, Md.

Osborne, M. J. (D), Head Welder, Breese Bros.; (Res.) 206 Oak St., Cincinnati, Ohio.

Osborne, T. W. (D), Welder, Integrity Supply Co.; (Res.) 61-25 Woodside Ave., Woodside, L. I.

Osborne, William S. (C), Welding & Sheet Metal Instructor, Timken Vocational High School,; (Res.) 715 Ninth St. N. E., Massillon, Ohio.

Osburn, E. R. (B), General Supt., Texas-New Mexico Pipe Line Co., Box 1860, Midland, Texas.

Osmin, Basil (B), Welding Supervisor, United Engineers & Constructors, P. O. Box 269, Burlington, N. J.

Osterhous, C. S. (C), Salesman, Sutton Garten Co.; (Res.) 5313 Carrollton Ave., Indianapolis, Ind.

Ostrom, Kurre W. (C), Asst. Supt., Arcos Corporation; (Res.) 4414 N. 8th St., Philadelphia, Pa.

Ostrander, F. T. (C), Vice-President, National Supply Corp., 30 Rockefeller Plaza, New York City.

Ovaska, Walter A. (C), Applied Eng., Air Reduction Sales Co.; (Res.) Cushing St., Hingham, Mass.

Overton, Lynn S. (D), 4979 N. 36th St., Milwaukee, Wis.

Overstrud, Melvin (B), Vice-President & Supt., Pioneer Engrg. Works Inc., 1515 Central Ave. N. E., Minneapolis, Minn.

Owen, Earl V. (D), Welder, Delco Appliance Corp.; (Res.) 458 Birr St., Rochester, N. Y.

Owen, F. C. (B), Manager, Owen Electric Mfg. Co., Fayetteville, N. C.

Owen, N. W. (D), 1817 Campbell Ave., Schenectady, N. Y.

Owens, Clarence R. (C), Welding Specialist, General Electric Co., 807 Russ Bldg., San Francisco, Calif.

Owens, J. W. (B), Dir. of Welding, Fairbanks Morse & Co., Beloit, Wis.

Owings, Steve (B), Harnischfeger Corp., Milwaukee, Wis.

P

Padovick, Matt (B), Welder, Colonial Iron Works; (Res.) 19307 Arrowhead Ave., Cleveland, Ohio.

Page, F. A. (B), Supervising Engineer, Boiler Section, Industrial Accident Commission, State Bldg., Room 157, San Francisco, Calif.

Pahmeyer, Fred O. (C), Works Manager, Combustion Engrg. Co., Heine Boiler Div., 5319 Shreve Ave., St. Louis, Mo.

Paige, J. W. (B), Supervising Constructor, Electric Boat Co., Groton, Conn.

Palmer, H. (B), Asst. Master Mechanic, Consolidated Mining & Smelting Co. of Canada, Ltd., Trail, B. C.

Palmer, M. A. (C), Plant Supt., The Air Preheater Corp., Wellsville, N. Y.

Palmer, Robert (C), Mech. & Elec. Supervisor, Res. Lab., General Electric Co.; (Res.) 55 Balltown Rd., Schenectady, N. Y.

Palmer, Walter (B), Manager, Eastern Div., Hollup Corp., 30 Church St., New York, N. Y.

Pankratz, Peter G. (D), Welding Operator, R. G. Le Tourneau Inc., 107 Le Tourneau Court, Peoria, Ill.

Paolino, A. G. (C), Welding Foreman, Cincinnati Milling Machine Co.; (Res.) 3528 Madison Park Ave., Oakley, Cincinnati, Ohio.

Paolino, Laurence (D), Layout & Assembler, Cincinnati Milling Machine Co.; (Res.) 3440 Cardiff Ave., Cincinnati, Ohio.

Paparella, Michael J. (D), Electric & Arc Welding, American Locomotive Co.; (Res.) 555 Florence St., Schenectady, N. Y.

Paque, E. J. (B), Chief Engineer, The Pollak Steel Co., 820 Temple Bar Bldg., Cincinnati, Ohio.

Parker, J. H. (B), Sales Engineer, National Cylinder Gas Co., Columbus, Ohio.

Parr, Maurice A. (D), Welding Operator, 5914 Monroe St., Burtonville, Ill.

Parrish, Robert E. (D), Welder, Mid-Continent Pet. Corp.; (Res.) 1623 W. 19th, Tulsa, Okla.

Parry, S. R. (C), Supt., Combustion Engrg. Co., 1032 W. Main St., Chattanooga, Tenn.

Parsons, G. S. (B), District Manager, The Lincoln Electric Co., 812 Mateo St., Los Angeles, Calif.

Parsons, J. S. (C), Welding Engineer, Stanley Works, New Britain, Conn.

Paschke, George H. (D), Welder, Fuel Economy Engrg. Co., 510 New York Bldg., St. Paul, Minn.

Paton, W. G. (B), The Austin Co., 16112 Euclid Ave., Cleveland, Ohio.

Patterson, Clarence Thos. (C), Metallurgical Engineer, The Solvay Process Co., 207 Parsons Drive, Syracuse, N. Y.

Patterson, Donald (D), Welder, Buehler Tank & Welding Works; (Res.) 2457 Cass Pl., Huntington Park, Calif.

Patterson, Don W. (B), Dist. Manager, Federal Machine & Welder Co., 2010 Rand Building, Buffalo, N. Y.

Patterson, Guy (C), Shop Supt., Midwest Piping & Supply Co., 520 So. Anderson St., Los Angeles, Calif.

Patterson, Harold A. (D), Welder Foreman, A. T. & S. F. Ry. Shops; (Res.) 638—17th St., Richmond, Calif.

Patterson, John H. (C), Managing Director, Murex Welding Processes Ltd., Murex Works, Hertford Road, Waltham Cross, Herts, England.

Patterson, John K. (D), Electric Welder, American Bridge Co., 132 Horseheads Blvd., Elmira Heights, N. Y.

Patterson, N. R. (B), President, Patterson Steel Co., P. O. Box 2620, Tulsa, Okla.

Patterson, Parker W. (B), Vice-President, Patterson Steel Co., P. O. Box 2620, Tulsa, Okla.

Patterson, Robert, Jr. (B), Engineer of Tests, Allegheny Steel Co., Brackenridge, Pa.

Patterson, W. K. (C), Estimator, Engrg. Dept., Crane Co., 321 E. 3rd St., Los Angeles, Calif.

Patton, E. R. (B), Representative, Int. Nickel Co., Inc., 915 Shoreham Bldg., Washington, D. C.

Paul, Joseph (D), Welder, Commonwealth Edison Co.; (Res.) 4139 West End Ave., Chicago, Ill.

Paulsen, E. E. (C), Welding Engineer, General Electric Co.; (Res.) R. D. Route 58, Box 166, Schenectady, N. Y.

Paulson, Frank O. (C), Asst. Mech. Engineer, Atlantic Gulf and Pacific Co.; (Res.) 89 Glenwood Road, Ridgewood, N. J.

Pawlowski, John A. (C), Welding Engineer, A. O. Smith Corp.; (Res.) 5282 North Shoreland, Milwaukee, Wis.

Payne, Burt H. (B), Manager, Stulz-Sickles Co., 134 Lafayette St., Newark, N. J.

Payne, Spencer (D), Student Engineer, Lincoln Electric Co.; (Res.) 18203 Cornwall Road, Cleveland, Ohio.

Peabody, Harold P. (B), Salesman, John A. Roebling's Sons Co., 51 Sleeper St., Boston, Mass.

Pearson, Raymond C. (C), Electric Welder, New York Navy Yard; 128 So. 1st Ave., Mount Vernon, N. Y.

Pearson, Roy W. (C), Engineer, Taylor-Winfield Corp., Warren, Ohio.

Pearson, Wm. C. (B), Welding Engineer, Westinghouse Electric & Mfg. Co., 20 N. Wacker Drive, Chicago, Ill.

Peattie, Wm. W. (B), President, Northern Engrg. Works, Detroit, Mich.

Peck, W. H. (F), 507 West St., Stillwater, Okla.

Pederson, E. B. (D), Supervisor, Applied Engrg. Dept., Air Reduction Sales Co., 1210 W. 69th St., Cleveland, Ohio.

Pegram, W. A. (C), Engineer, Midwest Piping & Supply Co.; (Res.) 520 S. Anderson St., Los Angeles, Calif.

Peiffer, Fred L. (B), Supt., F. L. Heughes and Co., Inc., 1029 Lyell Ave., Rochester, N. Y.

Pelky, Al (C), Link Belt Co., 2410 W. 18th St., Chicago, Ill.

Pelton, Ralph S. (C), Welding Engineer, Works Lab., General Electric Co., Schenectady, N. Y.

Pender, Warren (D), 4647 N. Clark St., Chicago, Ill.

Pendlebury, A. H. (C), Engineer, M. W. Kellogg Co., Ft. of Danforth Ave., Jersey City, N. J.

Penn, Henry (C), District Engineer, Amer. Inst. of Steel Const. Inc., 53 W. Jackson, Chicago, Ill.

Pennewill, G. W. (C), District Manager, National Cylinder Gas Co., 1520 S. Vandeventer Ave., St. Louis, Mo.

Pennington, H. A. (C), Welder, A. B. King & Co., 196 Chapel St., New Haven, Conn.

Perkins, Howard (C), Sales Representative, Big Three Welding & Equip. Co., Port Arthur, Texas.

Perona, Louis A. (C), Welding Foreman, Farrel Mfg. Co.; (Res.) 823 Kelly Ave., Joliet, Ill.

Perreault, John B. (D), Welder, 316 E. 102nd St., New York City.

Perry, Bernard H. (B), Welder, New Britain Welding Co., New Britain, Conn.

Perry, Charles F. (C), Marine Engineer, Bureau Marine Inspection & Nav., 1069 A Commerce Bldg., Washington, D. C.

Persons, O. H. (C), General Manager of Sales, Edgcomb Steel Co., D. & Erie Aves., Philadelphia, Pa.

Persons, W. R. (C), District Manager, Lincoln Electric Co., 926 Manchester Blvd., Pittsburgh, Pa.

Pestana, Edward (D), Welder, Oahu Sugar Co., P. O. Box 327, Waipahu, Oahu, T. H.

Peters, Lewis J. (D), Electric & Acetylene Pipe Welder, Johnson Larsen Co.; (Res.) 1569 Harding, Detroit, Mich.

Peters, V. (D), 11304 Florian Ave., Cleveland, Ohio.

Peterson, Alvin V. (B), Welding Supervisor, Electro-Motive Corp.; (Res.) 4111 Raymond Ave., Congress Park, Ill.

Peterson, Carl (C), Salesman, Puritan Compressed Gas Corp., 2012 Grand Ave., Kansas City, Mo.

Peterson, M. E. (B), Works Manager, Clearing Machine Corp.; (Res.) 8153 Langley Ave., Chicago, Ill.

Petrie, Geo. W. (C), Engineer, Pittsburgh Piping & Equip. Co., 43rd St. & A. V. R. R., Pittsburgh, Pa.

Petroskey, E. C. (B), Sales Engineer, Federal-American Cement & Tile Co.; (Res.) 723—15th St., N. W., Washington, D. C.

Petrusky, Jos. W. (D), 110 W. 11th St., Linden, N. J.

Petry, Walter W. (B), Supt. Maintenance, The Cincinnati Milling Machine Co.; (Res.) 25 Donald Ave., Pleasant Ridge, Cincinnati, Ohio.

Petsche, R. A. (C), Chief Machinist, U. S. Navy, U. S. S. Medusa, % Postmaster, San Pedro, Calif.

Pew, Arthur E., Jr. (B), Manager, Engrg. & Development Dept., Sun Oil Co., 1608 Walnut St., Philadelphia, Pa.

Pfeiffer, C. L. (B), Electric Engineer, Western Electric Co.; (Res.) 316½ N. Menard Ave., Chicago, Ill.

Pfeil, A. Leslie (B), President, Universal Power Corp., 4300 Euclid Ave., Cleveland, Ohio.

Pflasterer, C. R. (C), Asst. Engr. Metallurgist, Omaha Shops, U. P. R. R. Co., Omaha, Nebr.

Pflug, D. R. (B), Engineer, United Gas Public Service Co., P. O. Box 2492, Houston, Texas.

Phaff, James (D), Electric Welder, Eastman Kodak Co.; (Res.) 937 Genesee St., Rochester, N. Y.

Phelps, Arthur H. (C), Sales Engineer, Westinghouse Electric & Mfg. Co., 1 Montgomery St., San Francisco, Calif.

Phelps, C. E. (D), Welder, Mid-Continent Petroleum Corp.; (Res.) R. 1, Box 197, Tulsa, Okla.

Phelps, G. E. (C), Asst. Applied Engrg. Dept., Air Reduction Sales Co., 7991 Hartwick St., Detroit, Mich.

Phelps, W. D. (D), Electric Welder, % A. N. Roberts, Route 8, Afton, Okla.

Phillips, C. E. (C), President, C. E. Phillips & Co., 2750 Poplar St., Grand River Station, Detroit, Mich.

Phillips, Chas. J. (D), 645 E. 25th St., Baltimore, Md.

Phillips, Donald (D), Welder, York Ice Machine Co., % A. T. Light, York, Pa.

Phillips, Horace P. (C), Supt., Link Belt Co. Pacific Division, 400 Paul Ave., San Francisco, Calif.

Phillips, J. T. (B), Supt., Boiler Shop, Foster Wheeler Corp.; (Res.) 615 Pemberton Ave., Plainfield, N. J.

Phillips, R. L. (B), Bridge Sales Engineer, J. B. Klein Iron & Fdy. Co., Oklahoma City, Okla.

Phillips, Seward (F), 1324 East 18th St., Oakland, Calif.

Piasecki, Stanley (D), Welder, Del. Balso Constr. Co.; (Res.) 80½ Attorney St., New York City.

Pidgeon, Frank (C), Vice-President, Pidgeon-Thomas Iron Co., 107 East Iowa, Memphis, Tenn.

Pieper, James F. (D), 148—6th Ave., Brooklyn, N. Y.

Pierce, Harry W. (C), Supervisor of Welding, New York Shipbldg. Corp., Camden, N. J.

Pietsch, W. H. (C), Chief Draftsman, Marcus Hook Refinery, Sun Oil Co., Marcus Hook, Pa.

Pifer, Wm. (D), Combination Welder, Eastman Kodak Co.; (Res.) 52 Dalkeith Road, Rochester, N. Y.

Pillsbury, Chas. S. (C), Manager of Operations, Chicago Bridge & Iron Co., 1305 W. 105th St., Chicago, Ill.

Piltch, A. (C), Jr. Welding Engineer, U. S. Naval Gun Factory; (Res.) 1445 Otis Place, N. W., Washington, D. C.

Pinckney, C. C. (C), President, Birmingham Boiler & Engineering Co., P. O. Box 2228, Birmingham, Ala.

Pinczon, J. (B), Chantier De Penhoet, Saint-Nazaire, Loire-Inferieure, France.

Pine, Clyde (B), Instructor in Welding, Delgrado Trade School; (Res.) 615 City Park Ave., New Orleans, La.

Pingree, C. C. (B), Asst. Manager, Whitmore Oxygen Co., 430 E. So. Temple St., Salt Lake City, Utah.

Pino, George B. (C), Associate Naval Architect, Brooklyn Navy Yard; (Res.) 820 New York Ave., Brooklyn, N. Y.

Pippel, Donald C. (B), Vice-President, Allied Weld-Craft Inc.; (Res.) 1318 Gladstone Ave., Indianapolis, Ind.

Pitou, Eugene (B), Executive, The Patent Scaffolding Co. Inc., 3821—12th St., Long Island City, N. Y.

Pittman, E. W. (C), Chief Engineer, The Petroleum Iron Works Co., Beaumont, Texas.

Place, George (B), Surveyor, American Bureau of Shipping; (Res.) 263 West Passaic Ave., Rutherford, N. J.

Place, J. W. (B), President, U. S. Gauge Co., 44 Beaver St., New York City.

Place, Lee (C), Service Supt., Air Reduction Sales Co., 2949 N. W. Front Ave., Portland, Oregon.

Plaisted, Walter A. (D), Welding Instructor, New England Welding Lab., 88 St. Stephen St., Boston, Mass.

Plane, John C. (C), Proprietor, Hobart Arc Welding Equipment Co., 2118 No. Broadway, St. Louis, Mo.

Planeta, A. W. (C), Engineer, Artistic Wire Prods. Co., 1233 St. George Ave., Linden, N. J.

Plaus, R. A. (B), Engineer, W. A. Ramsay, Ltd., Honolulu, T. H.

Plinke, G. W. (B), Research Director, Henry Vogt Machine Co., 10th & Ormsby Ave., Louisville, Ky.

Plowright, R. J. Owen (B), Director & Works Manager, Messrs. Plowright Bros. Ltd., Shepley St., Chesterfield, Derbys, England.

Plum, Ira (D), Welding Operator, Frick Co.; (Res.) 46 W. Dalhgren St., Greencastle, Pa.

Plumley, Stuart (B), Editor, Welders' Digest; (Res.) 302 Walnut St., Winnetka, Ill.

Plummer, Fred L. (B), Chief Design Engineer, 914 Standard Bldg., Cuyahoga County, Cleveland, Ohio.

Plummer, Richard H. (D), Arc Welder, Contract Welders, Inc.; (Res.) 5708 Quimby Ave., Cleveland, Ohio.

Poehlman, Wm. J. (C), Research Engineer, A. O. Smith Corp.; (Res.) 2653 N. 18th St., Milwaukee, Wis.

Pohorenec, John (C), Welder, Metal Equipment Co.; (Res.) 12019 Wade Park, Cleveland, Ohio.

Pollard, T. J. (C), Industrial Engineer, Procter & Gamble, Kansas City, Kansas.

Pollei, Harold O. (C), Supervisor, A. O. Smith Corp.; (Res.) 3846 N. 24th Place, Milwaukee, Wis.

Pool, R. H. (B), Electric Supt., Truscon Steel Co., Youngstown, Ohio.

Poole, Laurence B. (C), Director, F. A. Poole, Ltd., 560 Carl St., Pretoria, South Africa.

Poole, William L. (C), Air Reduction Sales Co., 2825 No. 29 Ave., Birmingham, Ala.

Porter, C. E. (B), Sales, Smith Welding Equipment Corp.; (Res.) 1108 Omar Ave., Houston, Texas.

Porter, F. N. (D), Welder, Serviceman, Applied Engrg. Dept., Air Reduction Sales Co.; (Res.) R. D. 1, Box 317, Warren, Ohio.

Porter, J. Gordon (B), Sales Engineer, % Lincoln Electric Co. of Canada, Ltd., 65 Bellwoods Ave., Toronto, Canada.

Porter, J. S. (C), Sales Engineer, % Westinghouse Electric & Mfg. Co., 533 First National Bldg., Oklahoma City, Okla.

Posey, W. W. (C), President, Lancaster Iron Works, Lancaster, Pa.

Post, W. A. (B), Div. Manager, The Linde Air Products Co., Birmingham, Ala.

Poston, Claude P. (D), Welder, Electro-Motive Corp.; (Res.) 6203 So. Whipple St., Chicago, Ill.

Potter, Everett F. (C), Welding Specialist, General Electric Co.; (Res.) 30 Hazelwood Terrace, Pittsfield, Mass.

Potter, Morgan H. (B), 5304—15th Ave. So., Minneapolis, Minn.

Poundens, J. C. (D), Electrical Welder, U. S. Engineers, P. O. Box 97, Memphis, Tenn.

Powell, Marselis (A), Supt., Whitlock Coil Pipe Co., P. O. Drawer 390, Hartford, Conn.

Powell, R. E. (C), Welding Engineer, Western Electric Co., Inc., 100 Central Ave., Kearny, N. J.

Powell, William J. (D), Operator, Commonwealth Edison Co.; (Res.) 2501 N. Mason Ave., Chicago, Ill.

Powers, Ed C. (C) Asst. Secretary, The James F. Lincoln Arc Welding Foundation, 2700 Terminal Tower; (Res.) 20562 Stratford Ave., Cleveland, Ohio.

Powers, H. C. (C), Supervisor of Welding, American Air Filter Co., 215 Central Ave., Louisville, Ky.

Powers, James C., Jr. (C), Sales & Engineering, Handy & Harman, 425 Richmond St., Providence, R. I.

Powers, R. F. (B), General Supt., Kewanee Boiler Corp., Kewanee, Ill.

Pratt, Howard N. (C), Asst. Manager, Columbia Steel Co., Russ Bldg., San Francisco, Calif.

Prescott, Leonard P. (D), Electric Welder, Eastman Kodak Co.; (Res.) 8 Emanon St., Rochester, N. Y.

Price, Harold C. (B), President, H. C. Price Co., Box 149, Bartlesville, Okla.

Price, H. Dennis (D), Welder, Mid-Continent Pet. Corp.; (Res.) 1211 South Jackson St., Tulsa, Okla.

Price, Herman G. (C), Birmingham Tank Co., Birmingham, Ala.

Price, H. S. (B), Liverpool Refrigeration & Eng. Co., Polar Works, Sankey, Warrington, England.

Price, Percy (D), 224 E. Marietta, Peoria Heights, Ill.

Price, Paul L. (C), Asst. to Executive Vice-President, American Institute of Steel Construction, Inc., 101 Park Ave., New York City.

Priest, B. B. (C), Assistant Engineer, Amer. Bridge Co., 71 Broadway, New York City.

Priest, Chas. H., Jr. (C), Vice-President, Los Angeles Heavy Hdwe. Co., 218 No. Los Angeles St., Los Angeles, Calif.

Priest, H. M. (B), Engineer, Railroad Research Bureau, U. S. Steel Corp. Subsidiaries, 612 Frick Bldg., Pittsburgh, Pa.

Pryor, J. F. (B), Vice-President, Magnolia Airco Prods. Co., P. O. Box 319, Houston, Texas.

Pufahl, Hans R. (C), Engineer, The Union Carbide Co.; (Res.) 409 So. Humphrey Ave., Oak Park, Ill.

Puffer, Harold P. (C), Squad Leader Bridge Div. Michigan State Highway Dept.; (Res.) 1419 W. Ottawa St., Lansing, Mich.

Pugh, Guy L. (B), 607—22d Ave. N. E., Minneapolis, Minn.

Pullen, K. G. (C), Mechanical Engineer, Broken Hill Prop. Co., Ltd., Iron & Steel Works, Newcastle, Australia.

Purcell, John (C), Inspector, Standard Oil Co. of Calif.; (Res.) 2828 Grand Ave., Huntington Park, Calif.

Purdy, C. V. (C), Owner, Wyoma Welding Co., 65 Boston St., Lynn, Mass.

Purdy, John L. (C), Inspector, Buckeye Steel Castings Co.; (Res.) 1333 West First Ave., Columbus, Ohio.

Pursell, Robert (C), Welding Supervisor, Worthington Pump & Machinery Corp.; (Res.) 11 Park Place, Bloomfield, N. J.

Purslow, Herbert (B), Director & Engineer, St. George's Engineers Ltd.; (Res.) Langside, Priory Road, Sale, NR. Manchester, England.

Pynn, J. Garland (D), Operator, Madison Co. Dept. of Highways, Wampsville, N. Y.

Q

Quartz, Herbert O. (C), Welding Engineer, Allis Chalmers Mfg. Co.; (Res.) 1518 So. 81st St., West Allis, Wis.

Quasdorf, H. C. (C), Salesman, Carnegie-Ill. Steel Corp., 71 Broadway, New York City.

Quigley, James B. (B), Welding Instructor, Graver Tank & Mfg. Co.; (Res.) 3428 Fir St., Indiana Harbor, Ind.

Quinn, E. L. (B), Manager of Welding Division, American Manganese Steel Co., 389 E. 14th St., Chicago Heights, Ill.

Quinn, John I. (C), U. S. Bureau of Public Roads, Washington, D. C.

R

Rabbitt, James A. (B), Director of Bureau, Japan Nickel Information Bureau, Shisei Kaikan, Hibiya Park, Tokyo, Japan.

Rabenau, Frank (B), Asst. Supt. Wm. Bros. Boiler & Mfg. Co.; (Res.) 1206 Jefferson St. N. E., Minneapolis, Minn.

Radcliffe, Thos. D. (C), Met. Engineer, Standard Oil Co. of Calif.; (Res.) 1426 Chestnut St., Alameda, Calif.

Raftery, T. E. (C), Sales Representative, The Linde Air Products Co., 709 Melish Ave., Cincinnati, Ohio.

Rahtz, Arthur J. (C), Plant & Prod. Manager, Vulcan Rail & Const. Co., 51-12 Grand Ave., Maspeth, Queens, New York City.

Raitt, George H. (B), Vice-President and General Manager, The Steel Tank & Pipe Co. of Calif., 1100 Fourth St., Berkeley, Calif.

Ralston, R. W. (D), Erection Supt., Wyatt Metal & Boiler Works, Box 3052, Houston, Texas.

Ramer, E. J. (C), Designing Engineer, Dept. Public Works, State of New York, State Office Building, Albany, N. Y.; (Res.) 41 Barrows St., Albany, N. Y.

Randall, John F. (B), Welding Engineer, Combustion Engineering Co., Inc., 200 Madison Ave., New York, N. Y.

Raney, James E. (C), Sales Engineer, Lincoln Electric Co. Dist. Manager, 150 Causeway St., Boston, Mass.

Rash, William C. (D), Welder, General Electric Co.; (Res.) 2366 Harrison St., Schenectady, New York.

Rasmussen, Alvin C. (B), Vice-President & Chief Engineer, Insley Mfg. Corp., 801 N. Olney St., Indianapolis, Ind.

Rawls, Otis B. (C), Welder & Owner, Rawls Garage & Welding Co., Madison St., Dublin, Ga.

Ray, M. H. (D), Welder, Goslin-Birmingham, Mfg. Co.; (Res.) 974-A 50th St. No., Birmingham, Ala.

Ray, W. C. (C), Shop Supt., Wyatt Metal & Boiler Works, Dallas, Texas.

Raymo, Arthur J. (C), Welding Engineer, U. S. Navy Yard, Philadelphia, Pa.; (Res.) 25 Fairlamb Ave., Manoa, Upper Darby, Pa.

Raymo, C. T. (C), Welding Engineer, Chattanooga Boiler & Tank Co., 1011 East Main St., Chattanooga, Tenn.

Raymond, G. (B), Chief Engineer, Black, Sivalls & Bryson Inc.; (Res.) 3110 N. W. 20th St., Oklahoma City, Okla.

Raymond, H. K. (D), Acetylene Welder Instructor, New England Welding Lab., 88 St. Stephen St., Boston, Mass.

Reals, Chas. L. (C), Asst. Supt., Black, Sivalls & Bryson; (Res.) 717 Freemont, Kansas City, Mo.

Rebman, Chas. G. (D), Mechanical Supt., Mid-Continent Petr. Corp., Box 381, Tulsa, Okla.

Rechtin, Eberhardt (B), Manager New Construction, Bethlehem Steel Co., Shipbldg. Div., Fore River Plant, Quincy, Mass.

Reddie, W. W. (B), Section Head, Welding Section, Westinghouse Electric & Mfg. Co., E. Pittsburgh, Pa.

Redline, Ralph H. (B), Welding Supervisor, American Locomotive Co.; (Res.) 755 Park Ave., Dunkirk, N. Y.

Redman, R. P. (B), Vice-President, Aetna Iron & Steel Co., Jacksonville, Fla.

Redmond, James H. (D), Resistance Welder, General Electric Co.; (Res.) R. F. D. 5, Schenectady, N. Y.

Reed, Clarence (D), Welder and Repairman, E. P. Waggoner; (Res.) Box 865, Vernon, Texas.

Reed, Clifford H. (C), Welding Methods, General Electric Co.; (Res.) 84 James St., Schenectady, N. Y.

Reed, H. M. (D), Welder, Mid-Continent Pet. Corp.; (Res.) 116 North Union St. Tulsa, Okla.

Reed, John C. (C), Elect. Engineer, Bethlehem Steel Co., Steelton, Pa.

Reed, Malcolm V. (B), Engineer, Wyatt Metal & Boiler Works, Box 3052, Houston, Texas.

Reed, Walter C. (C), Brazing Engineer, General Electric Co., Pittsfield, Mass.

Reese, C. E. (C), Salesman, National Cylinder Gas Co.—Pacific Coast, 4950 Santa Fe Ave., Los Angeles, Calif.

Reese, D. F. (A), Vice-President, Hartford Steam Boiler Insp. & Ins. Co., 56 Prospect St., Hartford, Conn.

Reeves, Charles L. (B), Foreman, Minneapolis-Moline Power Implement Co., Moline, Ill.

Reeves, Eugene L. (C), Vice-President, Reeves & Skinner Machinery Co., 2211 Olive St., St. Louis, Mo.

Reeves, Dr. Lewis (C), Research Metallurgist, Appleby-Frodingham Steel Co., Ltd., Scunthrope, Lincolnshire, England.

Reichert, Harold W. (C), Welding Insp., Lakeside Bridge & Steel Co.; (Res.) 2629 W. Cherry St., Milwaukee, Wis.

Reid, Chas. W. (C), Supt., Patterson-Kelley Co., Inc., E. Stroudsburg, Pa.

Reid, Sibbald (C), Salesman, Chas. Rubel & Co., 1101 Vermont Ave. N. W., Washington, D. C.

Reid, Vaughan (C), President, City Pattern Works, 1161 Harper Ave., Detroit, Mich.

Reiff, Stanley G. (C), Sect. & Design Engineer, General Construction Co., 934 Redick Tower, Omaha, Nebr.

Reilly, L. D. (B), Manager, American Bridge Co., Ambridge, Pa.

Reinhard, Herbert F. (B), Secretary, International Acetylene Assoc., 30 E. 42nd St., New York, N. Y.

Reinhardt, G. A. (A), Dir. of Met. & Res., Youngstown Sheet & Tube Co., Campbell Works, Youngstown, Ohio.

Reinhart, H. D. (D), Box 312, Fredonia, Pa.

Reisinger, Paul R. (D), Electric Welder, York Safe & Lock Co.; (Res.) Craley, Pa.

Reist, J. F. (B), Welding Foreman, Keystone S. & W. Co., Welding Dept., Peoria, Ill.

Reno, Robert W. (C), Sales Manager, Soulé Steel Co., 6200 Wilmington Ave., Los Angeles, Calif.

Renton, Allan (C), President and General Supt., Hawaiian Gas Products, Ltd., P. O. Box 2454, Honolulu, Terr. of Hawaii.

Reschke, F. Paul (D), Reschke Mach. Works, 908 N. Washington, Wichita, Kansas.

Reubene, Samuel (B), Designing Engineer, Independent Structural Steel Co., Vandergrift Bldg., Pittsburgh, Pa.

Revell, J. J. (C), Welder, W. M. Whitmire Inc.; (Res.) 553 Government St., Mobile, Ala.

Revis, L. H. (C), District Manager, Compressed Industrial Gases, 139 Simpson St., Atlanta, Ga.

Reynolds, Joseph T. (D), Welder, Chevrolet Motor Co.; (Res.) 89 Riverview Ave., Tarrytown, N. Y.

Reynolds, Nolan (D), Welding Foreman, Ball Bros. Co., Muncie, Ind.

Reynolds, Ralph C. (D), Welder, J. B. Klein Iron & Fdry. Co., 1401 N. W. 3rd St., Oklahoma City, Okla.

Rhine, George E. (C), Welding Engineer, Edgcomb Steel Co., D. & Erie Aves., Philadelphia, Pa.

Riback, Emil S. (D), Welding Foreman, McCord Mfg. Co., 2587 E. Grand Blvd., Detroit, Mich.

Rice, S. B. (C), Welder or Operator, Swift & Co.; (Res.) 2400 N. 35th St., Kansas City, Kansas.

Rice, William Harold (B), Asst. Professor in Welding, Oklahoma A. & M. College, Stillwater, Okla.

Richards, Edwin J. (C), Electric Welder, Sun Shipbldg. & Dry Dock Co.; (Res.) 96 Spencer St., Philadelphia, Pa.

Richards, J. S. (B), Dir. of Mfg. Practices, American Steel & Wire Co., Rockefeller Bldg., Cleveland, Ohio.

Richards, Nathaniel A. (B), President, Purdy & Henderson Co., 45 E. 17th St., New York City.

Richardson, A. C. (C), Supt., Combustion Eng. Co., 1032 W. Main St., Chattanooga, Tenn.

Richardson, David (C), Lecture Engineer, 26 Winterbrook Road, Herne Hill, London, S. E. 24, England.

Richardson, George (D), Welding Inspector, General Electric Co.; (Res.) 36 Verdmont Ave., Lynn, Mass.

Richeda, Fred (B), Supt., The Lang Company; (Res.) Box 479, Salt Lake City, Utah.

Richter, G. (D), Welder, Wm. J. Myers, Co.; (Res.) 68 Walzer Road, Rochester, N. Y.

Richter, J. Louis (C), President & General Manager, Central Ohio Welding Co., Spring & Neilston Sts., Columbus, Ohio.

Rickel, F. J. (B), Salesman, Big Three Welding Equip. Co., Oklahoma City, Okla.

Ricketts, F. L. (D), Welding Operator, Southern Iron & Equip. Co.; (Res.) 1 Decatur Rd., Brookhaven, Ga.

Rickly, O. D. (B), Teacher, Ohio State University; (Res.) 2710 Joyce Ave., Columbus, Ohio.

Ridgway, Herbert (C), Asst. Engineer, American Bridge Co., 71 Broadway, New York, N. Y.

Riebeth, C. E. (B), General Manager, Commercial Gas Co., 2619—4th St. S. E., Minneapolis, Minn.

Riebeth, Theodore J. (C), Assistant Prof., Marquette Univ., School of Eng.; (Res.) 3613 N. Maryland Ave., Milwaukee, Wis.

Rieger, Arthur J. (C), Salesman, Hill Equip. Engrg. Co.; (Res.) 804 Maple St., Ziegler, Ill.

Riffie, Jacob F. (D), Welder, Reliance Gauge Column Co.; (Res.) 3224 East 90th St., Cleveland, Ohio.

Rigby, Edw. J. (C), Manager, Robt. Bryce & Co. Pty. Ltd., Bryce Bldgs. 526–32, Little Bourke St., Melbourne, Australia.

Rigby, L. (C), Welding Foreman, Truscon Steel Co., Youngstown, Ohio.

Riggle, Charles R. (B), Power Sales Supervisor, Ohio Edison Co., 25 E. Boardman St., Youngstown, Ohio.

Rigler, Geo. W. (D), Arc Welder, International Harvester Co., East Moline Works; (Res.) Prophetstown, Ill.

Rinek, John O. (A), Vice-President, Universal-Cyclops Steel Corp., Bridgeville, Pa.

Ringer, Robt. Lee (C), Development Engineer, Taylor-Winfield Corp., Warren, Ohio.

Ringsmith, O. F. (C), Chief Engineer, Taylor-Winfield Corp.; (Res.) 712 Kenilworth Ave. S. E., Warren, Ohio.

Rippel, J. N. (C), Foreman, Federal Shipbuilding & Dry Dock Co.; (Res.) 18 Bidwell Ave., Jersey City, N. J.

Ritchie, Richard W. (B), Surveyor, American Bureau of Shipping, 1435 Boatmen's Bank Bldg., St. Louis, Mo.

Ritter, Julius C. (C), Senior Engineering Draftsman, U. S. Navy Dept., Bureau of Const. & Repair, Washington, D. C.

Roach, Harold (C), Engineer, Bethlehem Steel Co.; (Res.) 3311 Shelby Dr., W. Los Angeles, Calif.

Roach, Justin (C), Salesman, The Linde Air Products Co.; (Res.) 4329 Butler Place, Oklahoma City, Okla.

Robbins, Harris H. (B), Time Study Engineer, St. Paul Hydraulic Hoist Co.; (Res.) 2207 University Ave. S. E., Minneapolis, Minn.

Rober, Chester A. (B), Welding Foreman, Bethlehem Steel Co., 97 East Howard St., Quincy, Mass.

Roberts, Charles (C), Pittsburgh Representative, H. C. Nutting Co.; (Res.) 5700 Penn Ave., Pittsburgh, Pa.

Roberts, C. William (C), Factory Supt., Southwestern Eng. Co., 4800 Santa Fe Ave., Los Angeles, Calif.

Roberts, Clay W. (C), Welding Foreman, Pennsylvania Shops No. 20th; (Res.) 1430 Eastwood Ave., Columbus, Ohio.

Roberts, D. E. (B), Engineer, The Linde Air Products Co., 30 E. 42nd St., New York City.

Roberts, E. T. (C), Welder Foreman, Reed Roller Bit Co.; (Res.) 8030 Easton St., Houston, Texas.

Roberts, G. R. (C), Salesman, National Cylinder Gas Co., Ft. of E. Franklin St., Peoria, Ill.

Roberts, John H. (B), Asst. Chief Designer, Edw. Wood & Co., Ltd.; (Res.) 46 Lambton Road, Worsley, Manchester, England.

Roberts, N. J. (C), Power Engineer, The Ohio Public Service Co., Warren, Ohio.

Roberts, R. B. (B), Salesman, Southern Oxygen Co., Arlington, Va.

Roberts, William (D), Electric Arc Pipe Welder, General Electric; (Res.) 900 Western Ave., West Lynn, Mass.

Roberts, W. M. (C), Testing Engineer, Combustion Eng. Co., 1032 W. Main St., Chattanooga, Tenn.

Roberts, W. P. (C), Manager, Air Reduction Sales Co.; (Res.) 3121 Highland Ave., Birmingham, Ala.

Robertson, James B., Jr. (C), Assoc. Naval Arch., Bureau of Marine Insp. & Nav., Dept. of Commerce, Washington, D. C.

Robertson, R. E. (C), Research Assistant, International Nickel Co., Research Library, Oak St., Bayonne, N. J.

Robinoff, Boris (B), Sales Engineer, National Tube Co.; (Res.) 5441 Fair Oaks St., Squirrel Hill, Pittsburgh, Pa.

Robinson, James M. (C), Owner, Robinson Welding Supply Co., 1951 East Ferry St., Room 203, Detroit, Mich.

Robinson, Richard W. (B), Manager and Engineer, American Bridge Co., 629—2nd St. S. E., Minneapolis, Minn.

Robinson, William J. (C), Welding Foreman, Iraq Petroleum Co., P. O. Box 309, Haife, Palestine.

Robson, L. D. (C), 37 S. E. 32nd, Oklahoma City, Okla.

Rock, Esteban (C), General Manager, Cia Productora de Oxigeno S. A., Apartdo 318, Monterrey, N. L. Mexico.

Rockefeller, Harry E. (B), Manager, Process Dev. Dept., The Linde Air Products Co., 30 E. 42nd St., New York City.

Rodgers, F. L. (B), District Manager, The Linde Air Products Co., 1280 Main St., Buffalo, N. Y.

Rodgers, Wm. (B), General Supt., Nashville Bridge Co.; (Res.) P. O. Box 38, Bessemer, Ala.

Rogers, C. E. (C), Foreman Welder, Arthur Tickle Engineering Works, Inc.; 977—52nd St., Brooklyn, N. Y.

Rogers, Frank D. (B), 2 Harrison St., Binghamton, N. Y.

Rogers, Fred E. (C), Editor, Air Reduction Sales Co., 60 East 42nd St., New York City.

Rogers, H. L. (C), Manager, Applied Engrg. Dept., Air Reduction Sales Co., 60 E. 42nd St., New York City.

Rogers, H. L. (C), Welding Supervisor, Marion Steam Shovel Co., 617 W. Center St., Marion, Ohio.

Rogers, O. L. (B), Salesman, Big Three Welding & Equip. Co., 102 S. Cheyenne, Tulsa, Okla.

Rogina, Roland (D), Welder, R. G. Le Tourneau Inc.; (Res.) 605 McKinley Ave., Bartonville, Ill.

Rohrer, Paul V. (D), Arc Welding, A. B. Farquhar Co. Ltd.; (Res.) 641 W. Locust St., York, Pa.

Rokes, D. G. (D), Layout Welders Helper, Midwest Piping & Supply Co.; (Res.) 449 E. 118th Place, Los Angeles, Calif.

Romanik, Mike (D), Mold Welding, Armstrong Cork Co.; (Res.) 105½ Whitall Ave., Millville, N. J.

Romann, John H. (B), Consulting Engineer, Girdler Corp., 224 E. Broadway, Louisville, Ky.

Ronay, Bela M. (B), Senior Welding Engineer, U. S. Naval Engineering Experiment Station, Annapolis, Md.

Ronehausen, Arthur J. (D), Shop Foreman, E. O. Mitchell Welding, Arwin, Calif.

Rooney, J. I. (C), Assistant Engineer, Board of Water Supply; (Res.) 3205—81st St., Jackson Heights, L. I., N. Y.

Rooney, T. R. (C), General Supt., Western Pipe & Steel Co., 444 Market St., San Francisco, Calif.

Roper, Edward H. (D), Applied Engrg. Dept., Air Reduction Sales Co., 60 E. 42nd St., New York, N. Y.

Rork, Frank C. (C), Foreman, Structural Drafting, Los Angeles Bureau of Power & Light; (Res.) 7531 S. Hobart Blvd., Los Angeles, Calif.

Rosborough, J. G., Jr. (C), Asst. Chief Engineer, Anheuser Busch Inc., St. Louis, Mo.

Roscoe, John S. (C), District Manager, The Lincoln Electric Co., 517 Erie Blvd. E., Syracuse, N. Y.

Rose, Lambert V. (D), Welder, Southern Pacific Co.; (Res.) 2809 Que St., Sacramento, Calif.

Rosenblum, Abe (C), Partner, Detroit & Cincinnati Welding Co., 315 E. 2nd St., Cincinnati, Ohio.

Rosencrans, Julian (C), Sales, Hobart Bros. Co., 276 Lafayette St., New York, N. Y.

Rosendahl, R. C. (C), Welding Foreman, Bucyrus Erie Co.; (Res.) 1013 N. Chicago Ave., S. Milwaukee, Wis.

Rosevear, H. M. (C), Sales Engineer, Wyatt Metal & Boiler Works, Box 5418, Terminal Annex, Dallas, Texas.

Rosmann, C. F. (C), Secy., Moise Steel Co., 4027 W. Scott, Milwaukee, Wis.

Ross, A. R. (B), Assoc. President, Board of Public Service, City of St. Louis, St. Louis, Mo.

Ross, C. W. (D), Clerk to Field Supt., Wyatt Metal & Boiler Works, Box 3052, Houston, Texas.

Ross, D. D. (B), 1346 W. 29th St., Los Angeles, Calif.

Ross, D. H. (B), Treasurer, Michigan Power Shovel Co., Benton Harbor, Mich.

Ross, George Arthur (C), Works Lab., General Electric Co., Schenectady, N. Y.

Ross, G. M. (B), Manager, Hawaiian Welding & Machine Co.; (Res.) 2020 Kamehameha Ave., Honolulu, T. H.

Ross, Harry H. (C), General Foreman, Bethlehem Steel Company; (Res.) 1012 F. St., Sparrows Point, Md.

Ross, Jm. A. (C), Supt., Dominion Bridge Co., Ltd., 275—1st Ave. W., Vancouver, B. C.

Ross, Jack D. (D), Gas Welder, Lockheed Aircraft; (Res.) 1324 N. Lima St., Burbank, Calif.

Ross, James K. (B), Chief Engineer, The United Welding Co., Box 247, Middletown, Ohio.

Ross, Paul (B), Plate Shop Foreman, Goslin-Birmingham Co.; (Res.) Box 506, Birmingham, Ala.

Rossheim, David B. (B), Consultant, Pressure Equipment & Materials, M. W. Kellogg Co.; (Res.) 299 Van Buren Ave., Teaneck, N. J.

Rosskopf, Thos. (B), Managing Director, N. V. Williem Smit & Co., Annastraat, 187, Nijmegen, Holland.

Roth, Henry W. (B), President, Roth Mfg. Co.; (Res.) 212 Dana N. E., Warren, Ohio.

Roulund, Troy L. (D), Welder & Mechanic, Roulund Motor Co.; (Res.) 618 West St., Vacaville, Calif.

Rouscher, Elmer E. (C), Supt. of Boiler & Welding Dept., A. B. Farquhar Co.; (Res.) 419 Lindberg Ave., York, Pa.

Rousseau, E. J. (B), President, Commerce Pattern Fdy. & Mach. Co., 4211 Grand River Ave., Detroit, Mich.

Rowell, Clarence (D), Route L., Pekin, Ill.

Rowland, W. B. (B), Manager, Process Service, Eastern Div., The Linde Air Products Co., 30 E. 42nd St., New York City.

Rowley, Carl F. (D), Welder, Machinist & Mechanic, State Highway Shop; (Res.) 848 14th St., Douglas, Ariz.

Rowsey, Chas A. (D), Foreman, Whitehead Metal Product Company; (Res.) 57 Grove St., Belmont, Mass.

Roy, E. A. (D), Operator, Revere Copper & Brass Co., Box 86, Vernon, N. Y.

Royer, Charles (C), Manager, Welding & Supplies Co., 3445 Parthenais, Montreal, P. Q., Canada.

Royer, Jacques (B), Asst. Manager, Welding & Supplies Co., 3445 Parthenais St., Montreal, P. Q., Canada.

Royer, J. W. (B), Vice-President, Medart Co., 3500 De Kalb Ave., St. Louis, Mo.

Rozon, W. A. (D), Dept. Head, Motor Vehicle Tank Equipment, Geo. W. Reed & Co., Ltd., 4107 Richelieu St., Montreal, P. Q., Canada.

Ruel, Herbert N. (D), Welder, Bethlehem Steel Co., 34 Ivy Road, S. Weymouth, Mass.

Ruff, Joseph B. (B), Consulting Engineer, 729—8 Ter. W., Birmingham, Ala.

Rugg, Philip N. (C), Electric Heating Engineer, Boston Edison Co.; (Res.) 16 Fairmount Ave., Wakefield, Mass.

Rugg, Walter S. (B), 123 University Place, Pittsburgh, Pa.

Rule, J. O. (D), Welder, Chicago Bridge & Iron; (Res.) 346 Franklin St., Napa, Calif.

Rumble, George (B), President, Lincoln Electric Co. of Can. Ltd., 65 Bellwood Ave., Toronto, Canada.

Rumfelt, Henry F. C. (C), Asst. Plant Engineer, U. S. E. D.; (Res.) 701 Musselshell, Fort Peck, Mont.

Rundquist, Lester H. (D), 15203 Center Ave., Harvey, Ill.

Runkel, Alfred (C), Standards Dept., R. G. Le Tourneau, Inc., Ft. of Grant St., Peoria, Ill.

Runkel, R. W. (C), Welding Supervisor, R. G. Le Tourneau, Inc.; (Res.) 205 Springdale, Peoria, Ill.

Rusch, Charles E. (D), Elec. Acetylene Welder, Alloy Steel Tank Co.; (Res.) Box 192, Wharton, N. J.

Rusch, Richard (F), 22 Judd Ave., New Britain, Conn.

Rusk, Charles M. (C), Welding Engineer, Horace T. Potts Co.; (Res.) 5420 N. 11th St., Philadelphia, Pa.

Rusky, Nicholas (D), Acetylene Burner, Bethlehem Steel Co.; (Res.) Hancock Terrace, Wollaston, Mass.

Russell, Bruce A. (B), Chief of Hull Estimating and Designing Dept., Federal Shipbuilding and Dry Dock Co., 21 West St., New York, N. Y.

Russell, Kenneth (D), % Sailors Snug Harbor, Staten Island, N. Y.

Russell, Ralph S. (B), Chief Engineer, Hydraulic Supply Mfg. Co., 7500—8th So., Seattle, Wash.

Russell, Roy (D), Welder, Sun Shipbldg. & D. D. Co.; (Res.) 249 Clifton Ave., Sharon Hill, Pa.

Russell, Theo. J., Jr. (D), Combination Welder, International Harvester Co.; (Res.) 2833 N. Monitor Ave., Chicago, Ill.

Ruth, Mahlon C. (C), Foreman, York Ice Machinery Corp.; (Res.) 1631 W. Market St., York, Pa.

Rutishauser, M. H. (B), Asst. Sales Manager, Welder Div., Harnischfeger Corp., 4400 W. National Ave., Milwaukee, Wis.

Rutledge, Robert (C), Draftsman, A. Lucas & Sons, Peoria, Ill.

Rutt, Eugene (D), Electric Welder, Taylor Forge & Pipe Works; (Res.) 1931 S. Homan Ave., Chicago, Ill.

Ruzich, Joseph S. (C), 6152 S. Paulina, Chicago, Ill.

Ryan, C. M. (C), Sales Engineer, Hawaiian Gas Products Co., Box 2454, Honolulu, T. H.

Ryan, Henry B. (C), Shop Foreman, Hawaiian Dredging Co.; (Res.) 941—18th Ave., Honolulu, T. H.

Ryan, Robert J. (C), Vice-President & General Manager, John Nooter Boiler Works Co., 1400 So. Second St., St. Louis, Mo.

Ryder, E. M. T. (A), Ways Engineer, Third Ave. Railroad System, 130th St. & Third Ave., New York City.

S

Sacks, J. Raymond (D), Welding Instructor, Hadley Vocational School, 1710 N. 14th St., St. Louis, Mo.

Sadler, Robert L. (D), Welder, Scott Paper Co.; (Res.) 1607 Edgemont Ave., Chester, Pa.

Sage, V. L. (C), District Sales Manager, National Cylinder Gas Co., 2420 University Ave., St. Paul, Minn.

Salisbury, H. R. (C), Assistant Manager, Air Reduction Sales Co., 17th & Allegheny Sts., Philadelphia, Pa.

Salmon, Philip A. (C), Asst. Engineer, Electric Engrg. Dept., Public Service Electric & Gas Co., 80 Park Place, Newark, N. J.

Salvador, J. V. (C), Manager, Standards Dept., R. G. Le Tourneau, Inc.; (Res.) 924 Hamilton Blvd., Peoria, Ill.

Samans, Walter (B), Mech. Engineer, Sun Oil Co., 1608 Walnut St., Philadelphia, Pa.

Sander, C. P. (A), General Supt., Western Pipe & Steel Co., 5717 Santa Fe Ave., Los Angeles, Calif.

Sangdahl, Geo. S. (C), Manager, Cleveland Office, Chicago Bridge & Iron Works, 654 Rockefeller Bldg., Cleveland, Ohio.

Sanneman, W. J. (B), Service Metallurgist, Tennessee Coal, Iron & R. R. Co., 1508 Brown-Marx Bldg., Birmingham, Ala.

Sarazin, R. (B), Prop., Soudure A L'Arc Sarazin, 47 Blvd. Levallois, Neuilly, Sene, France.

Sargent, Harold F. (D), Welder, Bethlehem Ship Building Corp.; (Res.) 33 Beideman St., San Francisco, Calif.

Sarvis, A. L. (D), Welding Operator, Allis-Chalmers Mfg. Co.; (Res.) 7828 Milwaukee Ave., Wauwatosa, Wis.

Sasek, Fred (D), Welder, Western Cartorage Co.; (Res.) R. R. 4, Edwardsville, Ill.

Satterlee, W. F. (B), Production Manager, % Hunter Steel Co., Neville Island, Pa.

Sauer, Carl M. (B), Sales Engineer, Air Reduction Sales Co.; (Res.) 3948 N. Delaware, Indianapolis, Ind.

Saul, Harry H. (C), Welding Instructor, H. Fletcher Brown Voc. High School, 14th & Market St., Wilmington, Del.

Saunders, Harold W. (C), District Manager, Air Reduction Sales Co., Park Avenue & Halleck St., Emeryville, Calif.

Sawyer, W. B. (C), Asst. General Manager of Sales, Columbia Steel Co., 1426 Russ Bldg., San Francisco, Calif.

Saxe, Van Rensselaer P. (B), Consulting Engineer, 100 W. Monument St., Baltimore, Md.

Sayre, Mortimer F. (C), Prof. of Applied Mechanics, Union College, Schenectady, N. Y.

Scarbery, Jasper L. (B), Acting Foreman, Jeffrey Mfg. Co., 4220 Cleveland Ave., Columbus, Ohio.

Schade, Wilbert C., Jr. (C), Prod. Manager, Brooks Paper Co.; (Res.) 7920 Delmar Blvd., University City, Mo.

Schafer, Charles N. (C), 14732 Wemple Road, Cleveland, Ohio.

Schafer, Philip E. (B), Welding Engineer, Schafer & Timlen Testing Labs., 1221 East 20th St., Baltimore, Md.

Schaffter, G. A. (C), Western Electric Co., Dept. 783-3, Kearny, N. J.

Schaible, Frank W. (C), Welding Foreman, Link Belt Co.; (Res.) 818 Chestnut St., Lansdale, Pa.

Schane, Harry P. (B), Welding Supervisor, Allis-Chalmers Mfg. Co., N. S. Pittsburgh, Pa.

Scharwenka, A. V. (C), President, Scharwenka & Welch Inc.; (Res.) 811 Bradford Ave., Westfield, N. J.

Schattel, K. F. (A), National Cylinder Gas Co., 205 W. Wacker Dr., Chicago, Ill.

Schaupp, J. W. (C), District Sales Manager, National Cylinder Gas Co., 4th & Thorn St., Coraopolis, Pa.

Scheerer, Carl F. (D), Welding Operator, Bethlehem Steel Co.; (Res.) 316 S. East Ave., Baltimore, Md.

Scheffler, C. A. (C), 3627 West 129th St., Cleveland, Ohio.

Schellhammer, Lawrence W. (C), Salesman, The Otis Steel Co.; (Res.) 5026 Oberlin Blvd., Cincinnati, Ohio.

Schenck, Chas. (A), Engineer of Development, Bethlehem Steel Co., Bethlehem, Pa.

Schenler, Walter M. (B), Vice-President and Treasurer, Superior Structural Steel Co., 5100 Farlin Ave., St. Louis, Mo.

Scherer, A. W. (D), Welder, Baash Ross Tool Co.; (Res.) 918 Quincey St., Bakersfield, Calif.

Schermerhorn, J. A. (C), Acting Works Manager, American Welding Co., Carbondale, Pa.

Scherrer, A. G. (C), District Sales Manager, National Cylinder Gas Co., 998—39th St., North Bergen, N. J.

Scherrer, Robert (D), Welder, Gilmore Oil Co.; (Res.) 3334 Lowell Ave., Los Angeles, Calif.

Schexschnider, Lee (D), Welder, Wilshire Oil Co.; (Res.) 163 E. 6th St., Downey, Calif.

Schieckenburg, Rene (D), Welder, R. G. Le Tourneau, Inc.; (Res.) 1009 W. Gift, Peoria, Ill.

Schiemann, E. H. (C), Mech. Engineer, % Kimberly-Clark, Neenah, Wis.

Schillinger, Charles F., Jr. (D), Operator, American Laundry Mach. Co.; (Res.) 126 Evangeline St., Rochester, N. Y.

Schimpke, Albert J. (C), Engineer, Murray W. Sales Co.; (Res.) 6830 Penrod, Detroit, Mich.

Schipplock, C. J. (B), Supt.-Dept. No. 10, Crane Co.; (Res.) 2114 W. 114 Place, Chicago, Ill.

Schleich, Randall (B), Engineer, The Travelers Insurance Co., Engineering Division, 7th Floor, Liberty Bank Bldg., Main St., Buffalo, N. Y.

Schleicher, Jacob Wm. (C), Sales Promotion, The Resisto-Loy Co., 1646—33rd St. N. W., Washington, D. C.

Schliecker, G. H. (B), Salesman, R. C. Neal Co., 76 Pearl St., Buffalo, N. Y.

Schlieder, H. J. (C), District Manager, Air Reduction Sales Co., 730 Grant St., Buffalo, N. Y.

Schlis, Wm. J. (D), 216 Wisconsin, Peoria, Ill.

Schlittler, H. F. (B), Asst. General Manager, Abegg & Reinhold Co., 2533 E. 26th St., Los Angeles, Calif.

Schlitz, Thomas R. (C), Chief Welding Instructor, Casey Jones School of Aeronautics, 1100 Raymond Blvd., Newark, N. J.

Schlup, Bernard L. (D), Welder & Mechanic; (Res.) 425 W. 35 St., Kansas City, Mo.

Schmidt, E. C. (C), Electric Welding, Keystone S. & W. Co., 706 W. Corrington Ave., Peoria, Ill.

Schmidt, Edward J. (D), 2011 Grove St., Brooklyn, N. Y.

Schmidt, Geo. W. (D), Supt. of Power, Indiana & Michigan Electric Co., Twin Branch Plant, Mishawaka, Ind.

Schmidt, J. Bernie (C), Welder, R. G. Le Tourneau, Inc.; (Res.) Chillicothe, Ill.

Schmidt, Karl W. (B), President, Builders Steel Co., 12th & Gentry, No. Kansas City, Mo.

Schmitt, William J. (B), President, Flesch & Schmitt Inc., 118 Brown St., Rochester, N. Y.

Schmuller, Frederick M. (B), Structural Engineer, Dept. of Public Works, 125 Worth St., New York, N. Y.

Schneider, Charles A. (C), Draftsman, Link-Belt Co.; (Res.) 1423 W. 90th St., Chicago, Ill.

Schneider, George (C), Acetylene & Welding Equip. Co., 1065 Atlantic Ave., Brooklyn, N. Y.

Schneiderwind, L. O. (B), President, Omaha Welding Co., 1501 Jackson St., Omaha, Nebr.

Schnetzer, S. (B), 125 Terrace Ave., Hasbrouck Heights, N. J.

Schoen, Henry (B), R-1-Benning Rd., Box 163-K., Washington, D. C.

Schoenbaum, Carl F. (D), Engineer, Harnischfeger Corp.; (Res.) 1506 So. 57th St., West Allis, Wis.

Schoenberg, Chas. W. (D), Estimator & Salesman, Jos. T. Ryerson & Son Inc.; (Res.) 454 North 39th St., Milwaukee, Wis.

Schoener, J. G. (B), Welding Engineer, International Nickel Co., Bayonne, N. J.

Schooley, F. W. (C), Contracting Engineer, Chicago Bridge & Iron Co., 608 So. Hill St., Los Angeles, Calif.

Schotte, Harry B. (D), Arc Welder, Chicago Bridge & Iron Co.; (Res.) 321 Lincoln Ave., Sharon, Pa.

Schramm, C. H. (B), Shaw-Box Crane & Hoist Co., Muskegon, Mich.

Schreeck, Karl W. (C), Design Engineer, Una Welding Inc.; (Res.), 1740 Page Ave., E. Cleveland, Ohio.

Schreffler, Chas. L. (D), Welder, Coleman Lamp & Stove Co.; (Res.) 1350 So. Market, Wichita, Kansas.

Schreiner, Norman George (B), Engineer, The Linde Air Products Co.; (Res.) 2037 N. 15 St., Philadelphia, Pa.

Schroeder, Emil C. (B), Paper Calmenson & Co., 975 East Seventh St., St. Paul, Minn.

Schroeder, Otto (C), Research & Mechanical Engineer, Soulé Steel Co., 1750 Army St., San Francisco, Calif.

Schuler, George (D), Welder, Bethlehem Shipbldg. Corp.; (Res.) 11 S. Comkling St., Baltimore, Md.

Schultz, Herold (F), 124 West St., Stillwater, Okla.

Schulz, William H. (B), Supt. of Production, % Donaldson Co. Inc., 666 Pelham St., St. Paul, Minn.

Schur, Robt. J. (D), Welding Operator, Delco Appliance Corp.; (Res.) 55 Buffalo St., Churchville, N. Y.

Schwarting, H. F. (C), Field Engineer, American Car & Foundry Co.; (Res.) 4950 Sutherland Ave., St. Louis, Mo.

Schwartz, Harry (D), Arc Welding, Eastman Kodak Co.; (Res.) 1821 Stone Road, Rochester, N. Y.

Schwarz, Arthur (B), Engineer, John Nooter Boiler Works Co.; (Res.) 7438 Williams Ave., Richmond Heights, Mo.

Schwemle, Carl (D), Forestburg, South Dakota.

Schwon, Paul (D), Welder, Link Belt Co.; (Res.) 8938 Blackstone Ave., Chicago, Ill.

Sciaky, David (B), Managing Director, Soudeuses Electrique Sciaky, 13 Rue Charles Fourier, Paris 13, France.

Scioletich, Mathew (C), Welding Engineer, Combustion Engrg. Co., Heine Boiler Division, 5319 Shreve Ave., St. Louis, Mo.

Scolari, T. (C), Data & Information Bureau, Compania Generale De Electtricita, Casella Postale 1658, Milan, Italy.

Scott, Carlyle H. (B), President, Scott Welding Corp., 34-15 Bradley Ave., Long Island City, N. Y.

Scott, Charles H. (C), Development Engineer, The Dorr Co., Inc., 570 Lexington Ave., New York, N. Y.

Scott, Douglas (D), Welder & Machinist, Waite Amulet Mines, Ltd., Noranda, P. Q., Canada.

Scott, Edward T. (B), President, Cleveland School of Welding Inc., 2261 E. 14 St., Cleveland, Ohio.

Scott, George E. (B), Manager (Ontario Div.), Dominion Welding Eng. Co. Ltd.; (Res.) 25 Edith Drive, Toronto 12, Ontario, Canada.

Scott, G. F. (C), Scott-Foster & Co., 126 Powell St., Vancouver, B. C., Canada.

Scott, Stephen M. (D), Arc Welder, General Electric Co.; (Res.) 707 Union St., Schenectady, N. Y.

Scott, S. S. (C), Senior Welding Engineer, N. Y. Navy Yard; (Res.) 115-18—200th St., St. Albans, N. Y.

Seabloom, E. R. (A), Research Engineer, Crane Co., 836 S. Michigan Ave., Chicago, Ill.

Seabury, C. C. (C), U. S. N., Bureau of Yards & Docks, Navy Dept., Washington, D. C.

Sears, Clifford M. (C), Vice-President & Secy., R. C. Neal Co., Inc., 46-48 Andrews St., Rochester, N. Y.

Seawell, Lacy W. (C), Structural Engineer, P. O. Box 1353, Atlanta, Ga.

Sebby, R. M. (D), Welder, Pomona Pump Co.; (Res.) 310 E. 6th St., Pomona, Calif.

Secretary, The (B), Marine Dept., Wellington C. 1, New Zealand.

Sedam, Marvin (B), Harnischfeger Corp., Milwaukee, Wis.

See, P. G. (F), Aggie Halls, Stillwater, Okla.

Seely, R. W. (D), Welding Operator, General Electric Co.; (Res.) 1524 Myron St., Schenectady, N. Y.

Selden, Edward W. (B), Welding Engineer, Struthers Wells Co.; (Res.) 10 Bishop Road, Guilford, Baltimore, Md.

Seltzer, Clarence E. (D), Welding Leader, Sun Shipbldg. & Dry Dock Co.; (Res.) 727 Jeffrey St., Chester, Pa.

Selvatico, D. M. (D), Apprentice Welder, Ordnance School, 40th Ord. Co., Raritan Arsenal, Metuchen, N. J.

Semik, Charles A. (C), Salesman, Omaha Welding Co., Omaha, Nebr.

Senesky, John S. (D), Dem., The Air Reduction Sales Co.; (Res.) 75—12th Ave., Sea Cliff, N. Y.

Sessions, F. L. (B), Partner, Sessions & Sessions, Consulting Engineer, 1529 Rockefeller Bldg., Cleveland, Ohio.

Seybold, Eugene (B), Production Engineer, Baldwin Locomotive Works, Philadelphia, Pa.

Seyffert, E. (D), 11526 Eggleston Ave., Roseland, Ill.

Seyfried, L. M. (C), Salesman, Sutton-Garten Co.; (Res.) 6727 Riverview Dr., Indianapolis, Ind.

Seymour, A. W. (D), Welder, Noranda Mines Ltd.; (Res.) 133 Eighth St., Noranda, Quebec, Canada.

Shackleton, F. W. (C), Salesman, Air Reduction Sales Co.; (Res.) 1465 Republic Ave., Columbus, Ohio.

Shadrake, Bolick J. (B), Designer, Erie Railroad Co.; (Res.) 3235 West 116th St., Cleveland, Ohio.

Shafer, A. B. (C), Vice-President, Garrett & Shafer Engrg. Works, 322 First Ave. South, Seattle, Wash.

Shaner, E. L. (A) President, Penton Publishing Co., 1213-35 West Third St., Cleveland, Ohio.

Sharp, H. P. (C), Methods Engineer, The Bristol Co., Waterbury, Conn.

Shaver, P. E. (B), Sales Engineer, Sun Shipbldg. & D. D. Co., Chester, Pa.

Shaw, Earston L. (D), Com. Welder, American Woolen Co.; (Res.) 9 Chester St., Melrose, Mass.

Shaw, Franklin B, (D), Swedesboro, New Jersey.

Sheaff, H. H. (C), Asst. Manager, Combustion Engrg. Co., Heine Boiler Div., 5319 Shreve Ave., St. Louis, Mo.

Shearburn, W. C. (C), Chief Inspector, R. G. Le Tourneau Inc., 207 Le Tourneau Court, Peoria, Ill.

Shearouse, James D. (B), Chief Engineer Sou Iron & Equip. Co., Atlanta, Ga.

Sheets, C. L. (D), Welder, Ernstmann Machine & Repair Co.; (Res.) 1817½ So. Main St., Wichita, Kansas.

Sheffer, John S. (B), Electrical Engineer, American Car and Foundry Co., Room 1322—30 Church St., New York City.

Shelbaer, W. N. (C), Foreman, Hope Natural Gas Co.; (Res.) Wolf Summit, W. Va.

Sheldon, Lucian A. (B), Engineer, General Electric Co., Schenectady, N. Y.

Shelton, E. P. (C), Manager of Detroit Office, Chicago Bridge & Iron Works, 1313 Lafayette Bldg., Detroit, Mich.

Shem, G. W. (B), President, The Alliance Structural Co., Alliance, Ohio.

Sheppard, George W. (B), Supervising Engineer, Ocean Accident & Guarantee Corp., 530 Insurance Exchange Bldg., Chicago, Ill.

Sherbondy, Fred G. (B), Vice-President, The Biggs Boiler Works Co., Akron, Ohio.

Sherman, Wm. F. (C), Editor (Detroit), The Iron Age, 7310 Woodward Ave., Detroit, Mich.

Sherrer, C. H. (B), District Manager, Toledo Terr., Lincoln Electric Co., 662 Spitzer Bldg., Toledo, Ohio.

Sherriff, James (D), Quarterman Supervisor, Bethlehem Steel Corp.; (Res.) 99 Goddard St., Quincy, Mass.

Sherry, Clarence (C), Welding Supervisor, Dominion Bridge Co., Box 310, Terminal A, Toronto, Ont., Canada.

Sherwood, J. G. (D), Welder, Weldrite Service Co.; (Res.) Route 6, Camden Station, Minneapolis, Minn.

Sherwood, L. K. (D), Welding Operator, Milton Bowers Welding Co., 346 Madison Ave., Memphis, Tenn.

Shields, W. (C), Assistant Foreman, Burners & Welders, Federal Shipbuilding & Dry Dock Co.; (Res.) 210 Fifth St., Jersey City, N. J.

Shilling, William (B), Honolulu Rapid Transit Co., 1133 Alpai St., Honolulu, T. H.

Shiner, George E. (C), Salesman, The Linde Air Products Co., 56th & Center Sts., Omaha, Nebr.

Shinkle, J. B. (B), Foreman, National Tank Co., Box 1588, Tulsa, Okla.

Shipman, Wm. H. (B), Engineer on Plant & Production Work, Babcock & Wilcox Co., P. O. Box 242, Barberton, Ohio.

Shipman, W. P. (C), Works Manager, Blaw-Knox Co.; (Res.) 1200 N. Sheridan Ave., Pittsburgh, Pa.

Shodron, John G. (C), Consulting Engineer, 1810 W. Wisconsin Ave., Milwaukee, Wis.

Shoemaker, Lewis F., Jr. (C), Draftsman, Bethlehem Steel Co.; (Res.) 835 N. Franklin St., Pottstown, Pa.

Shook, Henry W. (C), Foreman, Standard Oil Co. of N. J., Boston & Dean Sts., Baltimore, Md.

Short, E. (B), Welding Foreman, Cleaner Harvester Co., 10112 E. 18th St., Independence, Mo.

Shoultz, Wm. L. (C), Plant Supt., Stainless Steel Prods. Co.; (Res.) 964 Berry Ave., St. Paul, Minn.

Shryock, J. G. (B), Vice-President & Chief Engineer, Belmont Iron Works, 22nd St. & Washington Ave., Philadelphia, Pa.

Shultz, Bennie (C), Supervisor of Power & Light, University of Oklahoma, Faculty Exchange, Norman, Okla.

Shushansky, Hyman (D), Aircraft Welder & Sheet Metal Worker, U. S. Army Air Corps., Mitchell Field, 2nd Air Base Squad, Hempstead, N. Y.

Sibley, R. L. (D), Welder, Balmar Corp.; (Res.) 4224 Falls Road, Baltimore, Md.

Sidney, C. Watson (B), Welding Engineer, RCA Mfg. Co., Harrison, N. J.

Sieger, George N. (B), President & General Manager, S. M. S. Corp., 1165 Harper Ave., Detroit, Mich.

Siemer, Robert (B), President & Treasurer, Allan Manufacturing & Welding Co., Inc., 726 Washington St., Buffalo, N. Y.

Silver, Roy H. (B), President, Silver Welding Supply, West Roxbury, Mass.

Simmons, W. A. (D), Apprentice Welder, U. S. Pipe Co.; (Res.) 2109 N. 19th St., Bessemer, Ala.

Simmons, Walter H. (C), Owner, Welding Sales and Engineering Co., 1627 W. Fort St., Detroit, Mich.

Simms, Howard N. (D), Black, Sivalls & Bryson, Inc., P. O. Box. 1377, Oklahoma City, Okla.

Simon, Edward F. (B), General Manager & Secretary, The Ohio Machine & Boiler Co., 1501 University Road, Cleveland, Ohio.

Simon, Richard F. (F), 41 E. Woodruff Ave., Columbus, Ohio.

Sinclair, Richard G. (C), Draftsman, Federal Machine & Welder Co.; (Res.) 1960 Estabrook, N. W., Warren, Ohio.

Singleton, G. R. (B), President, New England Welding Labs., Inc., 88 St. Stephen St., Boston, Mass.

Singleton, Jack (C), District Engineer, American Inst. of Steel Const., Inc., 622 New England Bldg., Topeka, Kansas.

Sinn, Joseph (C), Rural Route 1, Jefferson City, Mo.

Sir, Walter W. (C), Master Mechanic, Commonwealth Edison Co., 3501 S. Pulaski Road, Chicago, Ill.

Sisk, J. W. (D), Burner & Acety. Mach. Operator, Goslin-Birmingham Mfg. Co., Route 1, Box 51, Irondale, Ala.

Sivley, G. A. (C), Designing Engineer, Sommer Products Co.; (Res.) 2226 Missouri Ave., Peoria, Ill.

Sivyer, William (C), Sales Engineer, Lincoln Electric Co.; (Res.) 2948 Rising Sun Road, Ardmore, Pa.

Skinner, M. G. (B), Secretary & Treasurer, St. Louis Blow Pipe & Heater Co., 1948 N. 9th St., St. Louis, Mo.

Skinner, Orville B. (C), Electrical Engineer, U. S. Naval Gun Factory; (Res.) 4443 Ellicott St., N. W., Washington, D. C.

Skog, Ludwig (B), Vice-President, Sargent & Lundy, Inc., Engineers, 140 South Dearborn St., Room 1600, Chicago, Ill.

Skuhrovec, J. (C), 12909 Farringdon Ave., Cleveland, Ohio.

Skutt, John (D), Welder, Midland Steel Prods. Co.; (Res.) 2067 W. 106th St., Cleveland, Ohio.

Slater, S. J. (C), Supervisor, Applied Engrg. Dept., Air Reduction Sales Co., Dorchester, Mass.

Sledge, Alexander (B), Lt. Comdr., C. C., U. S. Navy, Navy Yard, Mare Island, Calif.

Sleeman, W. C. (B), Supt., Pullman Standard Car Mfg. Co., 5th Ave. & 24th St., Box 308, Bessemer, Ala.

Slipper, Alan G. (C), Welding Sales Engineer, Hawaiian Gas Products Ltd., P. O. Box 2454, Honolulu, T. H.

Sloan, John (D), Welder, Link-Belt Co.; (Res.) 1418 Summerdale Ave., Chicago, Ill.

Sloane, P. C. (C), Sales Representative, The Linde Air Products Co.; (Res.) 328 E. 18th Ave., Columbus, Ohio.

Slosson, John (C), J. H. Williams & Co., 400 Vulcan St., Buffalo, N. Y.

Slottman, George V. (C), Manager, Applied Engineering Dept., Air Reduction Sales Co., Lincoln Bldg., New York City.

Sluss, A. H. (B), Professor Mechanical Engineering, University of Kansas, Lawrence, Kansas.

Smart, Walter C. H. (C), Welding Supervisor, Ingersoll-Rand Co.; (Res.) 2115 Northampton St., Easton, Pa.

Smellie, J. T. (D), Electric & Oxyacetylene Welder, The Steel Co. of Canada, Ltd.; (Res.) 40 Huxley Ave., N. Hamilton, Ontario, Canada.

Smith, A. E. (D), Inspector, Link-Belt Co.; (Res.) Midlothian, Ill.

Smith, Abram E. (B), Vice-President, Union Tank & Car Co., 228 N. La Salle St., Chicago, Ill.

Smith, Alex (D), The Youngstown Welding & Engrg. Co., 3700 Oakwood Ave., Youngstown, Ohio.

Smith, Alonzo (D), Welder, Sound Welding Inc., 905 E. 134th St., Bronx, N. Y.

Smith, Arthur W. C. (B), Assistant Manager, The Phoenix Iron Co., Phoenixville, Pa.

Smith, Belmont (B), Sales Engineer, The Galicher Co.; (Res.) 1473 Redondo, Salt Lake City, Utah.

Smith, B. K. (A), President, Big Three Welding Equip. Co., 509 M. & M. Bldg., Houston, Texas.

Smith, Cecil E. (C), Plant Supt., Air Reduction Sales Co.; (Res.) 1638 Jaeger Ave., Louisville, Ky.

Smith, Charles (D), Welder, W. C. Norris Mfg. Co.; (Res.) 430 S. 39 W. Ave., Tulsa, Okla.

Smith, Charles F. (D), Welder, Thibodaux Boiler Works, Inc.; (Res.) 3435 Government St., Baton Rouge, Louisiana.

Smith, Charles S. (B), District Manager, The Linde Air Products Co., 114 Sansome St., San Francisco, Calif.

Smith, Emmett A. (D), Lincoln Electric Co., 15920 Hazel Road, E. Cleveland, Ohio.

Smith, E. Butler (B), Supervising Mechanical Engineer, Pioneer Mill Co., Ltd., Lahaina Mavi, T. H.

Smith, E. C. (B), Chief Metallurgist, Republic Steel Corp., 1607 Republic Bldg., Cleveland, Ohio.

Smith, Everett G. (B), Welding Instructor, Louisiana State University; (Res.) 3243 Highland Road, Baton Rouge, La.

Smith, E. W. P. (B), Consulting Engineer, Lincoln Electric Co., 3580 Blanche Road, Cleveland Hts., Ohio.

Smith, F. B. (C), Shop Foreman, Motor Center; (Res.) 1158 Flower St., Bakersfield, Calif.

Smith, F. H. (C), District Manager, The Lincoln Electric Co., 2011 Florence Ave., Cincinnati, Ohio.

Smith, Fred M. (D), Welding Operator, Chicago Bridge & Iron Co.; (Res.) 43 Harrison St., Greenville, Pa.

Smith, Geo. P. (C), Quarterman Boiler Maker, Washington Navy Yard; (Res.) 1424-K St., S. E., Washington, D. C.

Smith, H. Christa (B), Sales Engineer, Federal Machine & Welding Co., 626 Broadway, Cincinnati, Ohio.

Smith, Harry H. (C), Sales, The Youngstown Sheet & Tube Co., Stambaugh Bldg., Youngstown, Ohio.

Smith, Hope (C), Plant Engineer, Combustion Eng. Co., 1032 W. Main St., Chattanooga, Tenn.

Smith, H. Sidney (A), Consulting Engineer, Union Carbide Co., 30 E. 42nd St., New York, N. Y.

Smith, John (B), Supt., Bethlehem Shipbldg. Corp. Ltd.; (Res.) 98 E. Howard St., Quincy, Mass.

Smith, John (C), Welding Foreman, Fisher Body Co., 1413 Regent Road, Wickliffe, Ohio.

Smith, J. F. (B), Welding Engineer, Dravo Corp., Neville Island Station, Pittsburgh, Pa.

Smith, Leo. W. (C), Director of Welding School, Providence School of Practical Training, 97 Fountain St., Providence, R. I.

Smith, Martin M. (B), General Manager & Assistant Treasurer, Commercial Acetylene Supply Co., 60 East 42nd St., New York, N. Y.

Smith, Melvin (D), Welder, Sound Welding, Inc., 905 E. 134th St., Bronx, N. Y.

Smith, Norman E. (D), Chief Welder, Material Service Corp.; (Res.) 614 West Monroe St., Joliet, Ill.

Smith, Otis L. (B), President Weldit Acetylene Co., 638 Bagley Ave., Detroit, Mich.

Smith, Paul E. (C), District Sales Manager, National Cylinder Gas Co., 529 Felicity St., New Orleans, La.

Smith, Philip (D), 217 York Ave., Brighton, S. I., N. Y.

Smith, Richard D. (D), Heat Treater, York Safe & Lock Co.; (Res.) 445 E. Princess St., York, Pa.

Smith, Roland (D), Welder Helper, Baash Ross Tool Co., Bakersfield, Calif.

Smith, Stanley (D), Welder, J. B. Klein Iron & Fdry. Co., 1401 N. W. 3rd St., Oklahoma City, Okla.

Smith, Stephen (C), Specialist, Applied Engineering Dept., Air Reduction Sales Co.; (Res.) 14 Tonnele Ave., Jersey City, N. J.

Smith, S. H. (C), Dist. Manager, Air Reduction Sales Co.; (Res.) 818 W. Winnebago St., Milwaukee, Wis.

Smith, T. A. (D), Inspector, R. G. Le Tourneau, Inc.; (Res.) 2400 No. Madison, Peoria, Ill.

Smith, B. Thomas (B), Foreman, Welding & Burning Dept., Federal Shipbuilding & Dry Dock Co.; (Res.) 316 Stegman Parkway, Jersey City, N. J.

Smith, Thomas E. (D), Welder, Eastman Kodak Co.; (Res.) 2044 Dewey Ave., Rochester, N. Y.

Smith, Turner C. (B), Asst. Engineer, General Petroleum Corp., 2525 E. 37th St., Los Angeles, Calif.

Smith, V. M. (B), Instructor of Welding, University of Kansas; (Res.) 1411 Haskell Ave., Lawrence, Kansas.

Smith, W. C. (B), 1503 E. Erwin St., Tyler, Texas.

Smith, Wm. E. (B), Chief Boiler Inspector, Hawaiian Sugar Planters Assn., P. O. Box 411, Honolulu, Hawaii,

Smith, Wm. R. (D), Salesman, Lincoln Elec. Co., 812 Mateo, Los Angeles, Calif.

Smith, William R. (C), Proprietor of W. R. Smith Welding School, 250 West 54th St., New York City.

Smith, W. W. (A), Asst. to President, Inland Steel Co., 38 S. Dearborn St., Chicago, Ill.

Smith, W. W. (C), Civil Engineer, Bureau of Yards & Docks, 3008 Second Road N., Arlington, Va.

Smith, W. Walter, Jr. (D), Asst. Master Mech., Morris P. Kirk & Son, Inc.; (Res.) 2144½ Las Colinas Ave., Los Angeles, Calif.

Smithers, Joseph (C), Sales Representative, Western Dist., Granite City Steel Co., 1104 R. A. Long Bldg., Kansas City, Mo.

Smyth, Robert (C), Marine Inspector (Boiler), Bureau of Marine Insp. & Nav., Commerce Bldg., Rm. 1872, Washington, D. C.

Snead, Marion C. (B), Plant Supt., Link, Belt Co., 116 Murphy Ave., S. W., Atlanta, Ga.

Snider, Robert (D), 5843 Erie St., Hammond, Ind.

Snipes, A. D. (C), Industrial Sales Engineer, Westinghouse Electric & Mfg. Co., 426 Marietta St., N. W., Atlanta, Ga.

Snow, Walter A. (D), Welder, Southern Pacific Co., % R. B. Chapman, 65 Market St., San Francisco, Calif.

Snyder, Carl (D), 15602 Lexington Ave., Harvey, Ill.

Snyder, George V. (D), Welder, Otis Elevator Co.; (Res.) 33 So. 12th Ave., Mt. Vernon, N. Y.

Snyder, P. W. (C), Lieutenant (Construction Corps.), U. S. Navy; Bureau of C. & R., Navy Dept.; (Res.) 1870 Wyoming Ave., N. W., Washington, D. C.

Soden, W. E. (C), Sun Oil Co., 1608 Walnut St., Philadelphia, Pa.

Soderstrom, E. D. (B), Assoc. Prof., Div. of Engrg., Oklahoma A. & M. College, Stillwater, Okla.

Sohn, Jesse S. (D), Welder, American Bridge Co., Leetsdale, Pa.

Somerville, G. G. (B), Electrical Engineer, General Electric Co., 63 Easton Ave., Pittsfield, Mass.

Sommer, Direktor Dr. Franz (C), Werksdirektor, Dr. Mont., Gebr. Böhler & Co. A.-G. Edelstahlwerk Düsseldorf, Dusseldorf-Oberkassel, Postfach 55, Germany.

Sommer, Phillip (C), Foreman, R. G. Le Tourneau, Inc.; (Res.) 207 N. Bourland, Peoria, Ill.

Sommerville, Richard V. (D), 2403 Maple Ave., N. S., Pittsburgh, Pa.

Sonderegger, A. (B), Engineer, E. T. H., Engineering Office, Seminarstr 21, Zurich 6, Switzerland.

Sonneborn, Arthur B. (B), District Manager, The Federal Machine & Welder Co., 1102 Fisher Bldg., Detroit, Mich.

Sorensen, Martin (C), Foreman, Food Machinery Co., 333 West Julian St., San Jose, Calif.

Sorensen, W. C. (C), Boiler Maker & Welder, American Crystal Sugar Co., Box 562, Oxnard, Calif.

Sorkin, Josef (B), Structural Designer, Ash, Howard, Needles & Tammen; (Res.) 1631 E. 49th St., Kansas City, Mo.

Soyars, John M. (C), Manager, National Cylinder Gas Co.; (Res.) 1502½ 16th Ave., So., Birmingham, Ala.

Sparks, H. H. (C), Mechanical Engineer, Dept. Water & Power; (Res.) 5118 Echo St., Los Angeles, Calif.

Sparling, J. T. (B), Standard Tank Co., 3232 E. 50th St., Los Angeles, Calif.

Sparr, Emil (B), Shop Supt., Genesse Bridge Co., Inc., 344 West Ave., Rochester, N. Y.

Spaulding, Ralph E. (B), President, Aetna Steel Construction Co.; (Res.) 1252 Windsor Place, Jacksonville, Fla.

Spaulding, Roy L. (C), Welding Foreman, American Hoist & Derrick Co., 63 S. Robert St., St. Paul, Minn.

Specht, Joseph (C), % Otis Steel Co., 3341 Jennings Road, Cleveland, Ohio.

Speed, Francis H. (C), Chief Engineer, Thomson-Gibb Electric Welding Co. 161 Pleasant St., Lynn, Mass.

Speller, Thomas H. (C), Partner, General Welding & Engineering Co.; (Res.) Bowen Road, Elma, N. Y.

Spence, Stanley L. (C), Secretary-Treasurer, Tulsa Boiler & Machinery Co., P. O. Box 1978, Tulsa, Okla.

Spencer, Coleman J. (C), Sinclair Refining Co., Pipe Line Dept., Humboldt, Kansas.

Spencer, Raymond A. (B), Asst. General Manager, The Canadian Bridge Co., Ltd., Walkerville, Ont., Canada.

Spenzer, E. B. (C), Supt. of Construction & Maintenance, The Cleveland Railway Co., 600 Midland Bldg., Cleveland, Ohio.

Spicer, Kenneth M. (C), Welding Engineer, The International Nickel Co., Research Laboratory, Oak St., Bayonne N. J.

Spiegelhalter, Andrew G. (A), Vice-President The Pusey & Jones Corp., Wilmington, Del.

Spielvogel, Siegfried W. (C), Assistant Division Engineer, Consolidated Edison Co. of N. Y., 4 Irving Place, New York City.

Spindler, W. A. (C), Assistant Professor of Metal Processing, University of Michigan, Department of Metal Processing; (Res.) 2047 E. Engineering Bldg., Ann Arbor, Mich.

Spittler, Eugene R. (C), Resistance Welding Engineer, General Electric Co.; (Res.) 6 Cornelius Ave., Schenectady, N. Y.

Spoon, Robert (D), 2828 E. Michigan St., Indianapolis, Ind.

Spooner, Leonard G. (B), Sales Manager, Arthur C. Harvey Co.; (Res.) 60 Everett St., Allston, Mass.

Sporn, Philip (A), Vice-President in Charge of Engineering, American Gas and Electric Service Corp., 30 Church St., New York, N. Y.

Sprague, A. G. (C), Proprietor, Sprague Service & Supply Co., 276–78 Davidson Ave., Perth Amboy, N. J.

Spraragen, William (B), Technical Secretary and Editor, American Welding Society; (Res.) 119–47—177th Place, St. Albans, Long Island, N. Y.

Sprehe, Francis (C), Vice-President, Midwest Steel Co., 2 N. Indiana, Oklahoma City, Okla.

Stahl, H. Hugo (C), Sales Engineer, Lincoln Electric Co.; (Res.) 1627 E. Duval St., Philadelphia, Pa.

Stallone, Leonard (D), Machine Operator, Aluminum Co. of America; (Res.) 122 Cacciola Place, Westfield, N. J.

Stander, E. R. (D), 1203 Whites Road, Kalamazoo, Mich.

Stanek, A. J. (D), Welder, 40th Ord. Co., Raritan Arsenal, Metuchen, N. J.

Stanfield, R. S. (B), Chief Engineer, Tide Water Assoc. Oil Co., East 22nd St., Bayonne, N. J.

Stanley, A. (D), 10153 S. Lowe Ave., Chicago, Ill.

Stanley, LeRoy F. (D), Operator of Own Welding Shop; (Res.) Route 2, Beloit, Ohio.

Stanley, Richard W. (B), Director of Mechanical Research, American Viscose Corp., Marcus Hook, Penna.

Stanulis, Joe S. (D), Proprietor, General Welding Works, 3130 So. Jefferson, Saginaw, Mich.

Stark, Guy F. (C), Electrical Engineer, Stark Electric Co., York & William Sts., Baltimore, Md.

Stasz, John (D), Millwright, Dodge Brothers Corp.; (Res.) 3835 Casmere, Detroit, Mich.

Steadman, W. C. (D), Welder, Pacific Welding Works, 2561 E. 54th St., Huntington Park, Calif.

Steck, Robert C. (D), Dem., Air Reduction Sales Co.; (Res.) 242 So. Columbus Ave., Mt. Vernon, N. Y.

Steckel, Frederick R. (B), Metallurgist, Columbia Steel Co., Russ Building, San Francisco, Calif.

Steele, Frank (B), Welding Engineer, Scully Steel Products Co.; (Res.) 853 Pleasant St., Oak Park, Ill.

Steele, Maurice G. (B), Hokeland, Havre de Grace, Md.

Steele, Roy M. (D), Electric Welder, Pfaudler Co.; (Res.) 269 Wellington Ave., Rochester, N. Y.

Steen, R. H. (C), Resident Construction & Designing Engineer, The Ohio Power Co., Philo, Ohio.

Steffen, G. C. (B), Special Sales Dept., Wessendorff, Nelms & Co., Houston, Texas.

Steidl, Erwin A. (B), Sales Engineer, Harnischfeger Corp., 4400 W. National Ave., Milwaukee, Wis.

Steinman, Geo. C. (C), Asst. Marine Engineer, Bur. of Marine Insp. & Nav., Commerce Bldg., Rm. 1069A, Washington, D. C.

Steinman, J. E. (B), General Supt., The American Shipbldg. Co., Foot of West 54th St., N. W., Cleveland, Ohio.

Steinmetz, Leslie (C), Salesman, % Commercial Gas Co., 2633—4th St., S. E., Minneapolis, Minn.

Steinmeyer, J. W. (B), Metallurgical Engineer, American Car & Fdy. Co., Berwick, Pa.

Stelljes, Gomer (C), Pipe Line Foreman, W. C. Sykes Co.; (Res.) 49 Hortense St., Rochester, N. Y.

Stepath, Nyron D. (F), 2983 Alabama Road, Camden, N. J.

Stephens, De Witt (D), Operator, City of Atlanta; (Res.) 790 Ponce De Leon Pl., Atlanta, Ga.

Stephenson, A. (B), The British Oxygen Co., Thames House, Millbank, London, S. W. 1, England.

Stepleton, P. W. (D), Welder, G. E. Christopher Iron Works; (Res.) 3033 Wellington Place, Wichita, Kansas.

Sternke, Richard W. (C), Shop Supt., Lakeside Bridge & Steel Co., 3200 W. Villard Ave., Milwaukee, Wis.

Stevens, A. C. (D), Section 1, Bldg. 52, General Electric Co., Schenectady, N. Y.

Stevens, Fred (D), Welder, Big Three Welding & Equip. Co., M. & M. Building, Houston, Texas.

Stevens, F. E. (C), Asst. Foreman, Jeffrey Mfg. Co.; (Res.) 206 N. Harris Ave., Columbus, Ohio.

Stevens, Harry (D), Electric Welder, U. S. Navy Yard; (Res.) 97B Smith St., Charleston, S. C.

Stevens, James S. (D), Supervisor, Air Reduction Sales Co., Shreveport, La.

Stevenson, D. B. (D), Maintenance Welder, American Brass Co., Ansonia, Conn.

Stevenson, Francis H. (C), Welding Supervisor, Lockheed Aircraft Corp.; (Res.) 571 South St., Glendale, Calif.

Steward, Douglas P. (B), Chief Engineer, Lorain Division, Carnegie-Illinois Steel Corp., 545 Central Ave., Johnstown, Pa.

Steward, Harry M. (B), Supt. of Maintenance, Boston Elevated Railway, 31 St. James Ave., Boston, Mass.

Stewart, C. (D), Welding Foreman, R. L. Spitzley Htg. Co., 96 Elm St., River Rouge, Mich.

Stewart, George (D), Welder, Cities Service; (Res.) 511 North Palm, Ponca City, Okla.

Stewart, H. M. (B), Engineer, Humble Oil & Refining Co., Baytown Refinery, Baytown, Texas.

Stewart, J. R. (B), Engineer, Canadian Liquid Air Co., Ltd., 1111 Beaver Hall Hill, Montreal, Quebec, Canada.

Stewart, L. L. (C), Salesman, Air Reduction Sales Co., Box 2457, Oklahoma City, Okla.

Stewart, R. W. (C), Draftsman, Applied Engrg. Dept., Air Reduction Sales Co., 60 E. 42nd St., New York City.

Stewart, S. A. (C), Shop Inspector, Tulsa Boiler & Mach. Corp.; (Res.) 2639 E. 14th Place, Tulsa, Okla.

Stewart, S. S. (B), Plant Manager, Wilson Welder & Metals Co., N. Bergen, N. J.

Stewart, W. E. (B), Chief Engineer, S. N. Clarkson Assoc., Inc., 420 Lexington Ave., New York, N. Y.

Stewart, W. F. (C), Welder, Van Dorn Iron Works; (Res.) 23308 Alexander Road, N. Olmstead, Ohio.

Stewart, W. S. (C), District Manager, The Lincoln Electric Co., Cleveland, Ohio.

Stibbs, Edward G. (B), Safety Inspection Engineer, Trinidad Leaseholds, Ltd., Pointe-A-Pierre, Trinidad, B. W. 1, British West Indies.

Stiglmeier, Albert F. (B), Secretary-Treasurer, Master Boiler Maker's Association, General Boiler Dept. Foreman, New York Central Railroad; (Res.) 29 Parkwood St., Albany, N. Y.

Stillman, Ralph K. (D), 126—5th Ave., Brooklyn, N. Y.

Stine, Wilmer E. (C), Electrical Engineer, Bureau of Const. & Repair, Room 2122, Navy Dept., Washington, D. C.

Stitt, J. R. (B), Asst. Prof. (Welding Engineering), Industrial Engrg. Dept., Ohio State University, Columbus, Ohio.

St. John, L. R. (B), Civil Engineer, Kansas City Highway Comm., Court House, Kansas City, Kansas.

Stocker, C. L. (C), Salesman, Lincoln Electric Co., 812 Mateo St., Los Angeles, Calif.

Stockholm, Seth R. (D), Manager, Troy Electric Welding Co., Inc., 145 River St., Troy, N. Y.

Stocking, Kenneth O. (C), Junior Engineer, U. S. Engineer Office, 1709 Jackson St., Omaha, Nebr.

Stockstill, Prentiss G. (D), Welder, Freeport Sulphur Co., Port Sulphur, La.

Stoffregen, Karl (C), Welding Engineer, Electric Arc Cutting & Welding Co.; (Res.) 838 No. 13th St., Milwaukee, Wis.

Stolba, Edward F. (B), Chief Engineer, New England Dept., The Fidelity and Casualty Co. of N. Y., Room 308—10 Broad St., Boston, Mass.

Stoller, Ed. (D), Welding Operator, R. G. Le Tourneau, Inc.; (Res.) 619 W. Callendar Ave., Peoria, Ill.

Stone, Edward (C), District Sales Manager, National Cylinder Gas; (Res.) 17219 Ilene Ave., Detroit, Mich.

Stoner, J. D. (F), Box 627, Monroe, Mich.

Stoody, Winston F. (A), President, Stoody Co., P. O. Box 429, Whittier, Calif.

Storrs, H. A. (C), Box 114, Mineral Wells, Texas.

Storts, P. O. (D), Welding Hanna Essex Coal Co.; (Res.) Hemlock, Ohio.

Storz, Melvin (D), 2622 Orchard St., Blue Island, Ill.

Stoudt, John M. (B), Design Equipment and Write Specifications, Division of Highway Maintenance City of Cincinnati; (Res.) 136 W. McMillan St., Cincinnati, Ohio.

Stout, Arthur (B), Estimator Todd-Johnson Dry Docks, Inc., P. O. Box 239, New Orleans, La.

Stover, C. R. (B), Welding Foreman, E. B. Badger & Sons; (Res.) Elkview, W. Va.

Strandell, John H. (C), Assoc. Engineer (Diesel), Bureau of Engrg., Navy Dept., Washington, D. C.

Strathdee, Wallace B. (B), Welding Supervisor. Westinghouse Electric & Mfg. Co., 10 High St., Boston, Mass.

Strauss, Jerome (B), Vice-President, Vanadium Corp. of Amer., 420 Lexington Ave., New York City.

Straw, Gilbert (D), Mechanic Helper, Hugh S. Jewett; (Res.) 414 Arwin, Calif.

Strayer, R. K. (B), Sales Manager, Lancaster Iron Works, Inc.; (Res.) 236 E. King St., Lancaster, Pa.

Strecker, Rudy (C), Owner, Peerless Welding Co., 265 10th St., San Francisco, Calif.

Streeter, J. Reel (B), Civil Engineer, Black Sivalls & Bryson, Inc.; (Res.) 4510 Lloyd Ave., Kansas City, Kansas.

Streett, James D. (C), Sales, Granite City Steel Co., 1804 Boatmens Bank Bldg., St. Louis, Mo.

Stricklen, E. E. (D), 7743 Coles Ave., Chicago, Ill.

Strine, Howard L. (D), Welder, York Ice Machinery Co.; (Res.) R. D. 1, York, Pa.

Stringham, Keever L. (C), Electrical Engineer, Lincoln Electric Co., 12818 Colt Road, Cleveland, Ohio.

Strissel, Daniel (D), Welder, U. S. E. D.; (Res.) Box 546, Fort Peck, Mont.

Strong, K. B. (C), Welding Engineer, % Chicago Hardware Fdy. Co., North Chicago, Ill.

Stroup, W. M. (D), Mechanic, Okla. Gas & Electric Co.; (Res.) 1112 N. W. 50th St., Oklahoma City, Okla.

Stuckey, W. A. (B), Supt. of Welding, Miss. Valley Struc. Steel; (Res.) 25th & Norwood, Melrose Park, Ill.

Stuebing, A. F. (C), Railroad Mechanical Engineer, Carnegie-Illinois Steel Corp., Carnegie Bldg., Pittsburgh, Pa.

Stulen, F. B. (B), Assistant Chief Engineer, Propeller Division, Pittsburgh Screw & Bolt Corp.; (Res.) 26 N. Howard St., Bellevue, Pa.

Stupp, Norman (C), Chief Engineer & Treasurer, Stupp Bros. Bridge & Iron Co., 3800 Weber Road, St. Louis County, Mo.

Sturgeon, W. C. (B), Supt. of Tank Shop, Elliott Co., Jeannette, Pa.

Sturgess, Robert K. (C), Materials Engineer, Standard Oil Co. of California, 225 Bush St., San Francisco, Calif.

Stuver, E. W. (C), Instructor, Jourden Diesel Schools, Inc.; (Res.) 235 So. Negley Ave., Pittsburgh, Pa.

Sugg, L. D. (B), Supervisor, Applied Engineering Dept., Magnolia Airco Prods. Co., P. O. Box 319, Houston, Texas.

Sullins, C. V. (B), Welder, % Standard Oil Co. of Venezuela, Caripito, Venezuela.

Sullivan, John F., Jr. (C), Supt. Struct. & Mech. Div., Commonwealth Edison Co., 72 W. Adams St., Chicago, Ill.

Surguy, H. L. (D), Welder, Gearench Mfg. Co.; (Res.) 6847 Avenue F, Houston, Texas.

Sussman, H. (B), Naval Architect, Navy Yard; (Res.) 2064 E. 2nd St., Brooklyn, N. Y.

Sutter, John (D), Welder, Sordoni Cons. Co.; (Res.) 188 Simpson St., Swoyersville, Pa.

Svendsen, Einar (B), Mechanical Engineer, F. L. Smidth & Co., 225 Broadway, New York City.

Swader, Claude (B), Welder, Chas. C. Steward Machine Co., 8432—8th Ave., North, Birmingham, Ala.

Swaffar, Howard J. (D), Welder, Black Sivalls & Bryson; (Res.) 4200 E. 9th St., Kansas City, Mo.

Swain, Philip W. (B), Editor of "Power," McGraw-Hill Pub. Co., 330 W. 42nd St., New York City.

Swan, Harry S. (C), Welding Engineer, John A. Roeblings Sons Co., 640 So. Broad St., Trenton, N. J.

Swanson, George (C), St. Paul Welding & Mfg. Co., 292 Walnut St., St. Paul, Minn.

Swanson, Russell (D), 121 Rangeway Road, North Billerica, Mass.

Swart, George (C), Instructor in Welding, Milwaukee School of Engrg.; (Res.) R. #7, Janesville, Wis.

Sweet, Herbert A. (B), Construction Engineer, Associated Factory Mutual Fire Insurance Co., 184 High St., Boston, Mass.

Sweesy, F. D. (D), 166 Chicago Ave., Youngstown, Ohio.

Swenson, D. A. (B), Western Iron & Fdy. Co., 702 E. 2nd St., Wichita, Kansas.

Swift, Alden W. (B), Service Engineer, Handy & Harman, 20 N. Wacker Drive, Chicago, Ill.

Swift, Clinton E. (C), Welding Engineer, C. F. Braun and Co., Alhambra, Calif.

Swift, W. C. (C), Consulting Engineer, Compressed Industrial Gases, Inc.; (Res.) West Alexandria, Ohio.

Swim, E. H. (C), Supervisor of Welding, B. F. Sturtevant Co.; (Res.) 11 Wachusett St., Mattapan, Mass.

Switzer, DeVere (C), Engineering Dept., Air Reduction Sales Co., Cleveland, Ohio; (Res.) Firestone Pk. Station Box 2683, South Akron, Ohio.

Swope, Robert B. (B), President, Southern Oxygen Co., Arlington, Va.

Sworski, Stanley F. (D), Welder, Brown Steel Tank Co.; (Res.) 618 Huron St., S. E., Minneapolis, Minn.

Sykes, George (B), General Publicity Dept., Union Carbide Co., 30 East 42nd St., New York, N. Y.

Sylar, Bert L. (C), Sales Representative; (Res.) 2501 Chamberlain Ave., Chattanooga, Tenn.

Sylvester, Walter G. (C), Research Engineer, Air Reduction Sales Co.; (Res.) 379 Bergen Ave., Jersey City, N. J.

T

Taber, Clifford W. (C), Welding Inspector, Metropolitan Water Dist. of So. Calif.; (Res.) 185 So. Oak St., Pasadena, Calif.

Tadd, R. W. (B), Manager, Welding Divisions, Ducommun Metals & Supply Co., Box 310, Arcade Station, Los Angeles, Calif.

Tadlock, J. H. (B), Seattle Branch Manager, J. E. Haseltine & Co., 440 Holgate St., Seattle, Wash.

Taft, Harold G. (D), Welding Foreman, Draper Corp.; (Res.) 41 Dutcher St., Hopedale, Mass.

Talmadge, W. Heath (B), Sales Manager, Barde Steel Co., 2709 Utah Ave., Seattle, Wash.

Tam, W. (C), Supt., Chicago Bridge & Iron Co.; (Res.) 75 Harrison St., Greenville, Pa.

Tangeman, Chas. L. (C), Eng. Maint. Dept., The Cincinnati Milling Machine Co.; (Res.) 3030 Griest Ave., Cincinnati, Ohio.

Tangerman, E. J. (B), Asst. Manager, American Machinist, McGraw-Hill Pub. Co., 330 W. 42nd St., New York, N. Y.

Tanner, Frank T. (D), 888 Folsom St., San Francisco, Calif.

Tapp, George F. (D), Instructor in Welding, Wentworth Institute, Welding Dept., Boston, Mass.

Tapparo, John A. (B), Plant Manager, The National Radiator Co., Lebanon, Pa.

Tarbell, James H. (B), Industrial Engineer, Kansas City Power & Light Co., 1330 Baltimore, Kansas City, Mo.

Tarbell, Raymond P. (C), Una Welding, Inc., 1615 Collamer Road, E. Cleveland, Ohio.

Tarrier, Fred A. (B), President, The Tarrier Steel Co., 762 Hanford St., Columbus, Ohio.

Taylerson, John E. (C), Engineering Assistant, Federal Shipbuilding & Dry Dock Co., Kearny, N. J.; (Res.) 77 Woodward Ave., Rutherford, N. J.

Taylor, Albert A. (D), Welding Foreman, Ohio Fuel Gas Co.; (Res.) 606½ W. Gambier St., Mt. Vernon, Ohio.

Taylor, A. E. (D), Research Engineer, American Rolling Mill Co.; (Res.) Box 168, West Middletown, Ohio.

Taylor, Clarence M. (C), Vice-President in Charge of Sales, The Lincoln Electric Co., Brookwood Dr., South Euclid, Ohio.

Taylor, E. W. (B), Vice-President, Industrial Brownhoist Corporation; (Res.) 135 Washington St., Bay City, Mich.

Taylor, G. R. (D), 38 Crescent St., Lyman, S. C.

Taylor, James E. (D), Welder, Black & Decker Co., Towson, Md.

Taylor, John F. (B), Foreman Welder, Bethlehem Shipbldg. Corp., Ltd., 20th & Illinois Sts., San Francisco, Calif.

Taylor, J. Hall (C), President, Taylor Forge & Pipe Works, Box 485, Chicago, Ill.

Taylor, J. T. (D), Welder, Ajax Iron Works, 1709 Blake St., Denver, Colo.

Taylor, S. H. (C), Western Manager, Lincoln Electric Co., 866 Folsom St., San Francisco, Calif.

Taylor, W. L. (D), Blacksmith Welder, Firestone Tire & Rubber Co.; (Res.) 730 Ines, Memphis, Tenn.

Tebben, J. D. (A), Sales Manager, Metallurgical Division, P. R. Mallory & Co., 3029 E. Washington St., Indianapolis, Ind.

Teeple, Ralph J. (C), District Sales Manager, Page Steel & Wire Division of American Chain & Cable Company, Inc., 1011 Healey Bldg., Atlanta, Georgia.

Telfer, Wm. (C), Instructor, University of Michigan; (Res.) 3111 Washtenaw Road, Ann Arbor, Mich.

Telford, George W. (D), Welder, Trailer Co. of America; (Res.) 6882 Esther Lane, Madeira, Ohio.

Tenney, G. E. (C), District Manager, The Lincoln Electric Co., 600 W. 41st St., Chicago, Ill.

Tepley, George L. (B), Salesman, Eagle Metals Co., 3628 East Marginal Way, Seattle, Wash.

Thames, R. M. (D), Welder, Mid-Continent Petroleum Co.; (Res.) R. R. #9, Tulsa, Okla.

Thayer, Frank L. (C), 120 Central St., Athol, Mass.

Theisinger, W. Gordon (C), Weld. & Met. Engineer, Lukens Steel Co.; (Res.) 1204 Walnut St., Coatesville, Pa.

Theriault, M. (D), 93 Commercial St., Timmins, Ont., Canada.

Tholen, M. A. (C), Foreman, Link-Belt Co., 2410 W. 18th St., Chicago, Ill.

Thomas, Charles S. (C), Asst. Metallurgist, Jeffrey Mfg. Co.; (Res.) 3138 Indianola Ave., Columbus, Ohio.

Thomas, Jack R. (B), Salesman, Taylor-Winfield Corp.; (Res.) 4419 N. Wolcott Ave., Chicago, Ill.

Thomas, James (C), Arc and Acetylene & Student of Welding, Fisher Body Pontiac Division; (Res.) 14056 Taust Ave., Detroit, Mich.

Thomas, Lester B. (C), Owner, Economy Welding Service, 1723 Cuninig St., Omaha, Nebr.

Thomas, R. D. (B), President, Arcos Corp., 401 N. Broad St., Philadelphia, Pa.

Thomas, R. David (C), Chief Engineer, Arcos Corp.; (Res.) 710 Braeburn Lane, Penn. Valley, Narbeth, Pa.

Thomas, R. H. (D), Tinner, Monsanto Chemical Co.; (Res.) 3965 Castleman Ave., St. Louis, Mo.

Thomas, Theodore G. (C), Architect, Engineering Dept., Commonwealth Edison; (Room) 828—72 W. Adams St., Chicago, Ill.

Thompson, Arthur C. (C), Supt., Boiler and Plate Shops, Koppers Co., Bartlett Hayward Division, 200 Scott St., Baltimore, Md.

Thompson, Bruce (D), Welder, Eastman Kodak Co.; (Res.) 128 Merrill St., Rochester, N. Y.

Thompson, F. J. (B), Welding Supervisor, National Tank Co., Box 1588, Tulsa, Okla.

Thompson, George F. (C), Engineer, Wellman Engrg. Co., 7000 Central Ave., Cleveland, Ohio.

Thompson, Harry T. (B), District Manager, Thermit Dept., Metal & Thermit Corp., 1514 North Ave., West, Pittsburgh, Pa.

Thompson, J. H. N. (B), John Thompson Water Tube Boilers Ltd., Ettingshall Works, Wolverhampton, England.

Thompson, James W. (D), Supervisor, Applied Engineering Dept., Air Reduction Sales Co., 940 Kenyon St., Cincinnati, Ohio.

Thompson, Marshall Hudson (C), Asst. Chief Engineer, Consolidated Goldfields Ltd., P. O. Box 1167, Johannesburg, South Africa.

Thompson, Moreton R. (B), Vice-President, Southern Counties Gas Co. of Calif., 810 So. Flower St., Los Angeles, Calif.

Thompson, N. W. (A), Shell Oil Co., 100 Bush St., San Francisco, Calif.

Thompson, Perry (C), 110 Cayton, Houston, Texas.

Thompson, Roland B. (C), Foreman, York Ice Machinery Co.; (Res.) 572 Penna. Ave., York, Pa.

Thompson, Thomas (D), Gas & Arc Welding, Jacob Ruppert Brewery; (Res.) 240-08 Weller Ave., Rosedale, L. I., N. Y.

Thomsen, S. Henry (C), 601—40th St., Brooklyn, N. Y.

Thomson, Alexander (B), Engineer, Pacific Car & Fdy. Co.; (Res.) 8409—55th Ave. So., Seattle, Wash.

Thomson, David (C), Welder, Bethlehem Shipbuilding Co., Inc.; (Res.) 302—58th St., Brooklyn, N. Y.

Thomson, Malcolm (C), Welding Engineer, General Electric Co., Lynn, Mass.

Thomson, W. L. (C), Designing Engineer, G. S. Blodgett Co., Inc., Burlington, Vt.

Thornberg, Al (D), Welder, R. G. Le Tourneau, Inc.; (Res.) 1815 N. University, Peoria, Ill.

Thornton, G. E. (C), Prof., State College, Washington, 210 Columbia, Pullman, Wash.

Thornton, Kent C. (C), Hull Design, The American Shipbldg. Co., Foot W. 54th St. N. W., Cleveland, Ohio.

Thow, Frank (C), The American Shipbldg. Co., Foot of West 54th St. N. W., Cleveland, Ohio.

Thrapp, George (C), Sales Manager, The Balbach Co., 1201-7 California St., Omaha, Nebr.

Throckmorton, Ralph E. (F), 355 W. Oak St., W. Lafayette, Ind.

Thum, August, Dr. (C), o. Professor und Vorstand der Materialprufüngsanstalt an der Techn. Hochschule Darmstadt (Hessen), Germany.

Thum, Ernest E. (C), American Society for Metals, 7016 Euclid Ave., Cleveland, Ohio.

Thumser, R. C. (C), Plant Engineer, Monsanto Chemical Co.; (Res.) 3944 Fillmore St., St. Louis, Mo.

Tibbenham, Louis John (C), Managing Director, Suffolk Iron Foundry, Stowmarket, England.

Tibbets, R. J. (C), Asst. General Supt., Sinclair Refining Co., Pipe Line Dept., 1412 Commerce Bldg., Kansas City, Mo.

Tickle, A. B., Jr. (B), Vice-President, Arthur Tickle Engineering Works, Inc., 21 Delevan St., Brooklyn, N. Y.

Tierney, J. D. (B), President, Storts Welding Co., Inc., 42 Stone St., Meriden, Conn.

Tiffin, Wm. T. (B), Instructor, University of Oklahoma, College of Engineering, Norman, Okla.

Timmis, Pierce (C), Service Equip. Engineer, United Engrs. & Const. Co., Inc., 1401 Arch St., Philadelphia, Pa.

Timmons, J. J. (B), Supervising Engineer, Fidelity & Casualty Co., 810 Arrott Bldg., Pittsburgh, Pa.

Tinker, G. H. (C), Bridge Engineer, The N. Y., Chicago & St. Louis R. R. Co., 926 Terminal Tower, Cleveland, Ohio.

Tinnon, John B. (B), Sales Manager, Metal & Thermit Corp., 120 Broadway, New York, N. Y.

Tinsley, C. R. (C), Chief Electrician, U. S. Pipe & Fdry. Co., Bessemer, Ala.

Tisza, E. E. (B), Mech. Engineer, Wilson Welder & Metals Co., Box 96, North Bergen, N. J.

Todd, Floyd (D), Welding Supervisor, Combustion Engineering Co.; (Res.) 4724 Winkleman Place, St. Louis, Mo.

Tolan, Richard L. (D), 8124 Crandon Ave., Chicago, Ill.

Tolman, Lee P. (C), 104 Pembroke Pl., Kew Gardens, N. Y.

Tomczyk, Joseph (D), Welder, Pioneer Eng. Works; (Res.) 3327 Pierce St. N. E., Minneapolis, Minn.

Tomlinson, P. H. (C), Metallurgist, Farrel Birmingham Co.; (Res.) 231 Westfield Ave., Ansonia, Conn.

Tomlinson, Thos. E. (C), Inspector, Southwestern Laboratories, P. O. Box 1379, Fort Worth, Texas.

Toner, H. J. (C), Welder, Bureau of Engineering, Navy Department; (Res.) 8508 Cedar St., Silver Springs, Md.

Toof, Herman L. (C), Draftsman, Providence Steel & Iron Co., Providence, R. I.

Torbjornsen, E. (B), Managing Director, N. A. Gasaccumulator, Chr. Augustsgt, 7B, Oslo, Norway.

Toro, August (C), Operator, Rains Transportation Co.; (Res.) Box 291, Pinedale, Wyo.

Totten, J. M. (B), Supervising Engineer, The Fidelity & Casualty Co. of N. Y., 10 Pryor St. Bldg., Atlanta, Ga.

Toulmin, H. A., Jr. (C) Patent Attorney, Toulmin & Toulmin, Mutual Home Bldg., Dayton, Ohio.

Townsend, A. J. (B), Mechanical Engineer, Lima Locomotive Works, Inc., Lima, Ohio.

Townsend, J. R. (A), Materials Engineer, Bell Telephone Labs., 463 West St., New York, N. Y.

Townsend, R. L. (C), Sales Representative, Phillips & Easton Supply Co.; (Res.) 829 S. Madison, Wichita, Kans.

Townsend, W. M. (B), Works Manager, Montreal Locomotive Works Ltd., 215 St. James St., W. Montreal, Canada.

Trainer, S. W. (B), Sales Engineer, Harnischfeger Corp., 1385 Broad St., Station Bldg., Philadelphia, Pa.

Trammell, R. H. (C), Manager, Fulton Supply Co., Atlanta, Ga.

Trexel, C. A. (C), Commander Civil Engineer Corps., U. S. Navy, Bureau of Yards & Docks, Navy Dept., Washington, D. C.

Tritchler, W. C. (C), Salesman, Smith Welding Equip. Corp., 2633 4th St., S. E., Minneapolis, Minn.

Troger, H. J., Jr. (C), Asst. to General Supt., Federal Shipbuilding; (Res.) R. F. D. 1, New Brunswick, N. J.

Troike, Otto (D), Maintenance Welder, Commercial Solvents Corp.; (Res.) 508 N. Glen Oak Ave., Peoria, Ill.

Tross, Ernest (B), Plant Manager, United Eng. & Fdy. Co., Youngstown, Ohio.

Troutman, Lester L. (D), Welder, Maintenance Dept., Gates Rubber Co.; (Res.) 3038 W. Gill Pl., Denver, Colo.

Trueblood, Richard O. (B), Instructor in Mech. & Elec. Engineering, University of Wyoming; (Res.) 505A So. 10th St., Laramie, Wyo.

Tsuruta, Akira (C), 891 3-Chome Matsubara-Cho, Setagaya-Ku, Tokyo-Shi, Japan.

Tucker, Norman Albert (C), Welding Engineer, The B. T. H. Co. Ltd.; (Res.) Westgate, Westgate Road, Rugby, England.

Tuke, Harry A. (C), Regional Manager Truscon Steel Co., 700 Investment Bldg., Washington, D. C.

Tullis, H. T. (C), 1101 E. Anahein, Long Beach, Calif.

Tunis, Richard (C), Chief Engineer, Sun Oil Co., Marcus Hook, Pa.

Tuomala, J. Evald (D), Cutter & Welder, Butler Bros. Mining Co.; (Res.) Box 695, Nashwauk, Minn.

Turall, Maurice W. (D), Cuba, Ill.

Turk, Earl E. (C), Salesman, National Cylinder Gas Co.; (Res.) 3223 W. 116 St., Cleveland, Ohio.

Turner, Clyde (D), 23 West 109th Place, 2nd Floor, Roseland Sta., Chicago, Ill.

Turner, C. A. (C), Lewis Institute, Chicago, Ill.

Turner, Edward G. (D), Welder & Maintenance, Ray & Son, Inc., 1023 No. Carolina St., Louisiana, Mo.

Turner, M. H. (C), Secretary, Home Oxygen Co. Inc., Box 349, Huntington, Park, Calif.

Tweed, R. (B), Works Manager, Braithwaite & Co., Ltd., P. O. Box 427, Calcutta, India.

Tysick, J. E. (D), Welder, American Laundry Machine Co.; (Res.) 15 Otis St., Rochester, N. Y.

U

Uhl, Ray (C), Metallurgist, Henry Disston & Sons, Inc., Philadelphia, Pa.

Ujiiye, T. (C), Kobe Zosensho, Mitsubishi Jukogyo Kabushiki, Kaisha Kobe, Japan.

Ullmer, Herman (B), Manager, Process Service, The Linde Air Products Co., 30 East 42nd St., New York, N. Y.

Ulrich, Austin (C), 508 N. Perry, Peoria, Ill.

Underwood, B. O. (C), Chief Engineer, The Boardman Co., Oklahoma City, Okla.

Underwood, C. M. (B), Senior Welding Engineer, U. S. Naval Gun Factory; (Res.) 17 Westwood Drive, Westmoreland Hills, Washington, D. C.

Unger, Arthur M. (A), Welding Engineer, Pullman-Standard Car Mfg. Co., 11001 S. Cottage Grove Ave., Chicago, Ill.

Unger, Magnus (C), Electrical Engineer, General Electric Co.; (Res.) 51 McKinley Terrace, Pittsfield, Mass.

Urbach, L. (D), 1083 Hopkinson Ave., Brooklyn, N. Y.

Urquhart, Thorton (C), Chief Engineer, Taylor-Winfield Corp.; (Res.) 19151 Carman, Detroit, Mich.

Ursem, Earl (D), Welder, American Manganese Steel Co., 5700—8th Ave., Los Angeles, Calif.

V

Valdes, Joseph (D), Company E, Governors Island, New York, N. Y.

Valenta, G. (D), Welder, Western Electric Co.; (Res.) 5851 Elm Ave., Berkley, Ill.

Valukas, Albert (D), Welder, American Brass Co.; (Res.) 235 Congress Ave., Waterbury, Conn.

Van Aernem, Ralph E. (D), Welder, American Locomotive Co.; (Res.) Ballston Spa, R. D. 3, New York.

Van Alstyne, G. (C), Advertising Manager, Air Reduction Sales Co., 60 East 42nd St., New York, N. Y.

Van Buren, Miles H. (B), Instructor in Civil Engineering, Cooper Institute of Tech.; (Res.) 480 Clinton Ave., Brooklyn, N. Y.

Van Craen, Alphonse (C), Managing Director, Electro-Soudure Thermarc S. A.; (Res.) 7 Rue G. Gillekens, Vilvorde, Belgium.

Vanderbeek, H. A. (C), Asst. Engineer, American Bridge Co., 71 Broadway, New York, N. Y.

Van Dreser, M. J. (C), Welding Engineer, McKay Co.; (Res.) Elm Terrace Apts., York, Pa.

Van Dyke, E. E. (C), Welder, Sommers Products Co.; (Res.) R. R. 5, Peoria, Ill.

Van Epps, W. E. (C), Genl. St. Foreman, Rochester Gas & Elec. Corp.; (Res.) 83 Macbeth St., Rochester, N. Y.

Van Gorder, Karl (D), Electric Welder, Crane Co.; (Res.) 2617 Walnut St., Blue Island, Ill.

Van Horn, V. C. (D), Maize Garage, Box 145, Maize, Kansas.

Van Iten, M. (D), Welder, Whiting Corp.; (Res.) 15712 Halsted St., Harvey, Ill.

Van Kirk, R. W. (C), Sales Engineer, General Electric Co., 1801 N. Lamar, Dallas, Texas.

Van Leuven, E. P. (D), Teacher, Kern County Union High School & J. C.; (Res.) 2716—21st St., Bakersfield, Calif.

Van Ormer, Lawson (C), Inspection Supervisor, Metallurgical Dept., Jones & Laughlin Steel Corp., Aliquippa, Pa.

Van Ouwerkerk, L. (C), Manager, The Röntgen Tech. Service, 217 Ryksweg, Overschie, The Netherlands.

Vansant, Elmer B. (A), Shop Supt., Badenhausen Corp., Cornwells Hghts, Bucks Co., Pa.

Van Wart, Walter B. (B), Vice-President, Wyatt Metal & Boiler Works, Dallas, Texas.

Vaughen, R. A. (C), Owner, Welders Supply Co.; (Res.) 706 W. Niscarawas St., Canton, Ohio.

Vaughn, J. I. (D), Welder, Wm. C. Ellis & Son; (Res.) 1166 Englewood, Memphis, Tenn.

Veihl, E. E., Jr. (C), Asst. Engineer, General Steel Castings Corp., Eddystone, Pa.

Viberg, E. R. (C), Asst. Vice-President, Canadian Car & Fdy. Co., Ltd., P. O. Box 160, Montreal, Canada.

Vick, A. C. (C), Asst. Supt., Stevens Metal Products Co.; (Res.) 427 Central Parkway, S. E., Warren, Ohio.

Vickery, J. F. (C), Welding Specialist, The General Electric Co., 4966 Woodland Ave., Cleveland, Ohio.

Viohl, Herbert (B), Chief Engineer, Benjamin F. Shaw Co., 2nd & Lombard St., Wilmington, Del.

Vock, Louis G. (C), Travelling Engineer, Metal & Thermit Corp., 7300 S. Chicago Ave., Chicago, Ill.

Vogel, Andrew (B), Engineer, General Electric Co.; (Res.) 1821 Lenox Road, Schenectady, N. Y.

Vogel, Carl S. (B), Welding Engineer, Edgcomb Steel Co., D Erie, Philadelphia, Pa.

Vogel, Chas. (D), Welder & Burner, Delco Radio, Div. of General Motors Corp.; (Res.) R. R. #1, Alexandria, Indiana.

Voldrich, C. B. (C), Associate Materials Engineer, Newport News Shipbuilding and Dry Dock Co., Newport News, Va.

Vollmer, Henry G. (B), Engineer, Gate City Iron Works, 1602 No. 11th St., Omaha, Nebr.

Volpert, Wm. (B), Proprietor, Volpert Welding Shop, 28 W. 2nd, Peru, Ind.

Volz, Fred O. (C), Sales Engineer, Lakeside Bridge & Steel Co.; (Res.) 6812 Hillcrest Drive, Wanwatosa, Wis.

Vom Steeg, E., Jr. (B), Welding Specialist, General Electric Co., 570 Lexington Ave., New York City.

Von Thaden, Herbert (B), Sales Engineer, Carnegie-Illinois Steel Corp.; (Res.) 280 Parker Drive, Pittsburgh, Pa.

Voss, Otto C. (B), Supt., Allis-Chalmers Co., 1226 W. Wiscave, Milwaukee, Wis.

Vreeland, John (B), Metallurgical Engineer, Chase Brass & Copper Co. Inc., Waterbury, Conn.

Vrooman, A. J. (C), Salesman, Air Reduction Sales Co., Wichita, Kansas.

Vrooman, G. C. (C), Detroit Edison Co., 2000 Second Ave., Detroit, Mich.

Vuchnich, M. N. (B), Welding Engineer, The Lincoln Electric Co., P. O. Box 8031, Johannesburg, S. A.

W

Wagaman, C. B. (D), Welding Operator, Frick Co., 714 Cleveland Ave., Chambersburg, Pa.

Wagner, Albert H. (C), Welding Instructor, Sun Ship & Dry Dock Co.; (Res.) 335 E. 12th St., Chester, Pa.

Wagner, Carl A. (C), Junior Metallurgist, Wilson Welder & Metals Co., North Bergen, N. J.; (Res.) 3514—114th St., Richmond Hill, N. Y.

Wagner, Gerhard (C), Consulting Engineer, 233 Broadway, New York, N. Y.

Wagner, William S. (C), Plant Engineer, Philadelphia Coke Co., 4501 Richmond St., Philadelphia, Pa.

Wagstaff, John P. (C), Structural Engineer, Bureau of Yards & Docks; (Res.) 1915 No. Danville St., Arlington, Va.

Walbridge, Robert P. (D), X-ray Technician, Lakeside Bridge Steel Co.; (Res.) 5425 No. 39th St., Milwaukee, Wis.

Walcott, William Daniel (C), Inspecting Engineer, Hydro Electric Power; (Res.) 620 University Ave., Toronto, Canada.

Waldie, Robert C. (A), Manager, Welding Dept., Williams & Co., Inc., 901 Penna. Ave., Pittsburgh, Pa.

Waldman, R. O. (C), Western District Manager, Chicago Steel & Wire Co., 553 Howard St., San Francisco, Calif.

Wales, C. Clark (B), General Manager Hamilton Bridge Co., 231 Bay St., N. Hamilton, Ontario, Canada.

Walker, George (B), Welding Supervisor, American Locomotive Co., 100 Orchard St., Auburn, N. Y.

Walker, Henry O. (B), Chief Engineer, Hammond Iron Works, Warren, Pa.

Walker, J. P. (C), President, National Tank Co., Box 1588, Tulsa, Okla.

Walker, K. (C), Foreman, Welding Dept., Bucyrus-Erie Co.; (Res.) 3132 So. Kinnickinnic Ave., Milwaukee, Wis.

Walker, R. W. (D), Welder, Western Electric Co.; (Res.) 4639 W. 19th St., Cicero, Ill.

Wall, A. S. (B), Engineer, Dominion Bridge Co., P. O. Box 280, Montreal, Canada.

Wall, Marion P. (B), Chief Engineer, Hedges-Walsh-Weidner Div., Combustion Engrg. Co., Inc., Chattanooga, Tenn.

Wallace, H. C. (C), Asst. Sales Manager, Air Reduction Sales Co.; Rt. 2, Box 103, Louisville, Ky.

Wallace, L. A. (C), Research Engineer, Atlantic Steel Co., P. O. Box 1714, Atlanta, Georgia.

Wallace, Robert M. (B), Assistant Supt., The Griscom-Russell Co., Massillon, Ohio.

Wallace, Walter P. (B), Jr. Metallurgist, Columbia Steel Co., Torrance, Calif.

Wallis, Jess (C), Welding Operator, R. G. Le Tourneau, Inc., 201 Le Tourneau Court, Peoria, Ill.

Walls, Charles (D), The Whiting Corp., Harvey, Ill.

Walsh, H. I. (D), Serviceman, Applied Engineering Dept., Air Reduction Sales Co.; (Res.) 621 Shenandoah St., Waterview, Portsmouth, Va.

Walter, C. S., Jr. (C), Shop Foreman, Belmont Iron Works, Royersford, Pa.

Walz, Gottlieb (D), Welding, Lakeside Bridge Steel Co.; (Res.) 2325 N. 65th St., Wauwatosa, Wis.

Wang, Ray H. (C), Erecting Engineer, The Superheater Co., East Chicago, Ind.

Ward, L. E. (D), Operator, Wm. A. Pope Co.; (Res.) 7152 Cornell Ave., Chicago, Ill.

Ward, N. F. (B), Associate Prof. of M. E., University of California, 232 Mechanics Bldg., Berkeley, Calif.

Ward, R. E. (B), 777 E. Washington Blvd., Los Angeles, Calif.

Ward, R. E. (D), Electric Welder, Mathieson Alkali Wks.; (Res.) McCrady, Va.

Ward, W. E. (B), Chief Draftsman, Clearing Machine Corp.; (Res.) 1132 Hollycourt, Oak Park, Ill.

Warfel, J. A. (C), Special Representative, Air Reduction Sales Co., 1116 Ridge Ave., N. S., Pittsburgh, Pa.

Warmington, Thomas J. (B), Plant Manager, Wm. Bros. Boiler & Mfg. Co., Nicollet Island, Minneapolis, Minn.

Warneka, Lloyd D. (C), 921 Taylor Ave., Scranton, Pa.

Warner, William L. (C), Assoc. Metallurgical Eng. of Welding Materials, Watertown Arsenal, Watertown, Mass.

Warnke, Eldon (C), Foreman, R. G. Le Tourneau, Inc.; (Res.) 129 Pierson St., E. Peoria, Ill.

Warriner, Reuel E. (B), Engineer, International Nickel Co., 915 Snoreham Bldg., Washington, D. C.

Wasson, John W. (B), Sales Engineer, The Von Hamm-Young Co., Ltd., P. O. Box 2630, Honolulu, T. H.

Watkins, Franklin (B), Foreman, R. G. Le Tourneau, Inc.; (Res.) 509 Hayword, Peoria, Ill.

Watkins, Harry (C), Foreman, R. G. Le Tourneau, Inc.; (Res.) 509 Hayword St., Peoria, Ill.

Watson, C. G. (B), President, Youngstown Welding & Engrg. Co., Youngstown, Ohio.

Watson, Chas. O. (B), Salesman, Taylor-Winfield Corp., 1225 West Washington Blvd., Chicago, Ill.

Watson, G. G. (C), Engineer, Bur. Power & Light, 508 S. Van Ness Ave., Los Angeles, Calif.

Watson, G. V. (B), President, Flour City Welding Co., 2937 Third Ave. So., Minneapolis, Minn.

Watt, J. R., Jr. (C), Salesman, Air Reduction Sales Co., Louisville, Ky.

Waugh, J. E. (C), Welding Engineering Dept., General Electric Co.; (Res.) 37 Imperial Ave., Cohoes, N. Y.

Weaber, J. A., Jr. (C), General Manager, Union Boiler & Mfg. Co.; (Res.) 301 N. 4th St., Lebanon, Ohio.

Webb, Eugene (C), Chief Inspector, Hartford Steam Boiler Insp. and Ins. Co., 617 Security Bldg., St. Louis, Mo.

Webb, W. W. (A), Plant Supt., Benjamin F. Shaw Co., Wilmington, Del.

Webber, W. C. (C), Sr. Quarterman Boilermaker, U. S. Naval Gun Factory, Shop 6, Washington, D. C.

Weber, Oscar E. (B), Chief Draftsman, S. Morgan Smith Co., Lincoln & Hartley St., York, Pa.

Weekes, James H. (C), Salesman, Union Carbide Co.; (Res.) 125 W. Main St., Oyster Bay, N. Y.

Weeks, M. (B), Shop Foreman, Wyatt Metal & Boiler Works, Box 3052, Houston, Texas.

Wegner, Russell (C), Welding Operator, Sommer Products Co.; (Res.) 1016 Sheridan St., Peoria, Ill.

Wehmeyer, Clarence W. (C), Chief Engineer, Heine Boiler Division, Combustion Engineering Co., 5319 Shreve Ave., St. Louis, Mo.

Wehr, Wm. G. (B), General Supt., Cleveland Crane & Engineering Co., Wickliffe, Ohio.

Weidler, Ward (C), Supt., Atlas Car & Mfg. Co., 1120 Ivanhoe Road, Cleveland, Ohio.

Weigel, A. C. (B), Assistant to the President, Combustion Engineering Co., Inc., 200 Madison Avenue, New York City.

Weigel, John H. (C), Welding Engineer, Combustion Engrg. Co.; (Res.) 117 Ochs Highway, Chattanooga, Tenn.

Weiger, Joseph A. (B), Vice-President, P. R. Mallory & Co., 3029 E. Washington St., Indianapolis, Ind.

Weihe, Cal. (C), Hudson Motor Car Co., Gratiot Plant, Detroit, Mich.

Weil, E. H. (D), Salesman, Lincoln Electric Co., 812 Mateo St., Los Angeles, Calif.

Weiler, Ernest (D), Welder, Baker Oil Tool Co., 6925 San Luis, Bell, Calif.

Weiler, H. G. (B), Owner, Weiler Welding Co., 218 E. Second St., Dayton, Ohio.

Weinberg, Philip H. (A), Sales Engineer, Philadelphia, Electric Co., 1000 Chestnut St., Philadelphia, Pa.

Weinberger, Ed. (B), Factory Representative, Marquette Manufacturing Co.; (Res.) 410 Pearl St., Denver, Colo.

Weinert G. (C), Designer, Federal Shipbuilding & Dry Dock Co.; (Res.) 548 Day Ave., Ridgefield, N. J.

Weinman, Rudolph A. (B), Associate Engineer, United States Bureau of Reclamation, Coulee Dam, Wash.

Weir, R. E. (C), Vice-President, Natl. Metal Processing Co.; (Res.) Hotel Carter, Cleveland, Ohio.

Weisgerber, Les (C), Sales Representative, Commercial Gas Co.; (Res.) 3537—1st Ave. So., Minneapolis, Minn.

Weiss, George (D), Welder, Chrysler Motor Corp., 632 So. Eastman Ave., Los Angeles, Calif.

Weissenburger, C. O. (C), President, Marietta Mfg. Co., Point Pleasant, W. Va.

Weist, Milton M. (C), Manager, Air Reduction Sales Co., 3100 So. Homan Ave., Chicago, Ill.

Welch, E. T. (C), Welding Instructor, Chattanooga Vocational High School; (Res.) 215 Chestnut St., Chattanooga, Tenn.

Welch, H. L. (D), Time Study, Reed Roller Bit Co.; (Res.) 2512 White Oak Dr., Houston, Texas.

Welch, Jerome B. (B), Welding Engineer, Cutler-Hammer, Inc.; (Res.) 3273 N. 17th St., Milwaukee, Wis.

Welch, Loyd (B), Welding Inspector, Southwestern Laboratories, Box 1618, Dallas, Texas.

Welch, Lyman (D), Welding, Eastman Kodak Co., East St., Pittsford, N. Y.

Welcomb, C. D. (C), Supervisor, Applied Engrg. Dept., Air Reduction Sales Co., 7991 Hartwick St., Detroit, Mich.

Weldon, J. J. (C), Welding Supervisor, General Electric Co.; (Res.) 112 Cromwell Ave., Pittsfield, Mass.

Welke, Elmer (D), 20 De Jonge St., Rochester, N. Y.

Welker, Ted (D), Operator and Proprietor, Flexarc Welding Service, 103 Wall St., Belvidere, N. J.

Weller, R. C. (C), Sales Engineer, Buffalo Welding Supply Co., 37 E. Ferry St., Buffalo, N. Y.

Welling, Harry H. (D), Welder, Geo. Welding & Son; (Res.) 2105 Reading Road, Cincinnati, Ohio.

Wells, Cecil G. (B), Engineer, Secretary-Treasurer, National Tank Co., Box 1588, Tulsa, Okla.

Wells, Robert K. (C), Lt. Comdr. (CC), U. S. Navy Board of Inspection and Survey, Navy Dept., Washington, D. C.

Welter, Gustave (B), Vice-President, The Bigelow Co., Box 706, New Haven, Conn.

Wendelgass, Andrew J. (D), Gas Welder, Pfaudler Co.; (Res.) 431 Carter St., Rochester, N. Y.

Wentworth, Philip (D), Welder, Acme Wire & Iron Works; (Res.) 105 Heflin Ct., San Antonio, Texas.

Wescott, George A. (D), Welder, Boston Navy Yard; (Res.) 109 Old St., Randolph, Mass.

Wessman, Arnold (C), Radiologist, Chicago Bridge & Iron Co., Box 277, Birmingham, Ala.

West, Robert D. (C), Engineer, Manitowoc Shipbldg. Corp., Manitowoc, Wis.

Westendarp, H. O., Jr. (C), Sales Engineer, General Electric Co., Schenectady, N. Y.

Westfall, John W. (C), East Longmeadow, Mass.

Westin, Edward (B), Foreman, Twin City Lines, Snelling and University Ave., St. Paul, Minnesota.

Westmoreland, W. L. (C), Welding Foreman, Champion Paper & Fibre Co., Pasadena, Texas.

Weston, Kenneth (C), Estimator, Flower City Boiler Co., Inc., 719 Exchange St., Rochester, N. Y.

Wetherby, B. R. (C), Michigan Representative, Champion Rivet Co., 403 Donovan Bldg., Detroit, Mich.

Whaley, B. L. (C), Whaley Brothers, 45-15 21st St., Long Island City, N. Y.

Whaley, M. L. (C), Flame Hardening Div., National Cylinder Gas Co., 3573 Archer Ave., Chicago, Ill.

Whaley, Wm. (C), % Magnolia Airco Gas Prods. Co., Fort Worth, Texas.

Wharton, John (C), Owner, Natl. Welding & Grinding Co., 2825 Canton St., Dallas, Texas.

Wheatley, John G. (C), Supt. Engineering Dept., Eagle Globe & Royal Indemnity Co., 150 William St., New York, N. Y.

Wheeley, Wm. S. (B), Vice-President & Treasurer, Pennsylvania Engineering Works, New Castle, Pa.

Whitcomb, L. K., Jr. (C), Carnegie-Illinois Steel Corp., Sales Engrg. Dept., Carnegie Bldg., Pittsburgh, Pa.

White, Alfred (D), Welder, Delco Appliance Corp.; (Res.) 405 Avenue D, Rochester, N. Y.

White, Bernard L. (D), Welder, Aluminum Corp. of America; (Res.) 470 No. Dean St., Englewood, N. J.

White, Dan (C), Research Engineer, Cardwell Mfg. Co., Wichita, Kansas.

White, F. Erskine (C), Vice-President, Birmingham Fabricating Co., Birmingham, Ala.

White, H. E. (C), Sales Engineer, % Lincoln Electric Co., 926 Manchester Blvd., Pittsburgh, Pa.

White, J. C. (B), Owner, John C. White Welding and Machine Works, P. O. Box 93, Neepawa, Man., Canada.

White, J. R. (B), District Manager, Pittsburgh Testing Labs. of Texas, 317 M. & M. Bldg., Houston, Texas.

White, Marvin E. (C), President, Welding Gas Prods. Co., 821 E. 11th St., Chattanooga, Tenn.

White, Merle W. (D), 209 N. Oak, Paola, Kansas.

White, Roy (D), Welder, Spencer Trailer Co., Augusta, Kansas.

Whitehead, Edward (B), Manufacturers Representative, Taylor-Winfield Corp., 1025 Investment Bldg., Washington, D. C.

Whitehurst, S. R. (C), President & Treasurer, Whaley Engineering Corp., Princess Anne Road & Virginian R.R., Norfolk, Va.

Whitley, Wilson (D), Welding Inspector, Metropolitan Dist. Water Supply Comm.; (Res.) 549 Centre St., Newton, Mass.

Whitmer, Floyd C. (C), 2198 Bunts Road, Lakewood, Cleveland, Ohio.

Whitney, J. T. (C), Chief Engineer, The Whitney Engrg. Co., 100 Arlington St., Boston, Mass.

Whittemore, H. L. (C), Chief, Engineering Mechanics Section, National Bureau of Standards; (Res.) 3906 McKinley St., N. W., Washington, D. C.

Whittlesey, Harold C. (C), Consulting Structural Engineer, 525 N. Normandie Ave., Los Angeles, Calif.

Wicker, M. G. (C), Salesman, Air Reduction Sales Co., 4501 E. Fayette St., Baltimore, Md.

Wierschem, Henry (C), Structural Engineer, Lakeside Bridge & Steel Co.; (Res.) 4116 No. 17th St., Milwaukee, Wis.

Wiese, H. D. (B), Proprietor, Superior Welding Co., 331 Knoxville Ave., Peoria, Ill.

Wiesner, Arthur J. (C), President, The Wiesner-Rapp Co., Inc., 212 Winchester Ave., Buffalo, N. Y.

Wiggin, A. (D), Electric Welder, U. S. Govt., Box 145, Balboa Heights, Canal Zone.

Wikoff, Alan G. (B), Union Carbide Co., 30 E. 42nd St., New York, N. Y.

Wilburn, W. R. (C), Owner, Wilburn's Garage, 409 N. McDonough St., Decatur, Ga.

Wilch, Frank A. (C), Harnischfeger Corp., 1514 Prospect Ave., Cleveland, Ohio.

Wilder, Arthur B. (C), Research Metallurgist, Jones & Laughlin Steel Corp.; (Res.) 104 Newburn Drive, Mt. Lebanon, Pa.

Wilder, Arthur L. (D), 4749 W. 41st St., Tulsa, Okla.

Wildman, Oliver (D), Welder, Carl B. King Drilling Co., Box 304, Eunice, New Mexico.

Wiles, John, Jr. (D), Welder, Link-Belt Co., 658 Englewood Ave., Chicago, Ill.

Wiley, Frank (B), Factory Supt., EWA Plantation Co., Ewa, Oahu, Hawaii.

Wiley, George L. (B), Welding Supervisor, Mid-Continent Petroleum Corp.; (Res.) Box 226, West Tulsa, Okla.

Wilhelm, A. J. (B), Supt., Clearing Machine Corp.; (Res.) 7907 Wellington Ave., Elmwood Park, Ill.

Wilhelm, Frank J. (C), Engineering & Sales, Welded Construction, The Dayton Fabricated Steel Co., 1300 E. Monument, Dayton, Ohio.

Wilhite, J. B. (D), Operator, Ben. Sibbitt Iron & Fdy. Co.; (Res.) 617 New York, Wichita, Kansas.

Wilke, R. H. (D), Foreman, General Electric Co.; (Res.) 1116 Phoenix Ave., Schenectady, N. Y.

Wilkins, E. L. (B), Salesman, Magnolia Airco Prods. Co., P. O. Box 319, Houston, Texas.

Wilkins, Richard A. (B), Vice-President, Revere Copper & Brass Co., Rome, N. Y.

Wilkinson, O. F. (C), Supervisor, Air Reduction Sales Co., 630 So. 2nd, St. Louis, Mo.

Willard, Raymond Jr., (F), Student, Richards House, Lehigh University, Bethlehem, Pa.

Willey, Raymond C. (C), Planning and Rate Setting, General Electric Co.; (Res.) 1551 Chrisler Ave., Schenectady, N. Y.

Williams, A. M. (C), Welder, Wellman Engrg. Co.; (Res.) 2234 Bellfield Ave., Cleveland Htgs., Ohio.

Williams, Bart (D), 10212 So. Racine Ave., Chicago, Ill.

Williams, Clarence E. (C), Assistant Supt., Una Welding, Inc.; (Res.) 1757 Rosedale Ave., East Cleveland, Ohio.

Williams, E. B. (B), Naval Architect, The American Ship Building Co., Ft. of W. 54th St., Cleveland, Ohio.

Williams, F. H. (B), Asst. Test Engineer, Canadian National Railways; (Res.) 71 Cornwall Ave., Town of Mount Royal, Prov. Que., Canada.

Williams, Gordon T. (C), Metallurgist, Deere & Co., Metallurgical Dept., Moline, Ill.

Williams, Jack E. (B), District Manager, The Linde Air Products Co., 422 No. Washington Ave., Minneapolis, Minn.

Williams, Joe (C), 1548 Courtlandt St., Houston, Texas.

Williams, Joseph S. (B), Chief Engineer, P. R. Mallory & Co., Inc., 3029 E. Washington St., Indianapolis, Ind.

Williams, Leslie N. (D), Welding Dept., Black, Sivales & Bryson; (Res.) 5815 Kingslee Drive, Hickman Mills, Mo.

Williams, Marshall (B), Asst. to President, American Bridge Co., Frick Bldg., Pittsburgh, Pa.

Williams, Robert D. (C), Associate in Mechanical Engineering, Dept. of Mechanical Engineering, Univ. of Ill., Urbana, Ill.

Williams, Sylvester V. (C), Director Inspection, Oil Field Products Div., A. O. Smith Corp., 8136 Rock Way, Wauwatosa, Wis.

Williams, Thomas R. (C), District Manager, Compressed Industrial Gases, Inc., 1410 Cowart St., Chattanooga, Tenn.

Williamson, Geo. F. (B), Plant Supt., Darby Corp., Kansas City, Kansas.

Willins, G. L. (C), Welding Engineer, Champion Rivet Co., E. 108th St. & Harvard Ave., Cleveland, Ohio.

Willis, C. C. (C), Asst. Supt. of Generation Dept., Oklahoma Gas & Electric Co., 3rd & Harvey St., Oklahoma City, Okla.

Willis, George H. (C), Asst. Foreman, Jeffrey Mfg. Co.; (Res.) 206 Otentangy St., Columbus, Ohio.

Willis, James (D), Operating Welder, Keystone S. & W. Co.; (Res.) 1820 Market St., Pekin, Ill.

Willis, John G. (C), Welding Technician, Navy Yard, Philadelphia Pa.; (Res.) 325 Morris St., Woodbury, N. J.

Willott, Jack (B), General Foreman, Electro-Motive Corp.; (Res.) 3548 S. Scoville Ave., Berwyn, Ill.

Wills, C. A. (A), General Manager & Vice-President, Wm. B. Pollock Co., Youngstown, Ohio.

Wills, John A. (D), Welder, Eastman Kodak Co.; (Res.) 6 Marjorie Court, Rochester, N. Y.

Wills, John G., Jr. (C), Engineer, Blaw Knox Co., 6417 Kentucky Ave., East End, Pittsburgh, Pa.

Wilson, A. R. (B), Engineer, Bridges & Buildings, Pennsylvania Railroad, Broad St. Station, Philadelphia, Pa.

Wilson, C. R. (D), Welder, Minneapolis Sheet Metal Works; (Res.) 2705 First Ave., S., Minneapolis, Minn.

Wilson, F. D. (B), District Engineer, The Austin Co., 510 N. Dearborn St., Chicago, Ill.

Wilson, G. G. (C), Equipment Designer, Eastman Kodak Co.; (Res.) 224 Scholfield Road, Rochester, N. Y.

Wilson, James W. (C), Sr. Marine Engineer, Bureau of Marine Inspection & Navigation, Dept. of Commerce, 14th & Constitution Ave., Washington, D. C.

Wilson, John A. (B), Engineer, Wyatt Metal & Boiler Works, Dallas, Texas.

Wilson, John H. (B), President Wilson Mfg. Co., Wichita Falls, Texas.

Wilson, J. Lyell (C), Asst. Chief Surveyor, American Bureau of Shipping, 24 Old Slip, New York, N. Y.

Wilson, Loy (B), Pipe Line Construction, O. C. Whitaker, P. O. Box 2619, Tulsa, Okla.

Wilson, P. W., Jr. (C), The American Shipbuilding Co., Lorain, Ohio.

Wilson, Ross (C), Welding Engineer, Big Three Welding Equip. Co., 102 S. Cheyenne, Tulsa, Okla.

Wilson, R. A. (B), Salesman, Lincoln Electric Co.; (Res.) 1238—4th E., Cedar Rapids, Iowa.

Wilson, R. G. (C), General Foreman, Quaker City Iron Works, Aramingo Ave. & Ontario St., Philadelphia, Pa.

Wilson, R. G. (C), Manager, Merchandise Dept., Compressed Industrial Gases, Inc., 221 No. La Salle St., Chicago, Ill.

Wilson, Ralph L. (C), Metal Engineer, Climax Molybdenum Co., 1101 First Natl. Bank Bldg., Canton, Ohio.

Wilson, R. M. (C), Engineer, General Electric Co.; (Res.) 47 Park St., Montclair, N. J.

Wilson, Samuel A. (C), Estimating & Designing Engineer, Bethlehem Steel Co., Fabricated Steel Const.; (Res.) 420 Hickory St., Bethlehem, Pa.

Wilson, Wayne E. (C), Sales Promotion, General Electric Co., Schenectady, N. Y.

Wilson, Wilbur M. (B), Research Prof. of Struct. Eng., Univ. of Ill., 118 Talbot Laboratory, Urbana, Ill.

Wilt, Luther A. (D), Asst. Supervisor of Welding, S. Morgan Smith Co.; (Res.) R. D. 3, Dover, Pa.

Wimmer, Clifford (C), Asst. Welding Foreman, Keystone Steel & Wire Co.; (Res.) 1104 Ann St., Peoria, Ill.

Winder, E. J. (D), Welder, 406 E. 99th St., Inglewood, Calif.

Wingert, Byron E. (D), Owner, Operator, Welding Engineers; (Res.) 524 W. Howard St., Hagerstown, Md.

Winkelman, Harvey E. (C), Cincinnati District Manager, National Cylinder Gas Co., 4620 Este Ave., Cincinnati, Ohio.

Winn, Carl D. (D), Teacher, Maricopa High School, Box 581 Maricopa, Calif.

Winneberger, J. H. (B), J. B. Klein Iron & Fdy. Co., Oklahoma City, Okla.

Winship, Robert H. (D), Foreman, Bethlehem Steel Co.; (Res.) 58 Stewart St., Quincy, Mass.

Winter, M. A. (C), 409–11 W. Calif. Ave., Oklahoma City, Okla.

Winterkamp, Frederick (B), Designing Engineer, Carnegie-Illinois Steel Corp., Engineering Dept., Youngstown, Ohio.

Winthorpe, James (C), Cost Analyst, Combustion Engineering Co. (Res.) 4117 Tennessee Ave., Chattanooga, Tenn.

Winton, Robert (B), Welding Engineer, Norfolk & Western Ry., Roanoke, Va.

Wise, Grover L. (D), Electric Welder, % T. L. Wise, Sr., R. D. 3, Syracuse, N. Y.

Wishart, Wm. (B), Principal Surveyor, American Bureau of Shipping, 1412 Hibernia Bank Bldg., New Orleans, La.

Witherspoon, H. C. (C), President, H. C. Witherspoon, Inc., 4901 Santa Fe Ave., Los Angeles, Calif.

Witte, Albert M. (C), Salesman, Taylor-Winfield Corp.; (Res.) 729 Grand Ave., Dayton, Ohio.

Wolbert, Charles (B), Asst. Manager, Heltzel Steel Form & Iron Co., Warren, Ohio.

Wolf, A. Francis (C), Inspector, Southern Counties Gas Co., 6148 Annan Way, Los Angeles, Calif.

Wolfe, Geo. F. (B), Chairman of Welding Committee, Dravo Corp., Neville Island, Pittsburgh, Pa.

Wolfe, H. W. (B), Manager, Great Northern Tool Supply Co., P. O. Box 957, Billings, Mont.

Wolfe, Jonathan (B), Asst. Supt., Track and Roadway, Chicago Surface Lines; (Res.) 7700 Cregier Ave., Chicago, Ill.

Wolfel, R. C. (D), Welding Foreman, Pollak Steel Co., 206 W. Church St., Marion, Ohio.

Wolfenden, John T. (C), Cleveland Dist. Representative, Hobart Brothers Co., 2921 Prospect Ave., Cleveland, Ohio.

Wood, A. P. (C), Welder, General Electric Co., A. C. Engrg. Dept. Bldg. 41, Schenectady, N. Y.

Wood, J. F. (C), Fleet Engineer, Pittsburgh Steamship Co., 614 Superior Ave., N. W., Cleveland, Ohio.

Wood, J. N. (B), Welding Supervisor, Firestone Tire & Rubber Co.; (Res.) 690 N. Willett St., Memphis, Tenn.

Wood, Robt. F. (B), Asst. to Mr. Corse, Wm. M. Corse Chemical & Metallurgical Engineers, 810—18th St., N. W., Washington, D. C.

Woodman, C. E. (B), Purchasing Agent, % National Refining Co., 27 Greystone Ave., Kansas City, Mo.

Woodrough, Kendal A. (C), Inspector, J. R. Worcester & Co.; (Res.) 72 Arlington St., Newton, Mass.

Woods, G. W. (C), Development Engineer, Hughes Tool Co., Houston, Texas.

Woods, Paul H. (C), Mechanical Supt., Tide Water Associated Oil Co., Associated, Calif.

Woods, Wylie (D), Owner, Wylie Woods Mach. Works, 127 S. Hall St., Princeton, Ind.

Woodside, F. Lloyd (B), Metallurgist, Climax Molybdenum Co. of Michigan, 14410 Woodrow Wilson, Detroit, Mich.

Woodworth, Walter Z. (D), Arc Welder, W. Va. State Road Commission; (Res.) Burlington, W. Va.

Woofter, H. A. (B), Consulting Engineer; (Res.) 16545 Blackstone Ave., Detroit, Mich.

Woollard, Harry (D), Welder, Lennox Furnace Co.; (Res.) 8 S. 6th Ave., Marshalltown, Iowa.

Woolman, E. G. (D), Boilermaker & Welding Foreman, Cities Service Oil Co., Box 1300, Ponca City, Okla.

Worden, Robert L. (D), Shop Owner, Ben Lore Welding Co., 229 So. Emporia Ave., Wichita, Kansas.

Workman, J. C. (B), The American Shipbldg. Co., Foot of West 54th St., N. W., Cleveland, Ohio.

Workman, S. F. (C), Factory Supt., Frick Co.; (Res.) 135 Snider Ave., Waynesboro, Pa.

Worth, A. F. (D), Welder, Lockheed Aircraft, Burbank, Calif.; (Res.) 1837 Tyburn St., Glendale, Calif.

Worth, Sid. L. (D), Foreman Bridge Const. & Welder, Cattaraugus County, Dept. of Highways; (Res.) 111 Mill St., Little Valley, N. Y.

Wozniak, Joseph J. (D), Welder, Western Electric Co.; (Res.) 1622 So. 49th Ave., Cicero, Ill.

Wray, S. S. (B), Salesman, Eccles & Davies Mach. Co., 1910 Santa Fe Ave., Los Angeles, Calif.

Wright, Donald C. (C), Consulting Management Engineer; (Res.) 3425 Wenonah Ave., Berwyn, Ill.

Wright, John P. (B), Production Manager, International Filter Co., 325 W. 25th Place, Chicago, Ill.

Wright, Louis W. (D), Apprentice Repairman, Air Reduction Sales Co.; (Res.) 2628 Virginia Ave., Louisville, Ky.

Wright, Paul (B), Mechanical Engineer, Paul Wright & Co., 244 Brown-Marx Bldg., Birmingham, Ala.

Wright, S. J. (D), Welding Supervisor, Norfolk & Western R. R., Motive Power Bldg., Roanoke, Va.

Wright, W. C. (D), Welding Instructor, Reed Roller Bit Co.; (Res.) 6920 Ave. L, Houston, Texas.

Wulftange, F. H. (C), Owner, Wulftange Mfg. Co., 828 Belle Ave., Hamilton, Ohio.

Wunder, Leon (D), 123 Lewis St., New York, N. Y.

Wyant, Robert A. (C), Research Fellow, Rensselaer Polytechnic Institute, Dept. of Metallurgical Engineering, Troy, N. Y.

Wyer, R. F. (C), Welding Engineer, General Electric Co., Schenectady, N. Y.

Wynn, Arthur R. (D), Asst. Foreman, Welding Dept., Bethlehem Steel Co.; (Res.) 2924 Northern Parkway, Baltimore, Md.

Wyss, J. Calvin (C), 1838 Allendale Ave., E. Cleveland, Ohio.

Y

Yard, Leroy E. (D), Welder, R. G. Le Tourneau, Inc.; (Res.) 1548 Lincoln Ave., Peoria, Ill.

Yasines, S. Franz (C), Asst. Professor, New York Univ., University Heights, N. Y.

Yates, Ira B. (B), General Sales Manager, Wilson Welder Metals Co., 60 E. 42nd St., New York City.

Yeadon, John R. (D), Welder, General Electric Co.; (Res.) 48 Myrtle St., Lynn, Mass.

Yehle, H. C. (D), Sheet Metal, Gleason Works; (Res.) 222 Garford Road, Rochester, N. Y.

Yeomans, Alfred (C), Engineer, U. S. Naval Gun Factory; (Res.) 3324—19 St., N. W., Washington, D. C.

Yetter, Gilbert L. (C), Engineer, U. S. Bureau of Reclamation, Coulee Dam, Wash.

Yingling, James A. (C), Foreman, York Mfg. Co.; (Res.) 208 Cottage Hill Road, York, Pa.

Yoch, A. H. (C), Specialist, Applied Engineering Dept., Air Reduction Sales Co.; (Res.) 4517 York Road, Philadelphia, Pa.

York, Herman E. (C), 310-A Ohua Ave., Honolulu, T. H.

York, Ralph L. (B), York & Co., Bandon, Ore.

Young, A. A. (C), Welding Engineer, A. B. King & Co., 194 Chapel St., New Haven, Conn.

Young, Almon P. (B), Associate Professor of Mechanical Engineering, Michigan College of Mining & Technology, Houghton, Mich.

Young, C. A. (B), Secretary of Production, American Petroleum Institute, 1508 Gulf State Bldg., Dallas, Texas.

Young, Chas. D. (B), District Manager, Metal and Thermit Corp., 7300 South Chicago Ave., Chicago, Ill.

Young, Charles R. (C), Builder and Contractor; (Res.) 101 Essex Ave., Bloomfield, N. J.

Young, E. D. (D), 4216 Lafayette St., Omaha, Nebr.

Young, James H. (B), Vice-President, General Engrg. & D. D. Co., Foot of 5th Ave., Oakland, Calif.

Young, Perrin W. (D), New England Welding Lab., 88 St. Stephen St., Boston, Mass.

Young, Raymond A. (B), President, Young Sales, Inc., 1032 So. 41st St., Milwaukee, Wis.

Young, Shang Shoa (C), % Wah Chang Trading Co., 233 Broadway, New York, N. Y.

Young, Willard A. (C), Treasurer, Young Sales, Inc., 1032 So. 41st St., Milwaukee, Wis.

Yuhasz, E. J. (C), Station 14, Windsor, Conn.

Z

Zahn, Otto (C), Salesman, Compressed Industrial Gases, Inc.; (Res.) 4410—17th Ave., Kenosha, Wis.

Zeilman, R. H. (B), Chief Engineer, The Thew Shovel Co.; (Res.) 236 Hamilton Ave., Elyria, Ohio.

Zentgraf, Lesley (D), Arc Welder, Oilgear Co.; (Res.) Crestwood Sub. Rt. 9, Milwaukee, Wis.

Zieger, Max (B), President & General Manager, American Trade Schools, Inc., 789 Broad St., Newark, N. J.

Ziegler, A. A. (B), Manager of Sales, Morris, Wheeler & Co., Inc., Fox St. & Roberts Ave., Philadelphia, Pa.

Ziegler, Lyle C. (C), Power Sales Engineer, The Ohio Public Service, 8th & Broadway, Lorain, Ohio.

Ziemer, Clarence (D), Welder, R. G. Le Tourneau, Inc., Peoria Ill.

Zimmer, Rudolf (C), 247 Nassau Ave., Brooklyn, N. Y.

Zimmerman, Donald M. (C), Foreman, York Safe and Lock Co.; (Res.) 617 N. Hartley St., York, Pa.

Zimmerman, Gene H. (F), 6842 Lafayette Ave., Chicago, Ill.

Zimmerman, John H. (B), Development Engineer, The Linde Air Products Co., 30 E. 42nd St., New York City.

Zinkand, William C. (C), Supervisor of Welding, Frick Co., 35 Philipps Ave., Waynesboro, Pa.

Zoll, H. W. (D), 4213 N. Mason Ave., Chicago, Ill.

Zorn, Wm. H. (B), Welding Engineer, Detroit Edison Co.; (Res.) 15096 Mill St., Allen Park, Mich.

Zouck, G. H. (C), Mechanical Engineer, The Balmar Corp., Woodberry, Baltimore, Md.

Zullo, Basil (C), 2027 E. 35th St., Brooklyn, N. Y.

INDEX TO ADVERTISERS

Air Reduction Sales Co.....Outside back cover
American Brass Company..... 854
American Brass Company..... 858
American Chain & Cable Company, Inc.. 857
Anti-Borax Compound Company..... 846
Bastian-Blessing Company..... 848
The Champion Rivet Company..... 863
Electric Arc Cutting & Welding Co..... 851
Federal Machine & Welder Co..... 853
General Electric Co.....Inside front cover
Haynes Stellite Company..... 861
Lincoln Electric Company..... 871
Lincoln Electric Company....Inside back cover
The Linde Air Products Company..... 772
P. R. Mallory & Co., Inc. 850
The McKay Company..... 845
Metal & Thermit Corporation..... 874
National Carbide Corporation..... 847
Page Steel & Wire Division..... 857
Resistance Welder Mfrs. Association.... 859
John A. Roebling's Sons Company..... 855
Ruemelin Mfg. Company..... 850
Square D Company 856
Stulz-Sickles Company..... 846
Thomson-Gibb Electric Welding Company 848
Union Carbide and Carbon Corp..... 772
U. S. Steel Corp. Subsidiaries..... 849
Victor Equipment Company..... 865
Westinghouse Elec. & Mfg. Co..... 867
Wilson Welder & Metals Co. Inc..... 869

Sections—CHAIRMEN, SECRETARIES AND REGULAR MEETING DATES

BIRMINGHAM 3rd Fri.
CHAIRMAN—J. W. ADAMS, 209 No. 52nd St., Birmingham, Ala.
SECRETARY-TRBAS.—W. L. POOLE, Air Reduction Sales Company, 2825 No. 29th Ave., Birmingham, Ala.

BOSTON 3rd Mon.
CHAIRMAN—F. B. MEHAFFEY, Air Reduction Sales Co., 122 Mt. Vernon St., Upham Crs., Dorchester, Mass.
SECRETARY—P. N. RUGG, 8 Eastern Ave., Wakefield, Mass.

CANTON, OHIO (Akron, Alliance, etc.)
ROBERT M. WALLACE, The Griscom-Russell Company, Massillon, Ohio

CHATTANOOGA, TENN. 1st Wed.
CHAIRMAN—A. R. MCLAIN, 622 Georgia Avenue, Apt. 110 Chattanooga, Tenn.
SECRETARY—J. H. WEIGEL, 117 Ochs Highway, Chattanooga, Tenn.

CHICAGO 3rd Fri.
CHAIRMAN—E. A. BALSLEY, Link-Belt Co., 300 W. Pershing Rd, Chicago Ill.
SECRETARY—M. S. HENDRICKS, Welding Engineer, Room 731, 506 South Wabash Avenue

CINCINNATI, OHIO
CHAIRMAN—JAMES K. ROSS, United Welding Co., Box 247, Middletown Ohio
SECRETARY—LEWIS B. BLAKENEY, Westinghouse E. & M. Co., 207 N. 3rd St., Cincinnati, Ohio

CLEVELAND 2nd Wed.
CHAIRMAN—F. L. PLUMMER, 2591 Charney Rd.
SECRETARY-TRBAS.—A. LESLIE PFBIL, Universal Power Corporation, Cleveland, Ohio

COLORADO 3rd Wed.
CHAIRMAN—J. H. JOHNSON, Johnson Supply Co., Denver, Colo.
SECRETARY-TREAS.—H. W. CREAGER, Hendrie & Bolthoff Mfg. & Supply Co., Denver, Colo.

COLUMBUS, OHIO 2nd Fri.
CHAIRMAN—F. S. CALDWELL, Jeffrey Mfg. Co., Columbus, Ohio
SECRETARY—G. S. HERREN, The Seagrave Corp., Columbus, Ohio

CONNECTICUT 1st. Tues.
CHAIRMAN—JOHN J. VREELAND, Chase Brass & Copper Co., Waterbury, Conn.
SECRETARY—H. A. PENNINGTON, A. B. King & Co., 196 Chapel Street, New Haven, Conn.

DETROIT 1st or 2nd Fri.
CHAIRMAN—H. P. DOUD, General Electric Co., 700 Antoinette, Detroit
SBCRBTARY—O. L. SMITH, Weldit Acetylene Co., 638 Bagley, Detroit, Mich.

GEORGIA 1st Mon.
CHAIRMAN—D. B. HUNT, 484 Brentwood Dr. N. E., Atlanta, Ga.
SECRETARY—H. J. LEE, P. O. Box 4564, Atlanta, Ga.

HAWAII 4th. Tues.
CHAIRMAN—ROBERT A. PLAUS, W. A. Ramsay, Ltd., Honolulu, T. H.
SECRETARY—ALAN G. SLIPPER, Hawaiian Gas Products Co., P. O. Box 2454, Honolulu, T. H

INDIANA
CHAIRMAN—A. C. RASMUSSEN, Insley Mfg. Corp., Indianapolis, Ind.
SECRETARY—G. HAISLUP, Electric Steel Castings Co., Speedway City, Indianapolis, Ind.

KANSAS CITY, MO. 3rd Mon.
CHAIRMAN—J. L. FIZZELL, National Steel Prods. Co., Kansas City, Mo.
SECRETARY—JAMES P. KIRK, Air Reduction Sales Co., Kansas City, Mo.

LOS ANGELES 3rd Thurs.
CHAIRMAN—C. P. SANDER, Western Pipe & Steel Co., Los Angeles. Calif.
SECRETARY,—J. C. GOWING, 644 E. Florence Ave., Los Angeles, Calif.

LOUISIANA 1st Fri.
CHAIRMAN—O. B. MCLAUGHLAN, Freeport Sulphur Co., Port Sulphur La.
SECRETARY—CLYDE PINE, The Isaac Delgado Trade School, New Orleans, La.

MARYLAND 3rd Fri. except April
CHAIRMAN—C. N. HILBINGER, T. A. Canty, Inc., Baltimore, Md.
SECRETARY-TREAS.—R. A. MANSFIELD, Southern Oxygen Co., Baltimore, Md.

MEMPHIS
CHAIRMAN—E. H. GILL, National Butane Gas Co., Memphis, Tenn.

MILWAUKEE 2nd Wed.
CHAIRMAN—R. W. STERNKE, Lakeside Bridge & Steel Co., Milwaukee, Wis.
SECRETARY—GILBERT F. MEYER, Machinery & Welder Corp., Milwaukee, Wis.

MONTANA 1st Wed.
CHAIRMAN—WM. GALLOWAY, 4407-B, Fort Peck, Mont.
SECRETARY—H. S. GRIFFITH, Box 1787, Fort Peck, Mont.

NEW YORK 2nd Tues. except when Joint Meeting is held
CHAIRMAN—CHARLES KANDEL, Craftsweld Equipment Co., Long Island City, N. Y.
SECRETARY—G. V. SLOTTMAN, Air Reduction Sales Co., 60 East 42nd Street, New York, N. Y.

NORTHWEST 3rd Wed.
CHAIRMAN—T. J. WARMINGTON, Wm. Bros. Boiler & Mfg. Co., Minneapolis, Minn.
SECRETARY—ALEXIS CASWELL, Manufacturers Assoc. of Minn., 200 Builders Exchange Bldg., Minneapolis, Minn.

NORTHERN N. Y. Last Thurs.
CHAIRMAN—R. W. CLARK, General Electric Company, Schenectady, N. Y.
SECRETARY—G. A. ROSS, General Electric Co., Schenectady, N. Y.

WESTERN NEW YORK Last Mon.
CHAIRMAN—J. R. DAWSON, Union Carbide and Carbon Res. Labs., Niagara Falls, N. Y.
SECRETARY—C. S. FREEMAN, The Lincoln Electric Company, Buffalo, N. Y.

OKLAHOMA CITY 1st Fri.
CHAIRMAN—K. B. BANKS, P. O. Box 1377, Oklahoma City, Okla.
SECRETARY—C. C. WILLIS, P. O. Box 1498, Oklahoma City, Okla.

OMAHA, NEBRASKA 2nd Thurs.
CHAIRMAN—L. B. THOMAS, Economy Welding Service, 18th & Cuning Sts., Omaha, Nebr.
SECRETARY—G. E. MCGRATH, McGrath Welding Co., 4026 Nicholas, Omaha, Nebr.

PEORIA—CENTRAL ILLINOIS
CHAIRMAN—E. E. ISGREN, R. G. Le Tourneau, Inc., Peoria, Ill.
SECRETARY—W. J. BROOKING, 855 Virginia Ave., Peoria, Ill.

PHILADELPHIA 3rd Mon.
CHAIRMAN—H. R. SALISBURY, Air Reduction Sales Co., Allegheny & 17th Sts., Phila., Pa.
SECRETARY—H. E. HOPKINS, Arcos Corp., 401 N. Broad St. Phila., Pa.

PITTSBURGH Middle Wed.
CHAIRMAN—G. F. WOLFE, Dravo Corp., Engineering Wks. Div., Pittsburgh, Pa.
SECRETARY—J. F. MINNOTTE, Minnotte Bros., 1201 House Bldg., Pittsburgh

PUGET SOUND
CHAIRMAN—H. HAYNES, Commercial Boiler Wks., 53 W. Lander St., Seattle, Washington
SECRETARY—L. A. BURQUE, Westinghouse Elec. & Mfg. Co., Seattle, Washington

QUAD CITIES
CHAIRMAN—A. R. GUSTAFSON, 840 Third Ave., Moline, Illinois
SECRETARY—J. W. SHUGARS, Lincoln Elec. Co., Moline, Illinois

ROCHESTER, N. Y. 1st Thurs.
CHAIRMAN—EDWIN ALLAN, Allan Iron & Welding Wks., Inc., 133 Murray St.
SECRETARY—PAUL W. JAMES, Lincoln Electric Co., 17 Lake View Park, Rochester, N. Y.

SAN FRANCISCO Last Fri.
CHAIRMAN—T. R. ROONEY, Western Pipe & Steel Co., San Francisco,, Calif.
SECRETARY—J. G. BOLLINGER, Air Reduction Sales, Park & Halleck Sts., Emeryville, Calif.

SAN JOAQUIN VALLEY
CHAIRMAN—G. D. ATMORE, 401 Santa Barbara St., Santa Paula, Calif.
SECRETARY—A. W. SCHERER, 918 Quincey St., Bakerfield, Calif.

ST. LOUIS 2nd Fri.
CHAIRMAN—N. F. MOSS, 7508 Wayne, St. Louis, Mo.
SECRETARY—M. E. MEYERSON, 2317 Chouteau Ave., St. Louis, Mo.

SOUTH TEXAS
CHAIRMAN—L. H. COURTRIGHT, Reed Roller Bit Co., Houston, Texas
SECRETARY—G. V. DYE, G. V. Dye Co., Houston, Texas

TULSA, OKLAHOMA
CHAIRMAN—G. L. WILEY, Box 226, W. Tulsa, Okla.
SECRETARY—O. L. ROGERS, Big Three Welding & Equipment Company, Tulsa, Okla.

YORK—CENTRAL PENNA.
CHAIRMAN—J. W. HENNESSEY, York Safe & Lock Co., York, Pa.
SECRETARY—C. B. HERRICK, 628 S. Pershing Ave., York, Pa.

YOUNGSTOWN (OHIO) 2nd Mon.
CHAIRMAN—E. W. TROSS, United Eng. & Fdy. Co., Youngstown, Ohio
SECRETARY-TREAS.—C. R. RIGGLE, Ohio Edison Co., Youngstown, Ohio

WASHINGTON, D. C. 2nd Tues.
CHAIRMAN—C. A. LOOMIS, Bureau of C. & R. Navy Dept., Washington, D. C.
SECRETARY—G. E. KNOX, Bureau of Yards & Docks, Navy Dept., Washington, D. C.

WICHITA, KANSAS
CHAIRMAN—L. E. KIRK, Vickers Petroleum Co., Wichita, Kansas
SECRETARY-TREAS.—K. O. HOUSER, Kansas Gas & Electric Co., Wichita, Kansas

LIST OF MEMBERS

(NOVEMBER 22, 1939)

Arranged Geographically

ALABAMA

Bessemer—
McCain, W. W.
Rodgers, Wm.
Simmons, W. A.
Sleeman, W. C.
Tinsley, C. R.

Birmingham—
Adams, Julien W.
Begley, F. P.
Beldon, Arthur J.
Blesi, John E.
Braun, E. W.
Davidson, R. W.
Deas, R. R., Jr.
Durstine, John E.
Floyd, Walter M.
Godwin, J. M.
Hammond, A. H.
Handley, A. B.
Holmes, D. O.
Johnson, E. J.
Jones, Russell, Jr.
Jones, Russell E., Sr.
Ledbetter, R. H.
Leigh, A. G.
Lewis, N. H.
Lindsey, I. T.
MacKenzie, James T.
McAtee, F. E.
McCaghren, J. R.
McCutcheon, W. P.
Michaels, E. E.
Millar, C. S.
Montebana, Joe
Nickell, K. V.
Nihlen, Arvid
Pinckney, C. C.
Poole, William L.
Post, W. A.
Price, Herman G.
Ray, M. H.
Roberts, W. P.
Ross, Paul
Ruff, Joseph B.
Sanneman, W. J.
Soyars, John M.
Swader, Claude
Wessman, Arnold
White, F. Erskine
Wright, Paul

Ensley—
Kunze, A. E.

Fairfield—
Brooks, W. E.

Gadsden—
Korner, C. E.

Gardendale—
Neel, A. O.

Irondale—
George, Stanley L.
McKinney, L. T.
Sisk, J. W.

Mobile—
Griser, John M.
Revell, J. J.

North Birmingham—
Flood, J. P.

Tuscaloosa—
Cain, J.

ARIZONA

Ajo—
Haggard, C. R.

Buckeye—
Coleman, Frank L.

Douglas—
Rowley, Carl F.

ARKANSAS

Walnut Ridge—
Dedman, S. F.

CALIFORNIA

Albany—
Burge, Kenneth A.

Alhambra—
Blennerhassett, L. I.
Cutler, Ralph W.
Earl, Lloyd R.
Lucas, John W.
Radcliffe, Thos. D.
Swift, Clinton E.

Arwin—
Ogilivie, C. L.
Ronehausen, Arthur J.
Straw, Gilbert

Associated—
Woods, Paul H.

Bakersfield—
Beveridge, Edwin F.
Blackman, E. N.
Hammett, H. R.
Jasper, Wm. J.
McCarthy, R. E.
Meyer, Arthur
Meyer, Paul K.
Scherer, A. W.
Smith, F. B.
Smith, Roland
Van Leuven, E. P.

Bell—
Weiler, Ernest

Bellflower—
Beebe, Erle J.
Huffman, Shelton B.
Ireland, B. N.

Berkeley—
Mainwaring, Norman, R.
Raitt, George H.
Ward, N. F.

Burbank—
Erickson, Harry A.
Foster, John H.
Kennon, Lorenzo
Ross, Jack D.

Crockett—
Carlson, C. L.

Downey—
Booth, C. E.
Schexschnider, Lee

Eaglerock—
McGhie, Roland W.

El Segundo—
Maradudin, A. P.

Emeryville—
Bollinger, J. Gale
Grant, C. E., Jr.
Saunders, Harold W.

Fellows—
Hayes, George, Jr.

Fresno—
Emmert, Keith

Glendale—
Eastman, Leslie E.
Heacock, E. E.
McClure, E. E.
Stevenson, Francis H.
Worth, A. F.

Hawthorne—
Dietrich, N. M.

Huntington Park—
Bockemohle C. L. A.
Dana, Theron G.
Dexter, D. O.
Govi, Joe
Levyn, Stanley M.
Patterson, Donald
Purcell, John
Steadman, W. C.
Turner, M. H.

Inglewood—
Keptner, Thomas C.
Winder, E. J.

Lomita—
Brown, J. E.

Long Beach—
Black, William A.
Tullis, H. T.

Los Angeles—
Alexander, G. W.
Allen, Clayton M.
Babbitt, C. A.
Baker, H. U.
Ball, D. A.
Banzhaf, E.
Baylor, W. B.
Binder, R. W.
Blake, James C.
Bruton, James J.
Buehler, Walter
Campbell, Geo. F.
Canfield, Milton E.
Chandler, Ezra A.
Cheney, William B.
Cipperly, E. J.
Clotfelter, R. A.
Cottrell, W. P.
Cummings, W. E.
De Camp, Ray E.
De Long, Chas. E.
Derby, F. R.
Emery, Frank C.
English, Walter W.
Erickson, C. J.
Etter, Harold P.
Fry, John O.
Gowing, J. C.
Graves, R. L.
Hansen, Roy E.
Hardy, J. B.
Heddell, Douglas
Heinmiller, P. M.
Horne, L. V.
House, Charles W.
Hughes, Paul B.
Johansen, Edwin B.
Johnston, A. P.
Johnston, Merkle C.
Kamschulte, E. J.
Kilian, J. O.
Klein, A. A.
Lacy, Walter P.
Lane, Harry A.
Laszloffy, A., Jr.
Laulhere, Bernard M.
Layfield, Elwood
Le Master, R. M.
Mariette, E. H.
Mathews, W. W.
McCaffrey, M. P.
McCartney, James W.
McClara, W. H.
McDonald, E. L.
McElfish, P. D.
McGinley, Francis V.
McGinnis, C. E.
McGuire, H. K.
Miller, Ray
Morrill, Guy
Murcell, U. C.
Newton, B. E.
Noll, Paul E.
O'Callaghan, J. J.
Parsons, G. S.
Patterson, Guy
Patterson, W. K.
Pegram, W. A.
Priest, Chas. H., Jr.
Reese, C. E.
Reno, Robert W.
Roberts, C. William
Rokes, D. G.
Rork, Frank C.
Ross, D. D.
Sander, C. P.
Scherrer, Robert
Schlittler, H. F.
Schooley, F. W.
Smith, Turner C.
Smith, Wm. R.
Smith, W. Walter, Jr.
Sparks, H. H.
Sparling, J. T.
Stocker, C. L.
Tadd, R. W.
Thompson, Moreton R.
Ursem, Earl
Ward R. E.
Watson, G. G.
Weil, E. H.
Weiss, George
Whittlesey, Harold C.
Witherspoon, H. C.
Wolf, A. Francis
Wray, S. S.

Lynwood—
Nelson, Fred

Mare Island—
Clinkscale, Roy
Sledge, Alexander

Maricopa—
Winn, Carl D.

Monrovia—
McMullen, L. A.

Montebello—
Haynes, A. R.

Monterey Park—
Bringer, R. A.

Mt. Shasta—
Mummey, L. R.

Napa—
Rule, J. O.

North Hollywood—
Cannon, Walter B., Jr.

Oakland—
Barker, J. E.
Cook, Ray
De Freitas, P. J.
Josephian, Wm.
Odom, J. Carl
Phillips, Seward
Young, James H.

Oxnard—
Sorensen, W. C.

Palo Alto—
Durand, William F.
Harcourt, Robert H.

Pasadena—
Clark, Donald S.
Taber, Clifford W.

Pomona—
Roy, Davis
Sebby, R. M.

Richmond—
Fischer, Michael
Patterson, Harold A.

Rio Vista—
Nelson, Julius

Riverside—
Brown, R. Earl

Roseville—
Collins, Frank E.

Sacramento—
Berry, Edwin
Korn, Kenneth E.
Rose, Lambert V.

Salinas—
Curtis, Wm. F.

San Francisco—
Bechtel, S. D.
Bloodgood, C. M.
Chaney, Harold P.
Chapman, R. B.
Coffee, Henry Colman
Commins, E. A.
Constanz, H. E.
Cook, Fredk. S.
Delhi, L. W.
Elsener, L. A.
Emmett, W. E.
Grundell, Leonard E.
Hammon, Geo. L.
Hanna, Richard
Hawley, A. L., Jr.
Henderson, L. P.
King, K. V.
LaForce, Jean
Leonhauser, A. O.
Martin, Chas. W.
Mason, James H.
Mathy, E. L.
Minnotte, Charles F.
Owens, Clarence R.
Page, F. A.
Phelps, Arthur H.
Phillips, Horace P.
Pratt, Howard N.
Rooney, T. R.
Sargent, Harold F.
Sawyer, W. B.
Schroeder, Otto
Smith, Charles S.
Snow, Walter A.
Steckel, Frederick R.
Strecker, Rudy
Sturgess, Robert K.
Tanner, Frank T.
Taylor, John F.
Taylor, S. H.
Thompson, N. W.
Waldman, R. O.

San Jose—
Sorensen, Martin

San Pedro—
Ley, Harry J.
Petsche, R. A.

Santa Monica—
Coghlan, S. F.

Santa Paula—
Atmore, Guy D.

Santa Rosa—
Haines, Mark

South Gate—
Bisbee, David P.
Mant, Frank M.

South Pasadena—
Clough, Frank H.
Grant, Edmund G.
Newby, H. L.

South San Francisco—
Aldrich, William
Conor, Frank

Stockton—
Bradbury, Geo. L.

Taft—
Blubaugh, Earl F.
Dunn, Oscar
Eaves, J. M.
Harrison, E. F.

Torrance—
Dillinger, L.
Wallace, Walter P.

Trona—
Edleman, L. A.

Tujunga—
Moyer, Daniel E.

Vacaville—
Roulund, Troy L.

Vallejo—
Keeney, Ray

Venice—
Farnsworth, R. D.

Vernon—
Brumbaugh, A. Kyle, Jr.

West Los Angeles—
Duff, Gordon
Roach, Harold

Whittier—
Stoody, Winston F.

COLORADO

Denver—
Applegate, C. W.
Brown, Thomas R.
Clark, E. E.
Creager, H. W.
Gallegly, Ray
Garrett, Gairald H.
Gooden, Elwood O.
Hussion Wm. T.
Jackson, H. J.
Johnson, J. H.
Kilgore, Gordon
LeBaron, R. P.
Liston, Earl C.
Lowry, Gilmore T.
McClintock, C. E.
McClure, Clyde E.
Odell, Aden G.
Organ, C. J.
Taylor, J. T.
Troutman, Lester L.
Weinberger, Ed.

CONNECTICUT

Ansonia—
Erler, J.
Gray, Joseph G.
Hook, Ira T.
Mitchell, James R.
Stevenson, D. B.
Tomlinson, P. H.

Bridgeport—
Bremer, E. W.
Flanigan, J. Gordon
Kelso, George W.
Leach, Robert H.
Morris, Dr. Alan

Cos Cob—
Dresher, C. S.

Derby—
Gesner, Hobart E.

Groton—
Paige, J. W.

Hartford—
Ferguson, William
Fish, Edwards R.
Fresher, George S.
Goodwin, J. L.
Powell, Marselis
Reese, D. F.

Manchester—
Garaventa, Ermano

Meriden—
Tierney, J. D.

Middletown—
Crowell, Tracy

Milford—
Bissell, Kenneth B.

New Britain—
Parsons, J. S.
Perry, Bernard H.
Rusch, Richard

New Haven—
Churchward, Jack
Farnham, M. A.
Hammond, Charles F.
Imperati, Joseph
Johnson, Seth
Jones, Henry W., Jr.
King, Alfred B.
Miller, John S.
Pennington, H. A.
Welter, Gustave
Young, A. A.

New London—
Keeler, Nelson Herbert

South Norwalk—
Bittner, Henry

Waterbury—
Candee, E. T.
Crampton, D. K.
Gallo, Francis A.
Sharp, H. P.
Valukas, Albert
Vreeland, John

West Hartford—
Halsey, Wm. D.

Westport—
Johnson, W. E.

Windsor—
Yuhasz, E. J.

DELAWARE

Edge Moor—
Hughes, A. H.

Wilmington—
Hays, Athel V.
Saul, Harry H.
Spiegelhalter, Andrew G.
Viohl, Herbert
Webb, W. W.

DISTRICT OF COLUMBIA

Amirikian, A.
Anthony, Chas. C.
Bert, John D.
Biggs, W. E.
Bissell, A. G.
Britt, Oscar L.
Bryan, Harry, Jr.
Bunn, W. B., Jr.
Carleton, H. E.
Carlson, Chas. A.
Chappelear, J. A.
Clarke, Neil A.
Coffin, Howard C.
Coleby, Comdr. Francis J. A.
Crecca, John D.
Currell, John W.
Derr, George M.
Dietz, William F.
Dormer, John A.
Eiwen, Chas. J.
Ellinger, Geo. A.
Enzian, R. I.
Farr, William S.
Fawcett, Lewis H.
Fisher, Leonard C.
Frankland, John M.
Hale, R. S.
Hamill, Thomas E.
Hiemke, Hugo W.
Hill, Joe H.
Hodes, Isidore
Hodges, Earle C.
Horn, A. M.
Hough, C. T., Jr.
Jeffries, W. J.
Jenks, Glen F.
Kelsey, Walter
Kenny, John F.
Knox, George
Lang, Elmer
Loomis, C. A.
MacCutcheon, Edward M., Jr.
MacIntosh, C. D.
Mahan, B.
McCord, H. C.
McGredy, R. H.
McKenzie, Wm. E.
Meissner, C. E.
Morgan, Nathan W.
Morris, Ned
Murphy, Charles P.
Nation, Robt. B.
Noyes, Mason S.
Olcott, Floyd B.
Patton, E. R.
Perry, Charles F.
Petroskey, E. C.
Piltch, A.
Quinn, John I.
Reid, Sibbald
Ritter, Julius C.
Robertson, James B., Jr.
Schleicher, Jacob Wm.
Schoen, Henry
Seabury, C. C.
Skinner, Orville B.
Smith, Geo. P.
Smyth, Robert
Snyder, P. W.
Steinman, George C.
Stine, Wilmer E.
Strandell, John H.
Trexel, C. A.
Tuke, Harry A.
Underwood, C. M.
Warriner, Reuel E.
Webber, W. C.
Wells, Robert K.
Whitehead, Edward
Whittemore, H. L.
Wilson, James W.
Wood, Robt. F.
Yeomans, Alfred

FLORIDA

Jacksonville—
Anderson, Karl H.
Moore, Clarence H.
Redman, R. P.
Spaulding, Ralph E.

Pensacola—
Getsug, Bert

St. Augustine—
Gardner, T. H.

GEORGIA

Atlanta—
Anchors, E. H.
Bird, George T.
Bloxton, C. R.
Donnelly, Chas.
Drennon, R. E.
Guillot, Edward
Harrison, E. R.
Hunt, Duard B.
Hunt, G. C.
Hunt, Leon
Jackson, R. H.
Lee, H. J.
McCoy, Arthur M.
Minch, W. T.
Revis, L. H.
Seawell, Lacy W.
Shearouse, James D.
Snead, Marion C.
Snipes, A. D.
Stephens, De Witt
Teeple, Ralph J.
Totten, J. M.
Trammell, R. H.
Wallace, L. A.

Brookhaven—
Ricketts, F. L.

Decatur—
Frierson, W. D.
Wilburn, W. R.

Dublin—
Rawls, Otis B.

Macon—
Balkcom, E. E.

Marietta—
Glover, James Bolan

Newnan—
Blackburn, Bryan

Savannah—
Inglesby, Chas. A.

Toccoa—
Cochran, Virgil
Flanagan, Robert F.

IDAHO

Caldwell—
Bennett, A. A.

Moscow—
McVey, John W.

ILLINOIS

Bartonville—
Parr, Maurice A.
Rogind, Roland

Berkley—
Valenta, G.

Berwyn—
Druetzler, Chas. O.
Hansen, Carl
McDonough, L. J.
Willott, Jack
Wright, Donald C.

Blue Island—
Culbertson, Russel P.
Storz, Melvin
Van Gorder, Karl

Brookfield—
Adams, David

Carterville—
Ray, Bean

Chicago—
Ahlstrom, Walter
Alt, William
Adams, Gale
Andersen, G. M.
Ardagh, J. C.
Arness, O. J.
Bacon, Harold
Bailey, O. A.
Balsley, Eugene A.
Banash, I. I.
Bassler, Clyde G.
Boardman, Harry
Boley, Frederick W.
Bornscheim, John F.
Bradley, Chas. E.
Bradley, R. B.
Brady, W. M. B.
Brandsma, R.
Brooke, Thomas V.
Brucks, H.
Bruen, Henry
Burnam, C. M., Jr.
Callas, Henry
Candy, Albert M.
Carlson, C. E.
Carlson, O. M.
Carpenter, F. H.
Charvat, Jos. J.
Christensen, Harry
Clausen, E. W.
Cox, Chas. N.
Cusson, F. J.
Daniels, Chas. J.
Daniels, H. J.
Danielson, L. G.
Davidson, Earl H.
Dencer, F. W.
Dowson, Harry
Dunham, M. Keith
Emin, G. H.
Everhard, E. P.
Faulkner, James H.
Fink, Kamille B.
Fitzgerald, F. R.
Flood, E. J.
Franzen, James F.
Freeman, Harry William
Gannett, H. E.
Gergits, William
Gilbride, Thomas J.
Gleich, W. J.
Graves, E. R.
Grot, Arnold S.
Hanson, Carl R.
Hasse, Frank C.
Hendricks, M. S.
Henke, E. J.
Hess, E. A.
Hinkle, W. R., Jr.
Hjulin, Paul
Hobart, Chas. J., Sr.
Hoier, Wm. V.
Holt, R. W.
Horlock, Jack
Horton, George Terry
Howland, Owen L.
Hudson, James A.
Hudson, William T.
Hurley, Wallace J.
Hurt, R. J.
Immer, W. L.
Johansen, E. K.
Jones, Tom
Kenrick, Ralph S.
Kerr, S. Logan
Kohlbry, R. L.
Krumholz, Alfred F.
Laughlin, G. C.
Lavallie, A. J.
Lebedeff, Michael N.
Livingston, John
Locum, Edward
Loeffler, G. B.
Long, R. E.
Long, V. K.
Longwell, R. C.
Magan, Thomas I.
Magee, C. M.
Mann, G. T.
Manz, J. W.
Markel, Orville
Marsh, Chas Thomas
Mayer, Frederick C.
McCoy, W. N.
McDonald, Lewis
McFarland, R. E.
McGregor, Carl, J.
Melby, F. C.
Menzies, J. C.
Meyers, L. J.
Middleton, R. J.
Mills, Ellsworth L.
Monroe, L. Chellis
Monroe, R. P.
Morgan, T. W.
Moynahan, George B.
Mueller, George C.
Muller, Jules
Munro, Wm.
Murphy, C.
Nickerson, J. F.
Nordine, A.
Nystrom, Karl T.
O'Day, L. W.
Olson, Herbert
Paul, Joseph
Pearson, Wm. C.
Pelky, Al
Pender, Warren
Penn, Henry
Peterson, M. E.
Pfeiffer, C. L.
Pillsbury, Charles S.
Poston, Claude P.
Powell, William J.
Russell, Theo, J., Jr.
Rutt, Eugene
Ruzich, Joseph S.
Schattel, K. F.
Schipplock, C. J.
Schneider, Charles A.
Schwon, Paul
Seabloom, E. R.
Sheppard, George W.
Sir, Walter W.
Skog, Ludwig
Sloan, John
Smith, Abram E.
Smith, W. W.
Stanley, A.
Stricklen, E. E.
Sullivan, John F., Jr.
Swift, Alden W.
Taylor, J. Hall
Tenney, G. E.
Tholen, M. A.
Thomas, Jack R.
Thomas, Theodore G.
Tolan, Richard L.
Turner, Clyde
Turner, C. A.
Unger, Arthur M.
Vock, Louis G.
Ward, L. E.
Watson, Chas. O.
Weist, Milton M.
Whaley, M. L.
Wiles, John, Jr.
Williams, Bart
Wilson, F. D.
Wilson, R. G.
Wolfe, Jonathan
Wright, John P.
Young, Chas. D.
Zimmerman, Gene H.
Zoll, H. W.

Chicago Heights—
Quinn, E. L.

Chillicothe—
Crouch, Cleer
Schmidt, J. Bernie

Cicero—
Andersen, H. M.
Braun, Ben N.
Drall, George J.
Fassero, Frank
Harbin, Peter L.
Jumbalo, S. J.
Martin, Wm.
Walker, R. W.
Wozniak, Joseph J.

Congress Park—
Peterson, Alvin V.

Cuba—
Turall, Maurice W.

Davenport—
Hampton, Wm. T.

East Alton—
Cunningham, G.
Harris, A. W.

East Moline—
Boyle, Leo W.
Martinson, Edwin O.

East Peoria—
Warnke, Eldon

East St. Louis—
Christie, R. L.

Edwardsville—
Sasek, Fred

Elmwood Park—
Wilhelm, A. J.

Farmington—
Filker, Arthur G.

Glen Ellyn—
Burks, R. H.
Gordon, Charles W.

Glenwood—
Moore, James H.

Hanna City—
Burkett, W. D.

Harvey—
Burgess, Leslie U.
Cody, Louis
Comerford, J. E.
Craig, G.
Dempsey, J. S.
Hatke, H.
Jones, Wm.
Ludtke, Alfred
McPhee, L. S.
Rundquist, Lester H.
Snyder, Carl
Van Iten, M.
Walls, Charles

Hazel Crest—
Lyons, L.

Herrin—
Coleman, Hazen E.
Lindsay, Laverne

Joliet—
Kastman, Lawrence
Marking, Lowell Paul
Perona, Louis A.
Smith, Norman E.

Kewanee—
Powers, R. F.

La Grange—
Blomberg, M.
Downing, H. M.
Henry, J. Robert
Heseltine, A.
Hruska, John H.
Kuehn, Ernest

Maywood—
Fentress, D. Wendell
Lange, Frank
Lytikainen, Elmer John

Melrose Park—
Stuckey, W. A.

Midlothian—
Smith, A. E.

Moline—
Burgston, C. H.
Cornelius, Martin P., Jr.
Cox, A. T., Jr.
Flodeen, Albin H.
Foley, Wm. J.
Gustafson, Arthur R.
Harris, Hugh H.
Reeves, Charles L.
Williams, Gordon T.

Morton—
Anker, Raymond H.

Mt. Vernon—
Brocklebank, P. T.
Collison, Thomas A.
Misker, Francis

North Chicago—
Strong, K. B.

Oak Park—
Lacey, Jim
Pufahl, Hans R.
Steele, Frank
Ward, W. E.

Pekin—
Farris, Howard S.
Rowell, Clarence
Willis, James

Peoria—
Bandy, William
Bartel, Leonard R.
Blome, Henry
Brooking, Walter J.
Calahan, James
Carr, Earl
Cecil, Carl
Dobson, S. W.
Dubie, O. L.
Eldred, S. E.
Getz, Harold
Getz, Robert
Gieszl, Ray
Greenfield, Clyde
Hanley, John L.
Hanson, Vernon, Jr.
Hardbarger, Lyle H.
Hayden, Joe
Heiman, Raymond M.
Hyler, L. L.
Isgren, Elmer
Isgren, Vernon R.
Joublanc, J. C.
King, E. Doyle
Le Tourneau, Richard
Loesch, Al
Lohnes, Stearn
Matheney, Sherman
Matheny, Thomas
McAdams, Paul F.
Miller, Elmer L.
Miskoe, W. I.
Neaveill, Chester E.
O'Neill, John
Pankratz, Peter G.
Reist, J. F.
Roberts, G. R.
Runkel, Alfred
Runkel, R. W.
Rutledge, Robert
Salvador, J. V.
Schieckenburg, Rene
Schlis, Wm. J.
Schmidt, E. C.
Shearburn, W. C.
Sivley, G. A.
Smith, T. A.
Sommer, Phillip
Stoller, Ed.
Thornberg, Al
Troike, Otto
Ulrich, Austin
Van Dyke, R. E.
Wallis, Jess
Watkins, Franklin
Watkins, Harry
Wegner, Russell
Wiese, H. D.
Wimmer, Clifford
Yard, Leroy E.
Ziemer, Clarence

Peoria Heights—
Hampton, G. J.
Hanson, Vernon E.
Price, Percy

Prophetstown—
Rigler, Geo. W.

Riverside—
Habel, B. R.
Kicherer, Harry J.

Rockford—
Andrews, Chas.

Rock Island—
Ashton, Frank W.
Hurlbutt, Robert H.

Roseland—
Seyffert, E.

St. Clair Co.—
Greenspon, A.

St. Paul—
McCarthy, Jim

Thornton—
Diekelman, W.

Urbana—
Bruckner, Walter H
Williams, Robert D.
Wilson, Wilbur M.

Washington—
Becker, A. J.

Winnetka—
Plumley, Stuart

Wood River—
Cummings, R. L.

Ziegler—
Rieger, Arthur J.

INDIANA

Alexandria—
Vogel, Chas.

Brownsburg—
Leonard, Roy

Cedar Lake—
Lee, Earle S.

Chesterton—
Anderson, H.

East Chicago—
Crawford, W. F.
Guirl, H. P.
Joyce, Edw. M.
Kuhn, H. C.
Kumiega, Walter J.
Newton, G. H.
Wang, Ray H.

Elkhart—
Kyler, Donald

Fort Wayne—
Mottar, R. F.

Gary—
Kennedy, Clarence J.
Lenz, John J.

Greencastle—
Alexander, W. E.

Hammond—
Krejci, E. L.
Maley, Geo. D.
McKinzie, Daniel J.
Snider, Robert

Indiana Harbor—
Quigley, James B.

Indianapolis—
Baker, John D.
Beers, Frank
Brant, Robt. P.
Campbell, B. H.
Dickson, R. H.
Doll, F. A.
Eaglesfield, R. D.
Eaglesfield, R. D., Jr.
Franks, Fred
Frazier, Joe
Frenzel, Wm.
Fulkerson, E. V.
Gage, Arthur
Garten, Wm. Ray
Haislup, George F.
Hall, Paul B.
Kick, Karl H.
Kimmel, Fred
Larson, C.
Martin, John E.
Maxwell, W. H.
Miller, Cecil J.
Osterhous, C. S.
Pippel, Donald C.
Rasmussen, Alvin C.
Sauer, Carl M.
Seyfried, L. M.
Spoon, Robert
Tebben, J. D.
Weiger, Joseph A.
Williams, Joseph S.

Lafayette—
Lindley, R. W.

Logansport—
Morris, F. H.

Michigan City—
Logmann, A. A.

Mishawaka—
Schmidt, Geo. W.

Muncie—
Crapo, Fred M.
Reynolds, Nolan

Newcastle—
Lowe, L. D.

Peru—
Volpert, Wm.

Princeton—
Woods, Wylie

West Lafayette—
Throckmorton, Ralph E.

IOWA

Ames—
Hainer, Linton

Bettendorf—
Gallagher, C. S.

Cedar Rapids—
Choate, R. E.
Malek, Henry
Wilson, R. A.

Clinton—
Clark, Lewis S.

Davenport—
Carle, W. J.
Meyer, Fred G.

Des Moines—
Boian, Wilbur O.

Earlham—
Lush, Chas. B.

Marshalltown—
Woolard, Harry

Mason City—
Barclay, Paul V.

Paton—
Ladd, Wendell

KANSAS

Augusta—
Bates, Harvey C.
White, Roy

Eureka—
Jones, Paul

Humboldt—
Spencer, Coleman J.

Hutchinson—
Ball, Harry W.
Kopper, H. D.

Iola—
Berning, Harold J.

Kansas City—
Brian, J. K.
Gosney, James
Hall, J. A.
Jones, Howard
McDonald, V. G.
Meyers, A. M.
Mika, H. L.
Pollard, T. J.
Rice, S. B.
St. John, L. R.
Streeter, J. Reel
Williamson, George F.

Lawrence—
Sluss, A. H.
Smith, V. M.

Leavenworth—
Jones, C. S.

Maize—
Van Horn, V. C.

Manhattan—
Carlson, W. W.
Ladd, R. J.
Lynch, D. E.

Otis—
Kerbs, L. E.

Ottawa—
Garrison, Paul R.

Overland Park—
Cramer, L. L.

Oxford—
Conwell, Fred

Paola—
White, Merle W.

Pittsburgh—
McCormick, Don E.

Potwin—
Nellans, H. M.

Stockton—
Hus, S. F.

Sun City—
Froman, Hugh C.

Topeka—
Moore, J. R.
Singleton, Jack

Wichita—
Arington, L. G.
Armstrong, R. M.
Brotherton, Earl S.
Bruer, C. B.
Cardwell, H. W., Jr.
Dickens, W. N.
Dickerson, R. D.
Dunham, J. H.
Easton, W. J.
Ellison, Ralph
Galley, John
Garberson, D. D.
Headrick, B. C.
Helvey, Oren C.
House, H. T.
Houser, K. O.
Kinder, G. P.
Kirk, L. E.
Lackey, Walter
Love, R. H.
McFaddin, M. D.
McMillan, J. H.
Melzarek, C. A.
Miller, W. G.
Minard, Herbert
Moore, W. P.
Reschke, F. Paul
Schreffler, Chas. L.
Sheets, C. L.
Stepleton, P. W.
Swenson, D. A.
Townsend, R. L.
Vrooman, A. J.
White, Dan
Wilhite, J. B.
Worden, Robert L.

Winfield—
McClung, A. F.

KENTUCKY

Ashland—
Cunningham, J. B.

Erlanger—
Gornall, E.

Florence—
Barker, George

Louisa—
Berry, Elmo

Louisville—
Arterburn, C. B.
Fritsch, R. E.
Gumm, Eugene C.
Hawkins, Jesse N.
Hicks, Gus H.
Lewis, Theodore
Martin, S. B.
Merkt, Joseph
Mivelaz, William A.
Moore, R. S.
Plinke, G. W.
Powers, H. C.
Romann, John H.
Smith, Cecil E.
Wallace, H. C.
Watt, J. R., Jr.
Wright, Louis W.

LOUISIANA

Baton Rouge—
Durkee, E. Leland
Elbourne, C. H.
Smith, Charles F.
Smith, Everett G.

Diamond—
Brandt, H. L.

Happy Jack—
Bonneval, L. Robert

Myrtle Grove—
Barrois, Nelson P.

New Orleans—
Abaunza, A. E.
Barrios, E. H.
Bell, Mace H.
Clothier, A. L.
Earle, Harry J.
Guillot, A. H.
Gust, Louis E.
Helper, H. H.
Higgins, E. C.
Huet, G. O.
Leaveau, Victor A.
Pine, Clyde
Smith, Paul E.
Stout, Arthur
Wishart, Wm.

Port Sulphur—
Bonneval, M. E.
Corne, E. J.
Ferree, E. B.
Harmon, W. R.
Ledet, E. J.
McLaughlan, O. B.
Stockstill, Prentiss G.

Shreveport—
Stevens, James S.

MAINE

Bath—
MacKinnon, Norman J.

Portland—
Henderson, H. F.

Sanford—
Murray, Stephen W. G.

Waterville—
Colby, Fred C.

York Village—
Irons, James D.

MARYLAND

Annapolis—
Ronay, Bela M.

Baltimore—
Allen, Herschel H.
Ballantyne, W. M. H.
Barkow, A. G. L.
Beck, William
Blakey, Robert M.
Boetcher, Hans Nielsen
Broski, M. Chester
Burggraf, Fred
Canty, T. A.
Christensen, L. H.
Cumberland, John C.
Dorsey, J. Richard
Gallagher, Chas. J.
Halas, John
Hewett, Rexford J.
Higgins, Nathan B.
Hilbinger, C. N.
Jankowiak, Leo
Jenkins, Alex F.
Jensen, Holger
Knight, A. Rhodes
Lahn, Sydney
Lang, Philip George
Lengerhuis, Otto
Maher, J. William
Mansfield, Roy A.
Michel, Clarence H.
Ortman, Edw. F.
Phillips, Chas. J.
Saxe, Van Rensselaer P.
Schafer, Philip E.
Scheerer, Carl F.
Schuler, George
Selden, Edward W.
Shook, Henry W.
Sibley, R. L.
Stark, Guy F.
Thompson, Arthur C.
Wicker, M. G.
Wynn, Arthur R.
Zouck, G. H.

Bethesda—
Brush, Charles C.
Kingsbury, Jesse A.

Catonsville—
Fitzgerald, Robert
Gathmann, Emil

College Park—
Mowatt, Theodore A.

Dundalk—
Humberstone, J. H.
Larsen, Robert
Lutes, E. B.
Miller, Dr. John W.

Ferndale—
Hudnall, John W.

Glen Echo—
Mallory, Walter S.

Hagerstown—
Wingert, Byron E.

Havre de Grace—
Steele, Maurice G.

Hyattsville—
Jefferson, T. B.

Parkville—
Blanton, John W.

Silver Spring—
Bramhall, Charles H.
Toner, H. J.

Sparrows Point—
Birkholz, John
Miller, V.
Ross, Harry H.

Takoma Park—
Jackson, C. E.
Ladd, Robert N.

Towson—
Adams, C. H.
Taylor, James E.

MASSACHUSETTS

Allston—
Dand, Raymond
Spooner, Leonard G.

Amesbury—
Casavant, Aime N.

Athol—
Thayer, Frank L.

Belmont—
Rowsey, Chas. A.

Beverly—
Horgan, Patrick J.

Boston—
Appel, Leonard A.
Austin, Herbert G.
Barnes, William H.
Canty, J. P.
Carlin, L. C.
Chitro, John
Combs, A. L.
Dahlgreen, R. F.
Davis, F. W.
Delbridge, C.
Doherty, E. R.
Faden, James L.
Fennell, Le Barron J.
Humble, John S.
Idell, Percy C.
Jackson, L. F.
Kenney, A. A.
Konetchy, Edward V.
Lockman, Edward L.
Mehaffey, Frank B.
Morris, A. P.
Norris, Edward W.
Norwood, Edgar A.
Peabody, Harold P.
Plaisted, Walter A.
Raney, James E.
Raymond, H. Kendall
Singleton, G. R.
Steward, Harry M.
Stolba, Edward F.
Strathdee, Wallace B.
Sweet, Herbert A.
Tapp, George P.
Whitney, J. T.
Young, Perrin W.

Brookline—
Miller, David
Morgan, J.

Cambridge—
Anderson, Arthur R.
Austin, William M.
Bullock, H. R.
Feyling, P. L. F.
Haertlein, Albert
Jefferson, John A.
Kyle, Peter E.
Moody, Chas. G.

Charlestown—
Donovan, James
Mooney, John A.

Dorchester—
Slater, S. J.

East Longmeadow—
Westfall, John W.

Everett—
Bengston, Nils B.

Fitchburg—
Birnie, Albert W.

Hingham—
Ovaska, Walter A.

Holyoke—
Marran, Vincent P.

Hopedale—
Taft, Harold G.

Jamaica Plain—
Giduz, Fred J.
Kroll, Victor J.

Lawrence—
Demars, Arthur J.

Lowell—
Callahan, J. Walter

Lynn—
Davis, Fred
Gibbs, Louis T.
Gilcreas, Walter F.
Purdy, Charlton V.
Richardson, George
Speed, Francis H.
Thomson, Malcolm
Yeadon, John R.

Malden—
Lee, Daniel A.
Neary, Arthur T.

Mattapan—
Swim, E. H.

Melrose—
Shaw, Earston L.

Nahant—
D'Arcy, Raymond A.
MacQueen, Ernest

Newton—
Whitley, Wilson
Woodrough, Kendal A.

North Billerica—
Swanson, Russell

North Weymouth—
Dubois, Edward

Pittsfield—
Gilbert, Ralph A.
Hopley, David
Lipschitz, William
Meyers, E. J.
Potter, Everett F.
Reed, Walter C.
Somerville, G. G.
Unger, Magnus
Weldon, J. J.

Quincy—
Brummitt, P. W.
Debes, E. D.
Ffield, Paul
Henley, Richard
Hogaboom, Allen G.
Moore, Alfred M.
Rechtin, Eberhardt
Rober, Chester A.
Sherriff, James
Smith, John
Winship, Robert H.

Randolph—
Wescott, George A.

Rockland—
McCarthy, Wm. C.

Salem—
Mason, Frederick D., Jr.

Saugus—
Boyle, Richard A.

Sharon—
Jones, Warren
Mower, D. A.

Somerville—
Lundgren, James E.
Mingotte, Eugene

South Boston—
Cooper, Sidney B.
Gordon, Leo

South Weymouth—
Ruel, Herbert N.

Springfield—
Knox, J. Winslow

Swampscott—
Burnett, Clarence

Wakefield—
Churchard, Chas. F.
Gove, Lewis P.
Rugg, Philip N.

Watertown—
Warner, William L.

Westborough—
Johnson, Carl C.

West Lynn—
Roberts, William

West Medford—
Coombs, Anthony S.

West Roxbury—
English, Richard C.
Silver, Roy H.

West Somerville—
Gray, Henry F.

Weymouth—
Costello, Thomas A.

Wollaston—
Rusky, Nicholas

Worcester—
Adams, Ernest N.
Elliott, Edward
Coster, Charles H. E.
Green, V. Wayne
Hall, Preston M.
Kerr, George M.

MICHIGAN

Albion—
Lawler, Dan

Allen Park—
Zorn, Wm. H.

Ann Arbor—
Dow, William G.
Spindler, W. A.
Telfer, Wm.

Bay City—
Taylor, E. W.

Benton Harbor—
Ross, D. H.

Dearborn—
Foss, Earl H.
Horne, Marvin

Detroit—
Allen, Terrance W.
Anderson, Walter
Bailey, Robert P.
Brown, Alexander C., Jr.
Brown, Edgar Burr
Brueckner, Julius R.
Calley, Joseph
Centers, C.
Clark, L. W.
Collom, Cletus J.
Corey, Donald H.
Coryell, Howard S.
Crigger, John
David, Wm
Dodge, Paul
Doud, H. P.
Douglass, Arthur S.
Eksergian, C. L.
Elliott, Thomas C
Fetz, A. F.
Flynn, C. F.
Friebel, G. J.
Frost, L. H.
Glover, Victor
Goodspeed, Elvin S
Harvey, S. M.
Hayes, William M.
Heckman, Homer H.
Heron, S. D.
Hoern, J. H.
Holtzhouse, Guy A.
Honey, Edward M.
Honhart, Jack C.
Jackson, H. A.
Keyes, J. W.
Kice, M. S., Jr.
Lane, Arthur L.
Le Grand, Rupert
Lewis, Frank
Maire, Stephen F.
Matte, Joseph, Jr.,
Moran, Thomas P.
Nichols, Leonard E.
Peattie, Wm. W.
Peters, Lewis J.
Phelps, G. E.
Phillips, C. E.
Reid, Vaughan
Riback, Emil S.
Robinson, James M.
Rousseau, E. J.
Schimpke, Albert J.
Shelton, E. P.
Sherman, Wm. F.
Sieger, George N.
Simmons, Walter H.
Smith, Otis L.
Sonneborn, Arthur B.
Stasz, John
Stone, Edward
Thomas, James
Urquhart, Thorton
Vrooman, G. C.
Weihe, Cal.
Welcomb, C. D.
Wetherby, B. R.
Woodside, F. Lloyd
Woofter, H. A.

East Lansing—
Hedrick, Leslie C.

Flint—
Jedlick, Charles L.

Grand Rapids—
Duesing, Ernest
Howell, A. J.
Layman, R. D.
Monahan, Joseph

Holland—
Bonge, Albert J.

Houghton—
Young, Almon P.

Huntington Woods—
Lewis, Alfred H.

Jackson—
Dissmeyer, Edward F.

Kalamazoo—
Aldrich, Robert H.
Stander, E. R.

Lansing—
Puffer, Harold P.

Monroe—
Stoner, J. D.

Muskegon—
Schramm, C. H.

Plymouth—
Bieszk, Anthony

Pontiac—
Austin, Byron B.

River Rouge—
Brendle, Russell W.
Bronson, Charles
Stewart, C.

Royal Oak—
Allen, Guy F.
Cramb, D. L.

St. Joseph
Dunn, U. S.

Saginaw—
Jarman, Donald
Stanulis, Joe S.

MINNESOTA

Duluth—
Bartter, Ivan R.
Blodgett, Omer

International Falls—
Knudson, M. N.

Le Sueur—
Baulcke, Chester H.

Minneapolis—
Adler, Robert
Akins, Clifford M.
Beilke, Martin
Bjornstad, C. I.
Brown, Walter V.
Caswell, Alexis
Chatfield, Howard
Dahlquist, C. E.
Danner, W. A.
Davis, Wilbert
Ekblad, Axel H.
Foley, W. J.
Forseth Geo. O.
Gaedy, Clarence
Gander, Aksel
Good, W. Phillips
Graneman, F. W.
Hammond, W. R.
Hayward, C. L.
Hoglund, Clarence E.
Hudoba, Louis
Hughes, Thomas P.
Irwin, George W.
Johnson, Amos
Klass, Fred
Kvasnicka, Wesley
Louis, Harry
McLean, D. G.
Mikulak, John
Moore, O. F.
Murphy, W. E.
Nelson, John
Newton, Robert
Ovestrud, Melvin
Potter, Morgan H.
Pugh, Guy L.
Rabenau, Frank
Riebeth, C. E.
Robbins, Harris H.
Robinson, Richard W.
Sherwood, J. G.
Steinmetz, Leslie
Sworski, Stanley F.
Tomczyk, Joseph
Tritchler, W. C.
Warmington, Thomas J.
Watson, G. V.
Weisgerber, Les
Williams, Jack E.
Wilson, C. R.

Nashwauk—
Tuomala, J. Evald

Robbinsdale—
Le Jeune, L. L.

St. Paul—
Barron, J. H.
Bauerfeld, Jack E.
Bollenbach, W. M.
Borne, Floyd O.
Comfort, C. E.
Grohs, Wm. H.
Hill, Gladstone
Janovec, Edward
Kelleher, T. F.
Kenny, L. T.
Kilbane, James, Jr.
Lindeke, Harold A.
Nystrom, Carl
Paschke, George H.
Sage, V. L.
Schroeder, Emil C.
Schulz, William H.
Shoultz, Wm. L.
Spaulding, Roy L.
Swanson, George
Westin, Edward

South St. Paul—
Andrus, B. S.

MISSISSIPPI

Pascogoula—
Hubbard, Ronald P.

MISSOURI

Carrollton—
Allison, Edgar T.
Carewe, Patrick J.

Fulton—
Fairchild, Frank

Grandview—
Hosler, H. C.
Knapheide, E. W.

Hickman Mills—
Williams, Leslie N.

Independence—
Gore, Burney F.
Short, E.

Jefferson City—
Bower, Cecil L.
Kelling, Scott
Sinn, Joseph

Kansas City—
Anderson, B. A.
Anderson, E. D.
Anderson, Wm. M.
Ashton, N. L.
Bain, George
Berg, Fred
Brown, Leland
Burge, R. G.
Camp, C. L.
Chapman, Leo
Clark, W. R., Jr.
Cross, L. H.
Darby, H. C.
Dickins, G. F.
Dishman, Chas. H.
Doll, Theodore
Donegan, Charles E.
Edwards, J. Preston
Fizzell, James L.
Haggard, Henry W.
Havens, Harry L.
Hurt, A. S., Jr.
Kelsey, Howard C.
Kirk, James P.
Koons, R. E.
Kunert, Max J.
Kurtz, W. H.
Libby, H. H.
Luthy, S. F.
Mika, George
Morgan, Lester
Nemecek, A. A.
Peterson, Carl
Reals, Chas. L.
Schlup, Bernard L.
Smithers, Joseph
Sorkin, Josef
Swaffar, Howard J.
Tarbell, James H.
Tibbets, R. J.
Woodman, C. E.

Lee's Summit—
Buxton, M. L.

Louisiana—
Turner, Edward G.

North Kansas City—
Frick, C. W.
Schmidt, Karl W.

Orrick—
Majewski, Paul M.

Overland—
Miller, H. J.

Richmond Heights—
Schwarz, Arthur

St. Joseph—
Holley, E. C.
Michel, H. A.

St. Louis—
Allina, Alfred
Bertsch, John A.
Bland, George O.
Bleikamp, C. E.
Bosse, E. M.
Brumbaugh, I. V.
Close, H. C.
Dodd, Leslie H.
Enslin, E. M.
Fantz, F. C.
Fantz, Fred D.
Feldhaus, Frank J.
Garner, F. R.
Gettinger, Ralph
Gill, Harry
Griesel, George
Hamlet, L. M.
Hill, Lockwood
Hoffman, Lester O.
Hooper, Wyllys G.
Howery, Theodore E.
Hurst, William I.
Joslin, E. W.
Kellogg, Wm. Pitt
Knachstedt, M. L.
Kohlbry, F. P.
Leonard, B. H.
Long, Richard
Lutz, John G.
Meyerson, Milton E.
Moran, Robert
Moss, N. F.
Netchvolodoff, V. V.
Pahmeyer, Fred O.
Pennewill, G. W.
Plane, John C.
Reeves, Eugene L.
Ritchie, Richard W.
Rosborough, J. G., Jr.
Ross, A. R.
Royer, J. W.
Ryan, Robert J.
Sacks, J. Raymond
Schenier, Walter M.
Schwarting, H. F.
Scioletich, Mathew
Sheaff, H. H.
Skinner, M. G.
Streett, James D.
Thomas, R. H.
Thumser, R. C.
Todd, Floyd
Webb, Eugene
Wehmeyer, Clarence W.
Wilkinson, O. F.

St. Louis County—
Jackson, Earl T.
Stupp, Norman

University City—
Schade, Wilbert C., Jr.

MONTANA

Billings—
Lamach, John
Wolfe, H. W.

Bozeman—
Homann, F. C.

Fort Peck—
Browning, Roy T.
Capper, Chester W.
Carpani, Peter J.
Galloway, William M.
Hellstern, Earl
Rumfelt, Henry F. C.
Strissel, Daniel

Glasgow—
Marchinek, Fred E.

Gohagen—
Jacobsen, Carl

Helena—
Hare, L. Fletcher

Nashua—
Adolph, F. Chan

Polytechnic—
Martin, Charles E.

Sunburst—
Andrews, James

Sweet Grass—
Juedeman, R. F.

Wheeler—
Griffith, Harry S.

NEBRASKA

Lincoln
Bakshas, Sam B.

Omaha—
Engler, Paul E.
Fuchs, B. L.
Hamby, P. W.
Johnson, A. B.
Knox, Charles
Larsen, Elmer
McGrath, G. E.
Mullane, Clinton W.
Pflasterer, C. R.
Reiff, Stanley G.
Schneiderwind, L. O.
Semik, Charles A.
Shiner, George E.
Stocking, Kenneth O.
Thomas, Lester B.
Thrapp, George
Vollmer, Henry G.
Young, E. D.

NEVADA

Ruth—
Blackmore, F. L.

NEW HAMPSHIRE

Claremont—
Ericson, Carl B.

Keene—
Hill, Gale E.

Nashua—
Le Bel, Everett M.

Portsmouth—
Endlich, P. J.

NEW JERSEY

Arlington—
Carr, James H.

Bayonne—
Costello, John J.
Frohlin, John
Kihlgren, Theodore E.
Laurinatis, John, Jr.
Massa, William J.
Moir, Stewart G. H.
Robertson, R. E.
Schoener, J. G.
Spicer, Kenneth M.
Stanfield, R. S.

Belleville—
Burghoorn, Leendert
Goodford, Jack A.

Belvidere—
Welker, Ted

Bloomfield—
Obreiter, Robert B.
Pursell, Robert
Young, Charles R.

Burlington—
Ireton, Samuel R., Jr.
Osmin, Basil

Camden—
Frey, Chas. A.
Griffith, W. G.
Pierce, Harry W.
Stepath, Myron D.

Carteret—
Evans, W. L.
Hila, John
Lonsdale, Wm.
McKay, Edwin O.

Clifton—
Blind, J. George

Cranford—
Albans, Thos. A.

Dunellen—
Lukas, J. C.

East Orange—
Grow, H. J.

Elizabeth—
Bauer, Leo
Card, Harold S.
Dalcher, L. M.
Elly, Robt. D.
Fyke, F. C.
Hodges, Charles
Holder, G. C.
Loane, P. M.

Englewood—
White, Bernard L.

Grantwood—
Eveleth, Frederic V.

Harrison—
Emery, Willard
Moore, L. M.
Sidney, C. Watson

Hasbrouck Heights—
Schnetzer, S.

High Bridge
Gezelius, R. A.

Hillside—
Bott, Harry B.
Humm, E. F.

Hoboken
Drake, H. C.

Irvington—
Frantzen, Chell

Jersey City—
Anderson, James L.
Begtrup, E. F.
Crowe, John J.
Deming, George M.
Doyle, James H.
Fisher, O. W.
Hopkins, R. K.
Kidd, Alexander
La France, B. A.
Landis, Houston E. Jr.
Levin, Nathan
McNutt, Louis C.
Pendlebury, A. H.
Rippel, J. N.
Shields, W.
Smith, Stephen
Smith, B. Thomas
Sylvester, Walter G.

Kearny—
Aborn, R. H.
Ohlssen, Wm.
Powell, R. E.
Schaffter, G. A.

Linden—
Andrews, F. H.
Petrusky, Jos. W.
Planeta, A. W.

Maple Shade—
Doering, Walter

Metuchen—
Andersan, Karl M.
Gibson, C. C.
Hampton, H. D.
Hiecke, W. K.
Selvatico, D. M.
Stanek, A. J.

Milltown—
Johnson, Lloyd C.

Millville—
Romanik, Mike

Montclair—
Llewellyn, F. T.
Wilson, R. M.

Mountain Lakes—
Borcherdt, Walter O., Jr.

Newark—
Gasiorek, John C., Jr.
Gorzelnik, Edward M.
Hilse, Fred
Holslag, C. J.
Lord, Arthur S.
Mattern, P. M.
Moss, Herbert H.
Payne, Burt. H.
Salmon, Philip A.
Schlitz, Thomas R.
Zieger, Max

New Brunswick—
Troger, H. J., Jr.

New Market—
Miron, John

North Bergen
Aschenbrenner, F. J.
Heller, J. M.
Scherrer, A. G.
Stewart, S. S.
Tisza, E. E.

Oaklyn—
House, Alan

Orange—
Britten, Clarence R.
Hoffman, Edward H.

Palisade Park—
MacDonald, Roderick
Mosley, A. Robert

Park Ridge—
Orr, Nelson J.

Paterson—
Ennis, H. V.

Perth Amboy—
Boniewski, Edward F.
Sprague, A. G.

Plainfield—
Halliburton, M. R.
Phillips, J. T.

Ridgefield—
Weinert, G.

Ridgewood—
Paulson, Frank O.

Roselle—
Mitchell, Fred E.

Rutherford—
Hodge, Thomas
Place, George
Taylerson, John E.

Springside—
Lumadue, C. H.

Swedesboro—
Shaw, Franklin B.

Teaneck—
Forgett, V.
Rossheim, David B.

Trenton—
Swan, Harry S.

Union—
Lum, S. Clark

Weehawken—
Blickman, Saul

West Collingswood—
Cotton, Charles G.

Westfield—
Boyd, Douglas E.
Flocke, Frank G.
Grove, Wm. G.
Scharwenka, A. V.
Stallone, Leonard

West Trenton—
Jones, R. P.

Wharton—
Rusch, Charles E.

Woodbridge—
Elliott, Joseph

Woodbury—
Willis, John G.

Wortendyke—
Broadhurst, Chas. T.

NEW MEXICO

Eunice—
Wildman, Oliver

NEW YORK

Albany—
Brickley, Earl M.
Browne, R. L.
Carpenter, John
Ramer, E. J.
Stiglmeier, Albert F.

Alexander—
Murta, Clarence

Auburn—
Walker, George

Babylon—
Goppoidt, P. R.

Ballston Spa—
Van Aernem, Ralph E.

Batavia—
Dial, Donald
Fiero, Austin
Glover, Walter G.
Langen, Wm. E.

Beacon—
Bolaskey, Frank

Bellaire Park—
Klos, Clifford H.

Bergen—
Cook, Willis E.

Binghamton—
Rogers, Frank D.

Buffalo—
Bard, J. K.
Blakely, R. A.
Bollin, Edward F.
Brand, E. A.
Freeman, Chas. S.
Galbreath, Emil
Gillespie, J. W.
Howard, Frank O.
Korn, M. P.
Mason, J. H.
Meyer, Amel R.
Munschauer, George E.
Patterson, Don W.
Rodgers, F. L.
Schleich, Randall
Schliecker, G. H.
Schlieder, H. J.
Siemer Robert
Slosson, John
Weller, R. C.
Wiesner, Arthur J.

Cattaraugus—
Mosher, Leland C.

Churchville—
Schur, Robt. J.

Clarence Center—
Kjeldgaard, Andrew, Jr.

Cohoes—
Waugh, J. E.

Corning—
Kaukeinen, R. M.

Dansville—
Bick, Richard R.
Dick, Walter G.
Martin, N. N.
O'Neil, John J.

Dunkirk—
Boswell, E. N.
Gruenberg, J. W.
Redline, Ralph H.

Eggertsville—
Bos, Jr., John

Elma—
Speller, Thomas H.

Elmira Heights—
Patterson, John K.

Endicott—
McNutt, A. D.

Flushing—
Adelson, S. S.

Freeport—
Biedermann, Harry

Fulton
Haskel, Ben

Hempstead—
Shushansky, Hyman

Ithaca—
Ames, W. B., Jr.
Hollister, Prof. S. C.

Jamesville—
Lang, P. A.
Ochsner, Edward

Kenmore—
Bell, John L.
Koerner, Harry

Kew Gardens
Tolman, Lee P.

Kingston—
Ashley, Charles H.

Lindenhurst—
Chivvis, George D.

Little Falls—
Barnes, Harold N.

Little Valley—
Worth, Sid. L.

Long Beach—
Hamer, T. M.

Long Island—
Axline, R. A.
Borner, Fred
Borst, W. H.
Buonasera, Albert
Connelly, Peter K.
Fitzpatrick, John
Goodman, Robert
Hallen, M.
Isenburger, H. R.
Kandel, Charles
Klevens, J. A.
Lilienthal, F. W.
Myhre, L. O.
Osborne, T. W.
Pitou, Eugene
Rooney, J. I.
Scott, Carlyle, H.
Whaley, B. L.

Maspeth—
Rahtz, Arthur J.

Mt. Vernon—
Arthur, Guy, Jr.
Pearson, Raymond C.
Steck, Robert C.
Snyder, George V.

New York City—
Adams, John
Amundsen, R. G.
Anderton, James T.
Applegate, M. W., Jr.
Armstrong, Charles H.
Arnott, David
Aumann, Henry
Baumann, Henry R.
Bearor, Theodore
Becker, Edward C.
Bedworth, Robert E.
Bellamy, Benjamin
Bennett, William
Bleecker, Kenneth
Boggs, R. W.
Booth, Henry
Bourgman, V.
Bryan, Charles W., Jr.
Bulkley, Harold F.
Bull, G. N.
Burns, Wm. T.
Callahan, John F.
Cameron, A.,
Carpenter, Allan W.
Carter, Glenn O.
Cash, Lester M.
Cassidy, P. R.
Cavagnaro, John, Jr.
Cavanagh, R. F.
Chapin, Richard N.
Claussen, Gerard
Cohen, F. W.
Connell, Fred
Critchett, James H.
Crosett, A. D.
Cullen, Lewis J.
Dalcher, John T.
David, E. V.
Day, C. L.
De Golyer, A. G.
De Pew, T. E.
Deppeler, J. H.
Dierckx, Jules
Donald, Russell S.
Doyle, E. A.
Edelson, L.
Eder, F.
Edwards, James L.
Ewertz, E. H.
Farmer, F. M.
Febrey, H. H.
Feiner, Mark A.
Fetherston, Thomas C.
Fish, Gilbert D.
Floeting, Edward R.
Fowler, E. W.
Frankland, F. H.
Fraser, O. B. J.
Fraser, Robert H.
Friedrich, Doctor W.
Gailor, C. F.
Galloway, R. Theron
Gannett, J. K.
Gaynor, A. E.
Gelderblom, A. J.
Gibson, C. D. W.
Gilbride, J. T.
Gobus, Alexander
Graham, R. R.
Greene, T. W.
Greenewald, E. L.
Grover, LaMotte
Haag, J., Jr.
Hagstrom, T.
Haile, William M.
Haller, P. Alfred
Hasler, Thomas
Havens, Wm. W.
Helmkamp, R. F.
Henriques, H. F.
Higbee, Clarence W.
Hoenighausen, John
Hoffberg, William Arnold
Holland, S. Emil
Holwill, Claude N.
Hovey, O. E.
Hovey, Walter F.
Howard, Wayne A.
Hurley, William
Irvine, Vess E.
Iwasaki, Massahide
Jacobus, David Schenck
Kehl, R. J.
Keir, James M.
Keller, R. B.
Kennedy, R. E.
Keogh, A. F.
Kerbey, E. A.
King, Frederick J.
Kinzel, A. B.
Kissock, Alan
Kugler, Arthur N.
Laine, Frank
Lamb, H. C.
Lavine, G.
Lavoy, Louis
Lindquist, Albert K.
Loeser, John C.
Loiselle, Edward
Lowrie, William
MacSorely, William W.
Maeurer, F. J.
Magrath, Joseph G.
Maver, Steve
McClintock, Harvey C., Jr.
McCracken, Edwin D.
McCune, Charles A.
Mever, C. G.
Milligan, R.
Mitchell, William
Moisseiff, Leon S.
Morehead, F. H.
Morgan, J. B.
Morris, E. D.
Mortensen, Leonard R.
Morton, A. F.
Muir, Jas. A.
Naes, Birger
Obert, C. W.
Oehler, A. G.
Oelhaf, Carl
Oliver, Frank J.
O'Reilly, Eugene J.
Ostrander, F. T.
Palmer, Walter
Perreault, John B.
Piasecki, Stanley
Pieper, James F.
Pino, George B.
Place, I. W.
Price, Paul L.
Priest, B. B.
Quasdorf, H. C.
Randall, John F.
Reinhard, Herbert F.
Richards, Nathaniel A.
Ridgway, Herbert
Roberts, D. E.
Rockefeller, Harry E.
Rogers, C. E.
Rogers, Fred E.
Rogers, H. L.
Roper, Edward H.
Rosencrans, Julian
Rowland, W. B.
Russell, Bruce A.
Ryder, E. M. T.
Schmidt, Edward J.
Schmuller, Frederick M.
Schneider, George
Scott, Charles H.
Sheffer, John S.
Slottman, George V.
Smith, Alonzo
Smith, H. Sidney
Smith, Martin M.
Smith, Melvin
Smith, William R.
Spielvogel, Siegfried W.
Sporn, Philip
Stewart, R. W.
Stewart, W. E.
Stillman, Ralph K.
Strauss, Jerome
Sussman, H.
Svendsen, Einar
Swain, Philip W.
Sykes, George
Tangerman, E. J.
Thomsen, S. Henry
Thomson, David
Tickle, A. B., Jr.
Tinnon, John B.
Townsend, J. R.
Ullmer, Herman
Urbach, L.
Valdes, Joseph
Van Alstyne, G.
Van Buren, Miles H.
Vanderbeek, H. A.
Vom Steeg, E., Jr.
Wagner, Gerhard
Weigel, A. C.
Wheatley, John G.
Wikoff, Alan G.
Wilson, J. Lyell
Wunder, Leon
Yasines, S. Franz
Yates, Ira B.
Young, Shang Shoa
Zimmer, Rudolf
Zimmerman, John H.
Zullo, Basil

Niagara Falls—
Dawson, J. R.
Fish, J. Arthur
Hettler, Wm. M.
Howell, W. H.
Huse, Harold M.
Kozar, Leon C.
Miller, Wilber B.
Misener, G. L.

Norwich—
Fancher, A. C.

Oyster Bay—
Weekes, James H.

Palmyra—
Blazey, John S.

Patchogue—
Alexander, Chas. E.

Pittsford—
Welch, Lyman

Poughkeepsie—
Cooke, A. E.
Hart, F. H. M.

Richmond Hill—
Dickau, Henry H.
Mott, Clement
Wagner, Carl A.

Rochester—
Agusto, John
Allan, Edwin
Ashcroft, Robert B.
Auld, Samuel
Barker, J. A.
Barker, P. F.
Barlette, Anthony
Belli, Raymond
Brown, Henry H.
Budds, George W.
Carey, Chas. D.
Cattanach, Robert L.
Chauncey, Clarence
Clifford, Jerry
Conley, Wm. V.
Currie, John W.
Decker, Walter J.
De Lice, Peter A.
Duemmel, Frank
Dwyer, Edward
Eckberg, A. R.
Ecklund, Paul A.
English, Harold W.
Fontana, Leon F.
Fredene, P. J.
Gaylord, T. S.
Goeltz, Philip
Grant, R.
Habel, Edward G.
Hand, Everett L.
Huntley, W. B.
Hutchison, Robert
James, Paul W.
Jansen, Charles
Jones, Emanuel
Kauffmann, Eugene A.
Kazoroski, W.
Kerr, Edward L.
King, Harold
Klick, Alfred J.
Knopf, Paul
Larson, Ivan E.
Lasaponara, George
Laske, Frank
Le Cain, G. H.
Lenahan, Vincent
Litzenberger, W. M.
Luther, Wm. F.
Marr, A. Philip
Martin, Clyde K.
Martindale, Lloyd R.
Mather, William
Matthies, Arthur
McClelland, R. L.
Meredith, A. B.
Meston, John
Milaty, T. M.
Miller, Edward L.
Mitchell, H. F.
Morgan, W. Trefor
Mostyn, Henry J.
O'Neil, Edgar
O'Rorke, Joseph
Owen, Earl V.
Peiffer, Fred L.
Phaff, James
Pifer, Wm.
Prescott, Leonard P.
Richter, G.
Schillinger, Charles F., Jr.
Schmitt, William J.
Schwartz, Harry
Sears, Clifford M.
Steele, Roy M.
Smith, Thomas E.
Sparr, Emil
Stelljes, Gomer
Thompson, Bruce
Tysick, J. E.
Van Epps, W. B.
Welke, Elmer
Wendelgass, Andrew J.
Weston, Kenneth
White, Alfred
Wills, John A.
Wilson, G. G.
Yehle, H. C.

Rome—
Wilkins, Richard A.

Rosedale—
Thompson, Thomas

St. Albans—
Scott, S. S.
Spraragen, William

Schenectady—
Aumiller, W. L.
Buckley, Chas.
Churchill, William W.
Clark, Roger W.
Cochrane, A. G.
Conway, Frank
Durham, Clyde
Edward, Edward J.
Gillette, Robert T.
Haughton, F. A.
Hicks, Jesse F.
Hobart, H. M.
Hutchins, W. C.
Jesperson, Chas.
Kortz, Howard R.
Leveen, Laurence R.
Lipsky, Joseph J.
Mann, Vernon E.
McAuliffe, J. J.
Meeker, Lincoln D.
Miller, F. Harold
Morenus, D. M.
Neal, Frank C., Jr.
Near, A. E.
Nigriny, Victor
Owen, N. W.
Palmer, Robert
Paparella, Michael J.
Paulsen, E. E.
Pelton, Ralph S.
Rash, William C.
Redmond, James H.
Reed, Clifford H.
Ross, George Arthur
Sayre, Mortimer F.
Scott, Stephen M.
Seely, R. W.
Sheldon, Lucian A.
Spittler, Eugene R.
Stevens, A. C.
Vogel, Andrew
Westendarp, H. O., Jr.
Wilke, R. H.
Willey, Raymond C.
Wilson, Wayne E.
Wood, A. P.
Wyer, R. F.

Scio—
Markham, Robert

Scotia—
Binck, Arthur S.
Catlett, James T.
Kruse, Harvey H.
Kuhlberg, Carl F.

Sea Cliff—
Senesky, John S.

Snyder—
Mohring, Robt. F.

Staten Island—
Boland, F. J.
Brass, Eugene
Dimelow, Harry K.
Northcutt, Harold W.
Russell, Kenneth
Smith, Philip

Syracuse—
Bentley, Harold P.
Beyreuther, Robert
Brown, Jesse J.
Mehl, Walter A.
Patterson, Clarence Thos.
Roscoe, John S.
Wise, Grover L.

Tarrytown—
Reynolds, Joseph T.

Tonawanda—
Gaines, John M., Jr.

Troy—
Clark, Le Roy W.
Hess, Wendell F.
Stockholm, Seth R.
Wyant, Robert A.

Valley Stream—
Abbott, Lester E.

Vernon—
Roy, E. A.

Wampsville—
Pynn, J. Garland

Watertown—
Graham, Marshall

Watervliet—
Forgett, Ernest

Wellsville—
Palmer, M. A.

White Plains—
Edmondson, G. D.

Yonkers—
Husaczka, Stephen

NORTH CAROLINA

Charlotte—
Couch, W. O.

Fayetteville—
Owen, F. C.

Stoneville—
Moore, Gladstone

OHIO

Akron—
Johnson, Albert T.
Sherbondy, Fred G.

Alliance—
Shem, G. W.

Ashtabula—
Isaacson, T. E.

Barberton—
Hodge, J. C.
Shipman, Wm. H.

Batavia—
Didday, Herman C.

Beloit—
Stanley, LeRoy F.

Cambridge—
Bates, Earl

Canton—
Destefano, J. James
Keplinger, John C.
Miller, W. Lloyd
Vaughen, R. A.
Wilson, Ralph L.

Cincinnati—
Becker, Clarence W.
Beiersdorfer, Louis W.
Blakeney, Lewis B.
Breese, Clayton D.
Campbell, Howard
Casey, R. H.
Davis, Augustine
Dettwiler, Walter P.
De Vries, Henry K.
Diserens, Ralph
Edwards, Oliver H.
Harris, A. M.
King, Robert
Kraus, Rudolf
Maddox, W. A.
Mayberry, Walter R.
Naegele, W. R.
O'Hara, Eric
Osborne, M. J.
Paolino, A. G.
Paolino, Laurence
Paque, E. J.
Petry, Walter W.
Raftery, T. E.
Rosenblum, Abe
Schellhammer, Lawrence W.
Smith, F. H.
Smith H. Christa
Stoudt, John M.
Tangeman, Chas. L.
Thompson, James W.
Welling, Harry H.
Winkelman, Harvey E.

Cleveland—
Abbott, James A.
Adelson, J. S.
Ames, Ethan E.
Arnold, A. C.
Baker, W. B.
Benedict, Everett R.
Berkeley, L. R.
Boom, W. B.
Brown, Fred E.
Brown, Irving C.
Burr, Louis H.
Campbell, Lorn, Jr.
Carbis, J.
Champion, Pierre
Champion, Robert
Coakley, John A., Jr.
Cramer, W. T.
Crocker, Thomas D.
Czyzak, Stanley J.
Davis, Alton F.
Davis, Walter E.
Driscoll, J. M.
Dyar, Hugh H.
Firons, Ray
Eisenman, W. H.
Elder, Clayton Thomas
Fehr, R. B.
Ferguson, J. E.
Fosler, G. S.
Frantz, Lloyd
Fraser, P. V.
Freedman, C. F.
Gast, R. A.
Gibson, A. E.
Graham, John
Gray, H. W.
Heffner, Edwin J.
Henderson, Robert
Henn, Philip F.
Hexter, Irving B.
Hudson, D. D.
Hurley, Jack
Jacob, F. B.
Jennings, Jack
Jennings, Wm. L.
Jerabek, T. E.
Kerr, Edward J.
Ketchum, M. S., Jr.
Kimball, Don
Kincaid, John H.
Kinkead, Robt. E.
Kneen, H. F.
Knowles, A. M.
Kriz, Robert J.
Lange, Edward C.
Lawless, Thos. J.
Lewis, James T., Jr.
Lincoln, J. F.
Lisy, John W.
Low, Albert S.
Maine, J. F.
Mathews, John M.
Maurath, George A.
Mayor, Wm.
McCloud, F. A.
McCreery, H. L.
McDonough, W. R.
McKeighan, J. Stuart
Meister, W. F.
Mello, Peter
Midnight, S. A.

Miller, Alexander
Nichols, C. R.
Noonan, Thos. J.
Nutt, Buell W.
Padovick, Matt
Paton, W. G.
Payne, Spencer
Pederson, E. B.
Peters, V.
Pfeil, A. Leslie
Plummer, Fred L.
Plummer, Richard H.
Pohorenec, John
Powers, Ed. C.
Richards, J. S.
Riffie, Jacob F.
Sangdahl, Geo. S.
Schafer, Charles N.
Scheffler, C. A.
Scott, Edward T.
Sessions, F. L.
Shadrake, Bolick J.
Shaner, E. L.
Simon, Edward F.
Skuhrovec, J.
Skutt, John
Smith, E. C.
Specht, Joseph
Spenzer, E. B.
Steinman, J. E.
Stewart, W. S.
Stringham, Keever L.
Thompson, George F.
Thornton, Kent C.
Thow, Frank
Thum, Ernest E.
Tinker, G. H.
Turk, Earl E.
Vickery, J. F.
Weidler, Ward
Weir, R. E.
Whitmer, Floyd C.
Wilch, Frank A.
Williams, E. B.
Willins, G. L.
Wolfenden, John T.
Wood, J. F.
Workman, J. C.

Cleveland Heights—
Austin, John B.
Collins, F. J.
Engle, A. D.
Gregory, E. T.
Kessler, F. W.
Lawrence, G. R.
Mettler, F. W.
Smith, E. W. P.
Williams, A. M.

Cleves—
Bergmann, J. R.
Biery, Albert A.

Columbiana—
Barrow, Clyde

Columbus—
Art, Vernon
Black, J. N.
Broughton, Thomas M.
Caldwell, Francis S.
Dahle, Frederick B.
Duff, Ralph
Emig, Emery
Filimon, Victor
Foust, J. A.
Gardner, James J.
Geisler, George L.
Gilbert, Yowland
Grant, C. O.
Hecox, Emory
Herren, George S.
Holderby, E. O.
Jones, A. B.
Mitchell, Anthony J.
Parker, J. H.
Purdy, John L.
Richter, J. Louis
Rickly, O. D.
Roberts, Clay W.
Scarbery, Jasper L.
Shackleton, F. W.
Simon, Richard F.
Sloane, P. C.
Stevens, F. E.
Stitt, J. R.
Tarrier, Fred A.
Thomas, Charles S.
Willis, George H.

Conneaut—
Bruce, George H.

Dayton—
Adams, Charles O.
Carl, James W.
Glenn, E. M.
Henry, Lester H.
Jones, Harold O.
Lewis, Alfred
Meacham, F. L.
Toulmin, H. A., Jr.
Weiler, H. G.
Wilhelm, Frank J.
Witte, Albert M.

East Cleveland—
Burnett, Robert R.
Dyer, Wm.
Madson, Arthur E.
Mallett, John E.
Mathers, Paul F.
Schreeck, Karl W.
Smith, Emmett A.
Tarbell, Raymond P.
Williams, Clarence E.
Wyss, J. Calvin

Elyria—
Beal, W.
Mello, Gustav
Zeilman, R. H.

Euclid—
Miller, Lee K.

Hamilton—
Bartels, Carl P.
Hoffman, Ray
Wulftange, F. H.

Hemlock—
Storts, P. O.

Ivorydale—
Lockeman, Geo. F.

Lakewood—
Boehmke, Arthur H.
Cella, P. J.
Fay, Z. John
MacPherson, Kenneth

Lebanon—
Weaber, J. A., Jr.

Lima—
Mebs, Erwin H.
Townsend, A. J.

Lorain—
Douglas, W. R.
Findlay, Samuel N.
Gillespie, A. J.
McCormick, V. B.
Wilson, P. W., Jr.
Ziegler, Lyle C.

Madeira—
Telford, George W.

Mansfield—
Frank, Martin M.
Mackey, Glenn C.
McDermid, J. A.

Marietta—
Lindimore, C. O.

Marion—
Rogers, H. L.
Wolfel, R. C.

Massillon—
Lichtenwalter, T. R.
Miller, H. L.
Osborne, William S.
Wallace, Robert M.

Middletown—
Hayas, Anson
Jones, E. L.
Kenney, L. T.
Ross, James K.

Montpelier—
Anspaugh, Eldon

Mt. Gilead—
MacMillin, Howard F.

Mt. Healthy—
Emmons, Robert H.

Mt. Vernon—
Taylor, Albert A.

North Bend—
Burdge, Oscar B.

North Canton—
Ballard, E. L.

North Olmstead—
Stewart, W. F.

Norwood—
England, William

Painesville—
Hilberg, Fred E.

Parma—
Cotman, Carl A.

Philo—
Steen, R. H.

Salem—
Gilson, Wm. E.

Shaker Heights—
Blum, Harold P.

Sharonville—
Kirby, R. L.

Shelby—
Lewis, Harry R.

South Akron—
Switzer, DeVere

South Euclid—
Landis, George G.
Taylor, Clarence M.

Steubenville—
Moxley, T. R.

Terrace Park—
Luck, W. V.

Toledo—
Curtiss, Joseph C.
Hansen, Hans
Homrighaus, A. H.
McIver, George W.
Sherer, C. H.

Trenton—
Miller, Harry C.

Troy—
Chaffee, W. J.
Hobart, W. H.

University Heights—
Dewald, W. A.
Markee, Arthur A.

Warren—
Axtell, Howell B.
Bragne, William W.
Byers, E. H.
Cooper, J. H.
Delvecchio, E. J.
Flask, Joseph
Gordon, John D.
Haas, Robt. E.
Heltzel, Carl
Heltzel, J. N.
Humphrey, Stanley M.
Hurcomb, F. A.
Hurd, James L.
Jones, James
Larsen, B. W.
Lasher, E. R.
Long, Theodore S.
MacKenzie, F. W.
Mallett, E. A.
Manternach, A. S.
Murray, J. J.
O'Connor, Michael
Pearson, Roy W.
Porter, F. N.
Ringer, Robert Lee, Jr.
Ringsmith, O. F.
Roberts, N. J.
Roth, Henry W.
Sinclair, Richard G.
Vick, A. C.
Wolbert, Charles

West Alexandria—
Swift. W. C.

West Middletown—
Taylor, A. E.

Wickliffe—
Heinle, W. C.
Smith, John
Wehr, Wm. G.

Worthington—
Egan, Howard P.

Youngstown—
Backus, Thomas, Jr.
De Nicholas, Joseph
Featheringham, T. J.
Haynes, John H.
Henricks, Harold H.
Hoelzel, E. C.
Hughes, Grover A.
Jenkins, Clyde
Jeswald, Joseph
Kolasky, J. C.
Lamond, Nestor T.
McCarthy, L. B.
Mentges, Frank
Nutter, Joseph T.
Pool, R. H.
Reinhardt, G. A.
Rigby, L.
Riggle, Charles R.
Smith, Alex
Smith, Harry H.
Sweesy, F. D.
Tross, Ernest
Watson, C. G.
Wills, C. A.
Winterkamp, Frederick

OKLAHOMA

Afton—
Phelps, W. D.

Amorita—
Ferril, Paul

Bartlesville—
Briggs, Clay
Kindsvater, E. F.
Price, Harold C.

Britton—
Husky, L. D.

Cushing—
Meyers, Phil.

Delaware—
Carbon, Earl

Harrah—
Hale, I. J.
McMillan, James D.
Minter, H. E.

Jet—
Jenkins, Osce O., Jr.

Miami—
Bitticks, Glenn L.

Muskogee—
McManus, S. M.

Norman—
Shultz, Bennie
Tiffin, Wm. T.

Oklahoma City—
Acker, L. E.
Banks, Kenneth B.
Barnett, Orville T.
Blake, J. W.
Brown, C. M.
Bullock, C. L.
Conley, G. D.
Cornwall, Francis
Day, Geo.
Drinkwater, Wade
Dunne, M. F.
Fisher, Russell
Goodnick, C. C.
Hampton, J. H.
Hart, Otto
Heidermon, J. E.
Klein, Wm.
Krisman, J. M.
Lashbrook, C. A.
Lippincott, L. E.
McCandlish, D. F.
Mideke, Joseph M.
Moosholder, Meredith G.
Newhall, M. H.
Niblack, J. D.
Nordahl, Noble
Noret, C. C.
Northcutt, B. D.
Olivo, W. L.
Phillips, R. L.
Porter, J. S.
Raymond, G.
Reynolds, Ralph C.
Rickel, F. J.
Roach, Justin
Robson, L. D.
Simms, Howard N.
Smith, Stanley
Sprehe, Francis
Stewart, L. L.
Stroup, W. M.
Underwood, B. O.
Willis, C. C.
Winneberger, J. H.
Winter, M. A.

Ponca City—
Anderson, Louis
Brewer, W. D.
Stewart, George
Woolman, E. G.

Sand Springs—
Clay, Phillip L.
Leeper, A. W.

Seminole—
Miller, C. P.

Shamrock—
Gilliam, G. L.

Stillwater—
Barajas, Paul M.
Dawson, James K.
Frost, Sargent E.
James, Louis
Miser, Vernon
Peck, W. H.
Rice, William Howard
Schultz, Herold
See, P. G.
Soderstrom, E. D.

Stroud—
Gassaway, Frank

Texhoma—
Meyers, Fred H.

Tulsa—
Adcock, A. A.
Barnes, I. H.
Bassett, H. H.
Butcher, R. L.
Coates, A. G.
Denison, F. E.
Despain, Harry F.
Ellis, J. J.
Grubbs, Vernon
Hammett, Fred W.
Harkins, Robert
Harp, L. M.
Hinkefent, Emil C.
Hodell, Louis R.
Hoffman, Forrest A.
Hoobler, P. R.
Hunt, S. D.
Laidley, C. D.
Leach, Donald A.
Looney, R. L.
Martin, Otto V.
McKeever, R. S.
McKelvey, J. F.
McKelvey, L. N.
McKinley, J. C.
McLaughlin, W. A.
McNamar, Chas. C.
McWhirt, T. B.
Moore, Ralph O.
Moorer, Wm. D.
Morgan, Gus
Neet, J. G.
Nelson, Charles S.
Norton, Walter
Parrish, Robert E.
Patterson, N. R.
Patterson, Parker W.
Phelps, C. E.
Price, H. Dennis
Rebman, Chas. G.
Reed, H. M.
Rogers, O. L.
Shinkle, J. B.
Smith, Charles
Spence, Stanley L.
Stewart, S. A.
Thames, R. M.
Thompson, F. J.
Walker, J. P.
Wells, Cecil G.
Wilder, Arthur L.
Wilson, Loy
Wilson, Ross

West Tulsa—
Grubbs, Charles F.
Wiley, George L.

OREGON

Albany—
Harber, G. P.

Bandon—
York, Ralph L.

Portland—
Kordic, J. F.
Place, Lee

PENNSYLVANIA

Allentown—
Baines, Harry C.
Grebe, Karl A.
Kenworthy, J. W.
McGonigle, J. L.

Alquippa—
Van Ormer, Lawson

Ambler—
Bartges, W. S.
Fairchild, G. Barnett

Ambridge—
Meyer, William
Reilly, L. D.

Ardmore—
Sivyer, William

Beaver Falls—
Newell, H. D.

Bellefonte—
Craig, J. B.

Bellevue—
Crouch, R. G.
Gibson, Glenn J.
Stulen, F. B.

Ben Avon—
Loos, C. E.

Berwick—
Beishline, Guy C.
Evans, M. S.
Steinmeyer, J. W.

Bethlehem—
Bowles, H. J.
Doan, Prof. Gilbert E.
Donaldson, Jos. T.
Gould, Leo J.
Hill, H. O.
Hilpert, M. G.
Jensen, Cyril D.
Johnston, Bruce G.
Jones, Jonathan
Lawson, H. W.
Lewis, Robert B.
Lewis, Wm. W.
Lorentz, R. E.
Schenck, Chas.
Willard, Raymond, Jr.
Wilson, Samuel A.

Blairsville—
Andre, Frank V.

Brackenridge—
Patterson, Robert, Jr.

Bridgeville—
Rinek, John O.

Bristol—
De Ganahl, Carl

Burnham—
Cole, Amos G.

Butler—
Helsel, W. G.

Canonsburg
Kerrigan, Joseph A.

Carbondale—
Schermerhorn, J. A.

Chambersburg—
Wagaman, C. B.

Chester—
Bowers, F. M.
Butler, Harry
Holzbaur, Arthur A.
Jackson, Thomas M.
Lloyd, S. Elvin
Sadler, Robert L.
Seltzer, Clarence E.
Shaver, P. E.
Wagner, H. Alberti

Coatesville—
Boyer, Thomas H.
Chapman, Everett
Edwards, John
Griffith, Walter M.
Theisinger, W. Gordon

Conshohocken—
Marland, Arthur

Coply
Fisher, Robt.

Coraopolis—
Schaupp, J. W.

Cornwell Heights—
Vansant, Elmer B.

Craley—
Reisinger, Paul R.

Dover—
Wilt, Luther A.

Dowington—
Johnson, Ashmore C.

Easton—
Smart, Walter C. H.

East Pittsburgh—
Jennings, Chas. H.
Reddie, W. W.

East Stroudsburg—
Reid, Chas. W.

Eddystone—
Veihl, E. E., Jr.

Elizabeth—
Kelly, William

Ellwood City—
Daley, P. L.

Emaus—
Miller, Robert O.

Erie—
Boyd, W. R.
Cochran, W. H.
Elwood, E. S., Jr.
Humble, C. E.

Fair Oaks—
Davis, Claude E., Jr.

Finleyville—
Callery, S. S.

Folcroft
Gaul, Rufus H.

Franklin—
Lytton, Charles W.

Fredonia—
Reinhart, H. D.

Glenside—
Gilbert, Carl R.

Greencastle—
Plum, Ira

Greensburg—
Booth, T. H.
Nevin, K. S.

Greenville—
McCright, P. H.
Nesbitt, W. S.
Smith, Fred M.
Tam, W.

Grove City—
Buchanan, Wirt F.

Gwynedd Valley—
Enck, E. G.

Harrisburg—
Goetze, Herman

Imperial—
McCuen, Harper

Jeannette—
Sturgeon, W. C.

Johnstown—
Baumer, Scott D.
Fritzsch, T. N.
Steward, Douglas P.

Lancaster—
Henry, Michael O.
Posey, W. W.
Strayer, R. K.

Lansdale—
Schaible, Frank W.

Lansdowne—
Mathews, S. B.

Lebanon—
Tapparo, John A.

Leetsdale—
Sohn, Jesse S.

Leola—
Graybill, J. S.

Linwood—
Davis, William J.

McKeesport—
Maxwell, K. A.

Malvern—
Nelson, T. H., Jr.

Manoa—
Hoskins, G. W.

Marcus Hook—
Bagsar, Dr. A. B.
Carney, J. E.
Jackson, Chas.
Pietsch, W. H.
Stanley, Richard W.
Tunis, Richard

Midland—
Eaton, Aaron L.

Milton—
Anchor, Charles J.

Monaca—
Feely, H. M., Jr.

Monessen—
Bleecker, W. H.
Godfrey, Vincent H.
Mechem, R. H.

Mt. Lebanon—
Wilder, Arthur B.

Narberth—
Thomas, R. David

Nazareth—
Edelman, Howard J.

Neville Island—
Kennedy, A. M.
Satterlee, W. F.

New Brighton—
Cline, Charles R.
Metzger, William F.

New Castle—
Wheeley, Wm. S.

New Kensington—
Hoglund, Gustav O.

Norristown—
Cullen, David
Davis, Chester E.

North Hills—
Gilbert, Jesse W.

Oakmont—
Cecil, Robt. E.

Oreland—
Judelsohn, Fred

Philadelphia—
Adams, C. A.
Allison, Joseph W.
Ames, M. S.
Barnes, Wm. W.
Bateman, Thos. H.
Beatty, R. C.
Borresen, William
Borst, G. E.
Borton, George W.
Brenner, Sebastian K. A.
Broadley, Charles V.
Brown, J. G.
Campbell, H. F.
Carson, W. F.
Cleaver, H. P.
Cleeland, Robert
Clements, R. B.
Clifford, E.
Cogan, L. J.
Colosi, Anthony
Daly, William
Dunn, Thomas J. J.
Dunne, John J.
Ewertz, Harold N.
Feniewicz, Roy
Fetcher, John R.
Fuller, L. J.
Gallagher, S. A.
Gibb, W. H.
Gibson, W. Herbert
Gilpin, W. S.
Goldsmith, Lester M.
Harris, James C., Jr.
Harris, Lee W.
Hastings, B. F.
Heitman, Chas. E.
Herbert, Frank R.
Hesadoerfer, Geo. M.
Hopkins, H. E.
Hosted, E. G.
Hubach, K. G.
Huber, George L.
Janzer, Jos. M.
Johnson, Geo. R.
Jones, Thomas A.
Kalix, Robert L.
Kline, Harry J.
Klinke, H. O.
Lauterbach, E. W.
Leiby, Geo. L.
MacGuffie, Charles I.
MacKinney, John J.
Maxwell, G. L.
McCracken, R. Scott, Jr.
McHose, K. W.
Meadowcroft, W. Joseph
Mikhalapov, George
Mitchell, J. A.
Moore, Thomas J.
Murrell, Eric H.
Oechale, S. John
Ostrom, Kurre W.
Persons, O. H.
Pew, Arthur E., Jr.
Rhine, George E.
Richards, Edwin J.
Rusk, Charles M.
Salisbury, H. R.
Samans, Walter
Schreiner, Norman George
Seybold, Eugene
Shryock, J. G.
Soden, W. E.
Stahl, H. Hugo
Thomas, R. D.
Timmis, Pierce
Trainer, S. W.
Uhl, Ray
Vogel, Carl S.
Wagner, William S.
Weinberg, Philip H.
Wilson, A. R.
Wilson, R. G.
Yoch, A. H.
Ziegler, A. A.

Phoenixville—
Freeman, H. B.
Lambert, J. R.
Smith, Arthur W. C.

Pittsburgh—
Allen, Leslie
Ames, Louis
Beeson, J. K.
Bernard, Frank M.
Bibber, Leon C.
Blankenbuehler, John
Bond, Frank A.
Bowden, Alfred D.
Deacon, A. J.
Detlor, Leonard T.
Edsall, Stanley D.
Egger, E. J. W.
Elliott, John E.
Ellis, A. R.
Faulk, F. R.
Forker, E. W.
Goodrich, Charles F.
Grabe, C. G.
Green, George J.
Haygood, Jas. W.
Heller, R. W.
Hester, H. N.
Jackson, J. O.
James, H. Rees
Kling, Fred E.
Knabe, Martin C.
Knable, G. Elkins
Larsen, G. S.
Lee, R. J.
Lincoln, R. B.
Lyons, W. E.
Male, Milton
Marble, A. E.
Marsh, H. G.
Mesta, L. W.
Minnotte, J. F.
Neeld, R. S.
Persons, W. R.
Petrie, Geo. W.
Priest, H. M.
Reubene, Samuel
Roberts, Charles
Robinoff, Boris
Rugg, Walter S.
Schane, Harry P.
Shipman, W. P.
Smith, J. F.
Sommerville, Richard V.
Stuebing, A. F.
Stuver, E. W.
Thompson, Harry T.
Timmons, J. J.
Von Thaden, Herbert
Waldie, Robert C.
Warfel, J. A.
Whitcomb, L. K., Jr.
White, H. E.
Williams, Marshall
Wills, John G., Jr.
Wolfe, Geo. F.

Pottstown—
Emery, John F., Jr.
Hoar, William S.
Shoemaker, Lewis F., Jr.

Reading—
Miller, Da Costa R.

Ridley Park—
Mochel, Norman L.

Royersford—
Walter, C. S., Jr.

St. Marys—
Abbott, Harold W.

Salunga—
Cooper, John

Scranton—
Warneka, Lloyd D.

Sewickley—
Dill, Frederick H.

Sharon—
Allen, Ben. L.
Miller, A. O.
Schotte, Harry B.

Sharron Hill—
Russell, Roy

Shiremanstown—
Keyser, Cares C.

Somerset—
Morrin, J. W.

Springfield—
Joyce, E.

State College—
Knepp, Peter W.

Steelton—
Reed, John C.

Swoyersville—
Sutter, John

Thomasville—
Meyer, Michael L.

Tremont—
Miller, Irvin

Tyrone—
Herlt, Paul A.

Upper Darby—
Matheny, James H.
Raymo, Arthur J.

Verona—
Hershey, Donald

Villanova—
Nelson, T. Holland

Warren—
Andersen, C. S.
Drum, Atwood E.
Johnson, Leon H.
Walker, Henry O.

Washington—
Greed, Wm. McK.

Waynesboro—
Baer, Chester
Benedict, D. N.
Kemper, Luther H.
King, Harry W.
Miller, J. Garfield
Workman, S. F.
Zinkand, William C.

West Chester—
Griffith, Howard A.

Wilkinsburg—
Minehart, Glenn K.

Wilmerding—
Leeper, James A.
McCune, J. C.

Wrightsville—
Haugh, Thomas M.

Yardley—
Johnston, Robt. S.

York—
Allen, Charles W.
Anderson, E. J.
Bingaman, Roy
Brady, Edward J.
Douglass, Earl E.
Ensminger, Frank P.
Fogle, Earl
Hall, Richard F.
Harris, John E.
Hartman, H. I.
Heilman, A. B.
Helm, David F.
Hennessey, John W.
Herrick, Clayton B.
Hollingshead, W. E.
Hopwood, N. P.
Kerchner, C. E. Lewis
King, Loyd Monroe
Kitchen, Norman A.
Lair, W. B.
Light, A. T.
Marini, Louis G., Jr.
McIntire, David P.
Miller, George J.
Motter, Harry W.
Moul, Raymond H.
Phillips, Donald
Rohrer, Paul V.
Rouscher, Elmer E.
Ruth, Mahlon C.
Smith, Richard D.
Strine, Howard L.
Thompson, Roland B.
Van Dreser, M. J.
Weber, Oscar E.
Yingling, James A.
Zimmerman, M. Donald

RHODE ISLAND

Central Falls—
Monsarrat, Nicholas

Providence—
Doucette, Herbert W.
Fowler, Lee M.
Gagnow, Joseph O.
Hahn, Fred C.
Hahn, Stephen J.
Moulder, A. W.
Powers, James C., Jr.
Smith, Leo W.
Toof, Herman L.

SOUTH CAROLINA

Charleston—
Stevens, Harry

Columbia—
Burriss, Luther J.

Lyman—
Taylor, G. R.

Summerville—
Elliott, Richard B.

SOUTH DAKOTA

Forestburg—
Schwemle, Carl

TENNESSEE

Chattanooga—
Alexander, James F.
Bayston, Richard B.
Chapman, Edward C.
Daniels, Robert M.
Doyal, John P.
Drinnon, O. C.
Franer, Frank X.
Freeman, P. J. L.
Gentry, W. W.
Glenn, Mark Twain
Grimshaw, G. E., Jr.
Holt, M. L.
Kimball, C. E.
Krause, Fred J.
Lane, Ralph S.
McConnell, Ray
McLain, A. R.
Morelock, J. E.
Moses, A. J.
Oehmig, Edward W.
Parry, S. R.
Raymo, C. T.
Richardson, A. C.
Roberts, W. M.
Smith, Hope
Sylar, Bert L.
Wall, Marion P.
Weigel, John H.
Welch, E. T.
White, Marvin E.
Williams, Thomas R.
Winthorpe, James

Copperhill—
Henegar, Herbert B.

Memphis—
Anderson, C. D.
Aschinger, R. R.
Atkins, C. N.
Bolin, Nick
Bowers, Milton
Bull, Charles M.
Campbell, L. W.
Craig, E. S.
Fox, E. R.
Gill, Ed.
Grissom, E. L.
Hays, D. S.
Hays, G. B.
Henson, Vergil A.
Heppel, Fred C.
Hodgson, Harry
Johnston, C.
Jolly, J. T.
Lee, Marvin L.
Pidgeon, Frank
Poundens, J. C.
Sherwood, L. K.
Taylor, W. L.
Vaughn, J. I.
Wood, J. N.

Nashville—
Henshaw, Richard A.

Raleigh—
Gifford, R. A.

TEXAS

Baytown—
Stewart, H. M.

Beaumont—
Hughen, J. M.
Pittman, E. W.

Borger—
Hile, Leo

Brownsville—
Haywood, James E.

Corpus Christi—
Hare, M. P.

Dallas—
Daniels, Ludlow
Donegan, James P.
Drury, B. B., Jr.
Murphey, H. Bruce
Ray, W. C.
Rosevear, H. M.
Van Kirk, R. W.
Van Wart, Walter B.
Welch, Loyd
Wharton, John
Wilson, John A.
Young, C. A.

Fort Worth—
Akey, R. P.
Tomlinson, Thos. R.
Whaley, Wm.

Galveston—
Kane, J. J.

Houston—
Abbe, T. W.
Addis, Wm. H.
Alhart, C. G.
Allen, Ben H.
Ashby, I. F.
Beeley, Raymond
Bender, R. C.
Boyer, F. E.
Brown, R. S.
Bruckmiller, Edwin H.
Brugge, B. J.
Brunt, Roy
Burke, W. P.
Burns, T. V.
Carver, John A.
Chambers, R. A.
Cook, Marvin
Courtright, L. H.
Cryer, Chas. W.
Culbertson, J. L.
Dillman, E. E.
Donaldson, J. R.
Dye, Gil V.
Estes, L. L.
Glover, W. B.
Goolsbee, C. C.
Graham, Arch
Greer, W. H.
Hall, R. E.
Hamilton, J. E., Jr.
Hebert, Roy F.
Herzstein, Albert
Horrigan, J. R.
Irvin, J. L.
Jacks, C. G.
Jackson, Edward C.
Jalowy, H. C.
Jones, Ed. C.
Kegg, Thomas J.
Kinser, Roy
Klauberg, W. E.
Largent, Jack
Leahy, Wm. C.
Lincoln, P. M.
Liner, W. R.
Luger, Karl E.
McCarthy, H. P.
McCormick, K. A.
McGlasson, S. G.
McLaren, L. G.
Merritt, R. A.
Meyers, G. A.
Morris, James F.
Muckleroy, J. M.
Mulligan, C. A.
Netherwood, Joseph S.
Pflug, D. R.
Porter, C. E.
Pryor, J. F.
Ralston, R. W.
Reed, Malcolm V.
Roberts, E. T.
Ross, C. W.
Smith, B. K.
Steffen, G. C.
Stevens, Fred
Sugg, L. D.
Surguy, H. L.
Thompson, Perry
Weeks, M.
Welch, H. L.
White, J. R.
Wilkins, E. L.
Williams, Joe
Woods, G. W.
Wright, W. C.

Livingston—
McDonald, S. F.

Midland—
Osburn, E. R.

Mineral Wells—
Storrs, H. A.

Pasadena—
Westmoreland, W. L.

Port Arthur—
Farquhar, B. W.
Perkins, Howard

Ranger—
Elwood, Howard

San Antonio—
Wentworth, Philip

Texas City—
O'Quinn, John D.

Tyler—
Smith, W. C.

Vernon—
Reed, Clarence

Wichita Falls—
Wilson, John H.

UTAH

Salt Lake City—
Allen, Frank M.
Kluge, Leroy E.
Pingree, C. C.
Richeds, Fred
Smith, Belmont

VERMONT

Brattleboro—
Dunklee, Robert E.

Burlington—
Thomson, W. L.

Rutland—
Gilbert, John C.

Springfield—
Howard, W. F.

VIRGINIA

Alexandria—
Duty, James M.

Arlington—
Anderson, Robert L.
Brooker, Edgar
Falconer, Archer E.
Hiles, R. E.
Hodge, Frank N.
Huester, H. J.
McMillan, Robt. P.
Miller, B. M.
O'Halloran, Thos. S.
Roberts, R. B.
Smith, W. W.
Swope, Robert B.
Wagstaff, John P.

Falls Church—
Beck, Tage

Hopewell—
Morris, T. C.

Ivanhoe—
Niebanck, Richard J.

McCrady—
Ward, R. E.

Newport News—
Cunningham, R. H.
Jones, S. O.
Voldrich, C. B.

Norfolk—
Keatley, Gordon M.
Whitehurst, S. R.

Portsmouth—
Greene, P. C.
Walsh, H. I.

Richmond—
Miller, Crosby

Roanoke—
Meybin, Robt. J.
Winton, Robert
Wright, S. J.

WASHINGTON

Bremerton—
Davis, R. H.
Fladland, Henry G.

Coulee Dam—
Ford, Wm. F.
Weinman, Rudolph A.
Yetter, Gilbert L.

Morton—
Eresch, Joseph

Pullman—
Thornton, G. E.

Seattle—
Brett, Geo. S.
Burque, L. A.
Case, W. R.
Cheever, H. A.
Conrad, Frank A.
Drew, L. F.
Ellis, Richard
Flint, William M.
Flohr, E. Firmin
Frink, Francis G.
Hallum, J. D.
Haynes, Hanford
Hoffman, C. H.
Holt, Joseph
Luetke, J. A.
McBride, Geo. Morgan
McCormick, John B.
McKeel, G. M.
Meacham, William A.
Nelson, J. T.
Nickum, William C.
Russell, Ralph S.
Shafer, A. B.
Tadlock, J. H.
Talmadge, W. Heath
Tepley, George L.
Thomson, Alexander

Spokane—
Douglas, G. C.

Tacoma—
Morgan, G. H.

WEST VIRGINIA

Burlington—
Woodworth, Walter Z.

Charleston—
Horn, T. L.

Elkview—
Stover, C. R.

Fairmont—
Helmick, Robert J.

Morgantown—
Jones, L. E.

Point Pleasant—
Weissenburger, C. O.

Weirton—
Barrett, George N.
Botts, John

Wheeling—
Foss, F. F.
Lautner, W. F.
McFarland, James R.

Wolf Summit—
Shelbaer, W. N.

WISCONSIN

Beloit—
Allen, Franklin T.
Owens, J. W.

Cudahy—
Furguson, Clyde A.

De Pere—
Blanchard, B. E.

Green Bay—
Moody, Chester S.

Janesville—
Swart, George

Kenosha—
Zahn, Otto

Manitowoc—
West, Robert D.

Milwaukee—
Arbella, Edward
Archer, Fred C.
Bailie, E. Harold
Barkow, E. F.
Barrett, Richard C.
Baumler, Harold A.
Behling, Emil A.
Behling, Harold W.
Behmke, Peter S.
Boeck, R. E.
Boncher, Harry P.
Brandt, Elmer
Brekelbaum, Erwin C.
Brown, Arthur G.
Chyle, John J.
Cooper, Gerald A.
Crawford, Wm. E.
Eddingsaas, O. G.
Evert, Herbert W.
Frank, Louis C.
Freeburg, W. S.
Friese, A. H.
Garriott, F. E.
Grable, Godfrey B.
Grant, Leland E.
Gusho, Joseph
Hansen, K. L.
Hansen, Ralph E.
Haslinger, Wm.
Hawley, R. G.
Hettelsater, Henry C.
Hibbard, F. Gardiner
Hirner, F. J.
Holman, Arnold C.
Johnson, Gustave
Keating, Leonard M.
Keller, R. J.
Koch, Bernard A.
Koresh, George
Kutuchieff, Ivan
Larson, Louis J.
Loucks, F. B.
Malsh, Fred O.
McMillan, Ainslie
Meyer, G. F.
Miller, Vern V.
Millette, Millard M.
Nystrom, K. F.
Oesterlein, W.
Overton, Lynn S.
Owings, Steve
Pawlowski, John A.
Poehlman, Wm. J.
Pollei, Harold O.
Reichert, Harold W.
Riebeth, Theodore J.
Rosmann, C. F.
Rutishauser, M. H.
Schoenberg, Chas. W.
Sedam, Marvin
Shodron, John G.
Smith, S. H.
Steidl, Erwin A.
Sternke, Richard W.
Stoffregen, Karl
Voss, Otto C.
Walbridge, Robert P.
Walker, K.
Welch, Jerome B.
Wierschem, Henry
Young, Raymond A.
Young, Willard A.
Zentgraf, Lesley

Neenah—
Schiemann, E. H.

Oconto Falls—
Behling, Ellsworth

Pewaukee—
Meyer, Arnold

Racine—
Binder, Henry J.
Noe, William A.

Sheboygen—
Brehm, Elmer

South Bend—
Fleming, Herbert J.

South Milwaukee—
Rosendahl, R. C.

Wauwatosa—
Sarvis, A. L.
Volz, Fred O.
Walz, Gottlieb
Williams, Sylvester V.

West Allis—
Gorman, Wilbur R.
Marks, Raymond
Quartz, Herbert O.
Schoenbaum, Carl F.

WYOMING

Laramie—
Trueblood, Richard O.

Pinedale—
Toro, August

CANAL ZONE

O'Neill, J. J.
Wiggin, A.

TERRITORY OF HAWAII

Ewa—
Lothian, James
Wiley, Frank

Hilo—
Glover, Geo. M.
Lindgren, Henry

Honolulu—
Carpenter, B. P.
Carrier, L. E.
Colbert, James E.
Cunha, Frank
Elliott, William
Hansen, George
Jones, Edward J.
Lee, H. T.
Mardick, John K.
Mullin, Alfred N.
Orso, M. P.
Plaus, R. A.
Renton, Allan
Ross, G. M.
Ryan, C. M.
Ryan, Henry B.
Shilling, William
Slipper, Alan G.
Smith, Wm. E.
Wasson, John W.
York, Herman E.

Lihue—
Neely, Arthur C.

Lukefield—
Jensen, Sigurd B.

Maui—
Chalmer, Joseph
Johnson, John M.
Smith, E. Butler

Paauhau—
Chalmers, Robert F.

Papaikou—
Anderson, Alex. A. S. M.

Waiahia—
Jensen, Car IE. E.

Wailulo—
Oka, Horace Y.

Waipahu—
Pestana, Edward

PACIFIC ISLANDS

Dukes, Marcus R.

PHILLIPPINE ISLANDS

Bennett, F. C.
Donnelly, Wm. Wise
Dyke, Edw. H.

PUERTO RICO

De Blois, Weldin
Ferre, Herman

AUSTRALIA

Ahlston, A. C.
Beattie, William H.
Chapman, W. D.
Fellows, H. S.
Hume, K. B.
Leigh, W. K.
Pullen, K. G.
Rigby, Edw. J.

BELGIUM

Charlier, Pierre
La Grange, D.
Van Craen, Alphonse

CANADA

Aita—
MacPhee, G. Y.

Alberta—
Bradshaw, A.
Green, G. W.
Latta, A. L.

British Columbia—
Beckett, Geo. H.
Dion, Ralph J.

Manitoba—
Bogh, Waldemar
White, J. C.

Montreal—
Cape, Gordon
Chambers, Hugh
Dembitsky, Thomas
Kerry, Frank George
Layton, M. S.
Lyster, H. M.
Mercier, Joseph
Royer, Charles
Royer, Jacques
Rozon, W. A.
Townsend, W. M.
Viberg, E. R.
Wall, A. S.

Ontario—
Best, Frank A.
Boyes, Stanley
Cady, George F.
Cavin, Gustave
Forbes, Edward Gordon
Hall, A. J.
Hamilton, Chas. J.
Jacobs, David S.
McMurtry, L. C.
Smellie, J. T.
Spencer, Raymond A.
Theriault, M.
Wales, C. Clarke

Ottawa—
Johnson, A.

Quebec—
Blanchard, Jules R.
Boyd, David
Cassells, John
De Montigny, Lucien
Fisher, A.
Gour, Philip
Gratton, Yvon
Scott, Douglas
Seymour, A. W.
Stewart, J. R.
Williams, F. H.

Saskatchewan—
Morris, Ernest J.

Sorel—
Blais, Lucien
Cousineau, L. P.

Toronto—
Alison, R. G.
Cave, Joseph
Crase, George H.
Foster, George J.
Hamilton, Chester B.
Hirt, E. L.
Lloyd, D. S.
Macdonald, Robert H.
Porter, J. Gordon
Rumble, George
Scott, George E.
Sherry, Clarence
Walcott, William Daniel

Trail—
Palmer, H.

Vancouver—
Balzer, Otto
Davis, Maxwell R.
Ross, J. A.
Scott, G. F.

Winnepeg—
Meurer, P. G.
Mills, Gordon

CHINA

Burgess, Norman O.

DENMARK

Lowener, V.
Madsen, K. K.

ENGLAND

Addison, James
Bainbridge, C. G.
Boyd, G. Murray
Braithwaite, R. G.
Clark, Henry Wilmot
Ferguson, Samuel
Fergusson, Hugh B.
Gardner, E. P. S.
Gooderham, R. M.
Lloyd, J. F.
Mitchell, James
Moore, Samuel
Neumann, Dr. Ing. A. J.
Patterson, John H.
Plowright, R. J. Owen
Price, H. S.
Purslow, Herbert
Reeves, Dr. Lewis
Richardson, David
Roberts, John H.
Stephenson, A.
Thompson, J. H. N.
Tibbenham, Louis John
Tucker, Norman Albert

FRANCE

De Maille, Roland
Heudelot, C. J.
Kauffmann, Gene
Langquepin, Jacques E.
Louis, J.
Pinczon, J.
Sarazin, R.
Sciaky, David

GERMANY

Fleiss, H. H.
Graf, Otto
Groebler, Hans Ing.
Munz, Hermann J.
Sommer, Dr. Direktor Franz
Thum, Dr. August,

HOLLAND

Rosskopf, Thos.
Van Ouwerkerk, L.

INDIA

Corstorphine, John
Dubash, D. S.
Latiff, M. A.
Tweed, R.

IRELAND

Fay, Patrick

ITALY

Scolari, T.

JAPAN

Ebashi, T.
Ojima, J.
Rabbitt, James A.
Tsuruta, Akira
Ujiiye, T.

MEXICO

Easton, F. S.
Neumann, Othon G.
Rock, Esteban

NEW ZEALAND

Beadle, T. M.
Edmundson, C. D.

NORWAY

Andersen, Prof. Aksel
Borsum, Finn
Jacobsen, Odd
Jankov, Paul V.
Torbjornsen, E.

PALESTINE

Hearn, W. W.
Robinson, William J.

PERSIA

Hall, Thomas C.

POLAND

Bryla, Stefen

ROUMANIA

Miklósi, Cornelius Dr. Ing.

SOUTH AFRICA

Anderson, Albert C.
Brown, Maurice J. E.
Carnac-Rivett, A. J.
Dall, David MacDonald
Eaton, C. D.
Gemmell, Robert D.
Lorio, A. B.
Mason, Allan Shaw
Nel, Gert Jacobus
Poole, Laurence B.
Thompson, Marshall Hudson
Vuchnich, M. N.

SOUTH AMERICA

Curry, Robert A.
Garcya, P.
Gwynne, G. R.
Hlusicka, J.
Jewell, Alpha
Maroni, Angel A.
Sullins, C. V.

SWEDEN

Ellve, Eric
Forssell, Pontus

SWITZERLAND

Sonderegger, A.

WEST INDIES

Stibbs, Edward G.

SUSTAINING COMPANIES

Companies Supporting the Society Through Regular Advertising and Sustaining Members

Products and Activities as Furnished by Each Company

Air Reduction Sales Co., Lincoln Building, 60 East 42nd Street, New York, N. Y. Produces and supplies Airco high-purity oxygen, nitrogen, hydrogen, argon, neon, helium, krypton and xenon, atmospheric gases, acetylene, carbon dioxide in liquid and solid "DRY-ICE" form, carbide, Airco welding and cutting torches, pressure regulators and automatic welding and cutting machines, acetylene generators, welding and cutting supplies, gas welding rods, electrodes and Wilson Arc Welders. Maintains a nation-wide oxyacetylene welding and cutting and arc welding supply service distributed through 40 district sales offices, 585 distributing stations and 112 plants.

The American Brass Company, Waterbury, Conn. Manufactures Anaconda copper alloy welding rods for oxyacetylene and electric welding, including Tobin Bronze, manganese bronze, brazing metal, phosphor bronze, electrolytic copper, deoxidized copper, Super-Nickel and Everdur. Tobin Bronze (reg. U. S. Pat. Office) is a most satisfactory material for general oxyacetylene welding because of its uniformity, composition, free flowing qualities at 1625° F. and high tensile strength. The low welding temperatures usually obviate the necessity of special preheating and dismantling.

American Bridge Company, Pittsburgh, Pa. Fabricators and erectors of structural steelwork for every purpose, including bridges, buildings, barges, hulls, towers, substations, dams, turntables, viaduct and subway structures. Also manufacturers of eyebars and electric furnaces of the Heroult type. Plants at Ambridge, Pa., Gary, Ind., Trenton, N. J., Elmira, N. Y., Pittsburgh, Pa., Minneapolis, Minn., and Toledo, Ohio.

***American Bureau of Shipping, 24–26 Old Slip, New York, N. Y.** President, J. Lewis Luckenbach. Chief Surveyor, D. Arnott. A society for the survey and registry of shipping, whose committees of management comprise the representative shipowners, underwriters, shipbuilders and marine engineers of the United States. The Bureau exists solely for the benefit of these various shipping interests, all accrued funds being used for its maintenance and the improvement of the service. The Bureau has a large corps of surveyors stationed at the various ports of the world. Welding for ships comes under its supervision.

***American Gas and Electric Service Corporation, 30 Church Street, New York, N. Y.** Public utility servicing corporation for American Gas and Electric Company's wholly owned subsidiaries operating an interconnected system composed of Indiana & Michigan Electric Co., Indiana General Service Co., The Ohio Power Co., Wheeling Electric Co., Appalachian Electric Power Co., Kentucky and West Virginia Power Co., Inc., and Kingsport Utilities, Incorporated. The Scranton Electric Co. and Atlantic City Electric Co. operate separate systems. The Service Organization has pioneered in utility engineering, in high-tension transmission and large high-pressure steam plant practice.

American Institute of Steel Construction, 101 Park Avenue, New York, N. Y., is the membership organization of the fabricators and erectors of structural steel in the United States. The Institute is interested in the standardization of the product and its design, improvements in design and fabrication methods and the advancement and promotion of the market for steel structures.

The American Ship Building Company is engaged in the construction and repair of ships, engines and boilers. It is the oldest and largest organization of its kind on the Great Lakes. This company operates seven shipyards, located as follows: Lorain, Ohio, Cleveland, Ohio, Superior, Wis., South Chicago, Ill., Buffalo, N. Y. (two plants) and Toledo, Ohio. General Offices and Engineering Departments are located in Cleveland, Ohio.

American Steel & Wire Company, Cleveland, Chicago, New York and all principal cities. The electric and acetylene welding rods manufactured by this company are labeled "Premier." These rods include low carbon, high carbon, alloy steel and stainless steel. They also manufacture U. S. S. stainless and heat-resisting steels in the form of cold rolled strip steel, wire and wire products. A complete line of standard S. A. E. alloys is also available.

Anti-Borax Compound Co. Inc., Fort Wayne, Indiana. Manufacturers of the world-known "E-Z" Welding Compound for general forge use and "Crescent" Welding Compound, the best substitute for borax. Manufacturers of "Anti-Borax" Oxyacetylene Welding and Brazing Fluxes. "Anti-Borax" Fluxes include a flux for welding cast iron; a brazing flux for brass, bronze, malleable iron, etc.; a flux for bronze welding cast iron; two aluminum welding fluxes; a stainless Steel flux; a flux for Silver Soldering and a Tinning Compound. "Anti-Borax" Fluxes are guaranteed to give perfect satisfaction.

***The Atlantic Refining Company, Philadelphia, Pa.,** manufacturers and marketers of petroleum products. A fully integrated Company producing crude oil in the Mid-Continent field with refineries at Philadelphia and Port Arthur, Texas. Maintains research laboratories in conjunction with its refining and oil producing activities. Through subsidiaries operates some 2200 miles of crude and product pipe lines and a fleet of 25 ocean going tankers. Marketing territory—the Atlantic seaboard, from Massachusetts to Florida—also in foreign countries.

The Austin Company, Cleveland, Ohio. Engineers and builders for industrial plants of all kinds including manufacturing plants, warehouses, power plants, office buildings, research laboratories. The company maintains offices in twelve cities throughout the United States, as well as in Canada and England. Its structural steel fabricating plant at Cleveland pioneered in the development of welded structural steel and has furnished and erected upward of 25,000 tons of welded steel for industrial plant construction.

***The Babcock & Wilcox Company, 85 Liberty Street, New York, N. Y.,** are manufacturers of water tube steam boilers, both stationary and marine, pulverized-coal equipment, water-cooled furnaces, steam superheaters, economizers, air heaters, chain-grate stokers, refractories and process equipment. A subsidiary, the Babcock & Wilcox Tube Company, manufactures seamless tubes and pipes of steel and alloys. Works at Barberton, Ohio, Beaver Falls, Pa., and Augusta, Ga.

***Badenhausen Corporation, Cornwells Heights, Pa.,** a subsidiary of Riley Stoker Corporation, Worcester, Mass. Designers, Fabricators and Erectors of Heating & Power Boilers—Land and Marine, High and Low Pressure Steam Generating Units, Superheaters, Economizers, Waterwalls, Airheaters, Desuperheaters, Stacks, Tanks and Pressure Vessels of Riveted or Fusion Welded Construction.

***E. B. Badger & Sons Co., 75 Pitts St., Boston, Mass. Established 1841.** Licensing agents, designers, builders of Houdry Catalytic Cracking plants and Edeleanu units. Complete plants and equipment for distillation and treating of petroleum

---

* These companies also carry a Sustaining Member in the Society.

oils. Complete laboratory for evaluation of crudes and petroleum products. Complete chemical plants for producing alcohol, solvents, synthetic chemicals, coal tar products, cellulose products, paints, for the rubber, food product and soap industries and recovery of wastes. Corrugated expansion joints for steam, oil and process piping, including high temperatures and pressures.

Bastian-Blessing Company, The, General Offices 240–258 East Ontario St., Chicago, Ill. Pioneers in equipment for using and controlling high pressure gases. Manufacturers of the complete line of Rego Oxy-Acetylene Equipment including Welding and Cutting Torches, Pressure Reducing Regulators, "The Two-Stage Precision Regulator," Automatic Gas Economizers; High Pressure Charging- and Discharging-Manifolds, Fittings, Valves, Master Regulators, Relief Valves for Oxygen, Acetylene, Hydrogen, Carbonic and Liquefied Hydro-Carbon Gas Industries. The Rego line is sold and serviced by a chain of Distributors throughout the United States, Canada and abroad.

***The Bath Iron Works Corporation, Bath, Me.,** has built 79 ships since 1927 including first class yachts and trawlers for private owners as well as Coast Guard Cutters and destroyers for the United States Government. 2600 men are currently employed in the yard of which 300 are in the Welding Department. Over 70% of the riveting has been replaced by welding. The men must have experience in welding in confined positions and a variety of metals such as medium steel (black and galvanized), high tensile steel, stainless steel, monel metal and aluminum.

***Bell Telephone Laboratories, Inc., 463 West Street, New York, N. Y.** The laboratory research unit of the Bell Telephone System engages in basic laboratory investigations in the electrical arts of communication and in the sciences fundamental to those arts; develops and designs systems and apparatus for wire and radio communication, both telephone and telegraph, develops and investigates materials and establishes standards of quality.

***Bethlehem Steel Company, General Offices, Bethlehem, Pa.,** produces steel in all grades and analyses, and manufactures a wide range of commercial steel products such as: Alloy Steels; Bars and Special Sections; Boiler and Pressure Tubes; Steel Pipe and Casing; Bridges, Buildings and Steel Structures; Building Specialties; Steel Plate Work and Construction Castings; Corrosion-Resisting Steels; Flanged and Dished Products; Forgings; Pig Iron; Rails and Accessories; Semi-Finished Steel; Freight and Passenger Cars, Mine Cars; Steel Joists and Studs; Pipe; Plates; Sheets; Piling; Structural Shapes; Tin Plate; Tools and Tool Steels; Trackwork; Wheels, Axles; Wire Rope and Strand.

***The Big Three Welding Equipment Company** with offices and warehouses at Houston, Fort Worth, Corpus Christi and San Antonio, Texas, Tulsa, Oklahoma City, Oklahoma, are selling and servicing Lincoln Electric Company's welding machines, all types, such as engine driven, electric driven, belted units, steam turbine and Diesel driven welding machines, welding cables, helmets, face shields, lenses, holders and parts. Fleetweld electrodes, stainless steel, chrome, hardfacing and any other electrodes as manufactured by The Lincoln Electric Company at Cleveland, Ohio.

***Blaw-Knox Division, Pittsburgh, Pa.,** construct fusion welded equipment for high pressures and have all facilities to meet every requirement of the A. S. M. E. Code. They are experienced in the fabrication of the various special corrosion resisting alloys and their engineering department designs Process equipment to meet specific purchaser's requirements. In addition the diversified interests of their other divisions put them in an unusual position to serve the equipment user.

***The Buda Company, Harvey, Illinois, established in 1881.** Builds Gasoline, Natural Gas and Diesel Engines for Marine, Automotive and Industrial Service. Also manufacture Railway Equipment and Supplies including Motor Cars, Hand Cars and Push Cars, Frogs, Switches, Bumping Posts, Car Stops, Tie Spacers, Rail Benders, Track Drills, Tie Tampers. Also manufacture Jacks with capacities from $1^1/_2$ ton to 100 ton. Manufacture the Buda-Hubron Earth Drill and small Inboard Marine Engines and Diesel Nozzle Tester.

***Edward G. Budd Manufacturing Company, Philadelphia, Pa., and Detroit, Mich.** ALL STEEL automobile bodies and stampings. Die and Tool Work. Stainless steel railway cars, stainless steel marine and bridge structure, airplanes, tanks, automobile truck, bus and trailer bodies, fabricated by the SHOTWELD process. Equipment for the Rayon & Chemical Industries. TRADEMARK—SHOTWELD; ALL STEEL.

***Carnegie-Illinois Steel Corporation, Carnegie Building, Pittsburgh, Pa., and 208 South LaSalle Street, Chicago, Ill.,** a subsidiary of United States Steel Corporation. Combined in this one unit are the manufacturing facilities of the former Carnegie, Illinois and Lorain Steel Companies, and American Sheet and Tin Plate Company. Production covers a vast range of rolled, forged, cast and fabricated steel products—plates, shapes, bars, rails, sheets, tin plate, stainless and heat resisting steels, high-tensile steels, all types of alloy steels and special sections of every description.

***The Champion Rivet Company, of Cleveland, Ohio, Western Plant at East Chicago, Indiana,** is one of the pioneer companies in the metal joining industry. Over forty-five years ago the late Mr. David J. Champion founded the company and pioneered in the manufacture of steel rivets. During the past nine years they have been making a complete line of heavily coated welding electrodes known to the trade as Champion Red Devil, Blue Devil, Gray Devil shielded arc electrodes. They are represented in practically all the principal cities of the United States.

***Chicago Bridge & Iron Company, 37 West VanBuren Street, Chicago, Ill.** The Company employs welding in the fabrication of vessels conforming to Paragraph U-68 of the A. S. M. E. Code for Unfired Pressure Vessels at its Birmingham plant, and vessels fabricated to Paragraph U-69 of the Code at its Birmingham, Chicago and Greenville, Pennsylvania plants. Also in building pipe lines, barges, flat bottomed storage tanks, spheres, spheroids, smokestacks and elevated tanks.

The Clearing Machine Corporation, 6499 West 65th Street, Chicago, Illinois, is engaged in the manufacture of standard size and well built power presses for the sheet metal industries. Welded steel construction is employed in the manufacture of medium and large size mechanical and hydraulic presses ranging from 100-ton single action "R" Type presses to double and triple action presses of any desired tonnage. Advanced welding design, efficient welding technique and modern manufacturing facilities are among the outstanding features of CLEARING presses.

***Cleveland Electric Illuminating Company** serves Cleveland and 133 other communities in five counties in northeastern Ohio with electric light and power. Territory embraces 1700 square miles extending 100 miles along south shore of Lake Erie from Pennsylvania State Line westward to Avon. Population 1,300,000. Customers 331,000. Power is generated in three modern pulverized fuel steam generating plants, one at Ashtabula, another in Cleveland and a third at Avon. These plants have generating capacity of 527,500 kilowatts and are interconnected. Company also provides central steam heating service for the downtown area of Cleveland.

***Combustion Engineering Company, Inc., 200 Madison Avenue, New York,** manufacturers of all types of Water Tube and Fire Tube Boilers, Pulverized Fuel Burning Systems and Stokers; also Water-Cooled Furnaces, Superheaters, Economizers and Air Heaters; fabricators of Pressure Vessels, Tanks, etc., welded or riveted, in carbon, alloy or clad steel; also Platework, Domestic Range Boilers, Gas Water Heaters, and Cast Iron Soil Pipe and Fittings.

Commonwealth Edison Company, 72 West Adams Street, Chicago, Illinois. Public utility operating company engaged in the production, purchase, transmission, distribution and sale of electric energy for domestic, commercial, industrial, transportation and municipal purposes, in the City of Chicago. Also engages in the sale of electrical appliances to promote the development of its electricity supply business.

Compressed Industrial Gases, Inc. Executive Offices, 221 North LaSalle Street, Chicago, Illinois. Manufacturers of Oxygen, Acetylene, Hydrogen and Nitrogen Gases and distributors of Hollup Electrodes and Victor Welding and Cutting Apparatus. Twenty-six Branch Offices and plants are maintained throughout the United States. Wholly owned subsidiaries are Hollup Corporation, Chicago, Illinois, manufacturers of Welding Electrodes; Medicinal Oxygen Company, Philadelphia, Pennsylvania.

***Crane Co., 836 So. Michigan Ave., Chicago, Ill.** Crane Co. maintains fabricating shops in Chicago, Bridgeport, Birmingham, Los Angeles and other Branches in the larger cities throughout the country. In Chicago complete facilities are available for bending, lapping, welding and heat treating of piping in both carbon and alloy steels for use in steam power plant, oil refinery, oil and gas transmission, chemical and various other industries. Pressure vessels are also fabricated by welding under A. S. M. E. Code requirements.

***The Detroit Edison Company** does the entire commercial electric lighting and industrial power business in 40 cities, 87 incorporated villages and rural areas in 217 townships with a population

McK STAINLESS ELECTRODES Certified
THE McKAY COMPANY certifies that the electrodes in this package are part of a lot that have been tested within the recommended current and voltage ranges. The resultant Weld Deposit when uncontaminated by parent metal was found to analyze as shown:
Size 1/8 Type 18-8
ANALYSIS
Chromium 19.08
Nickel 10.21
Carbon .057
Columbium
Molybdenum
THE McKAY COMPANY · PITTSBURGH, PA.

Here's How McKay CERTIFIES the Resultant Weld

THIS CERTIFICATE on every WATERPROOF PACKAGE of McKAY

McK STAINLESS ELECTRODES Certified

Our newest distributor, with branches in 10 cities, is Joseph T. Ryerson & Son, Inc.

Made by the makers of the "researched" line of quality electrodes, and the famous line of McKay Chains.

THE McKAY COMPANY • PITTSBURGH, PA.
Sales Offices: York, Pa.

conservatively estimated at 2,581,000. Territory served 7587 square miles. Operates 4 large steam power plants, 3 small steam plants and 7 small hydraulic plants with a total capacity of 1,020,675 kw. The Company serves 666,452 electric customers, 11,247 gas customers and 1732 steam heating customers.

*Dowingtown Iron Works, Downingtown, Pa., welded plate fabrication of every type for all purposes of ordinary carbon steel, chrome nickel, chrome alloys, nickel, nickel clad steel, stainless clad steels, Monel metal and Silicon bronze; to form: acid tanks, air pressure tanks, grease tanks, jacketed tanks, pressure tanks, tanks for storage of chemicals, buoys, pontoons, stills, calandrias, fermentation tanks, storage bins, etc.

The Eastman Kodak Company is the world's largest manufacturer of photographic materials and equipment, with three plants at Rochester, New York; plants at Kingsport, Tennessee, Peabody, Massachusetts, Chicago, and Toronto; five plants in Europe; a plant in Australia; and a world-wide distributing organization. The Kodak Research Laboratories' staff, numbering 500, engages in scientific work pointed toward the continuous advance of photography. Nonphotographic materials manufactured include acetate rayon, an acetate molding composition, cellulose-nitrate and cellulose-acetate solutions and synthetic organic chemicals.

Electro-Motive Corporation, La Grange, Ill. Manufactures oil electric locomotives up to 6000 hp. for high speed streamline trains, and 600 hp. and up for freight, branch line and switching service, all of welded construction. Road locomotives are entirely of welded construction made of various pressings and structural shapes and mostly of high tensile alloy steel.

The Federal Machine & Welder Company of Warren, Ohio, manufacturers of all types of resistance welders including butt and flash welders, spot and projection welders, seam welders, multiple spot of both the *ULTRA SPEED* and hydromatic type, portable gun welders and resistance heaters. Special Welders have been developed for many fields, including steel strip mills, aluminum and stainless steel aircraft welding, refrigerator, automobile and railroads. Standard welders are kept in stock.

Federal Shipbuilding and Dry Dock Company, General Office and works: Kearny, New Jersey. New York Office: 21 West Street. Builders of naval and merchant ships, barges and floating structures. Drydocking and vessel repair work. Heavy machine work and bent plate steel fabrication.

***The Fibre-Metal Products Co., Chester, Pa.** Manufacturers of Welding Accessories and Industrial Face Protection. Originators, inventors and manufacturers of a special and exclusive process for producing *ONE PIECE* Arc Welding Helmets and Hand Shields from vulcanized fibre. Also, a complete line of the same items in Fabricated styles. Chrome Leather Helmets, Sand Blast Helmets, Metallic and Carbon Arc Electrode Holders. A portable machine, known as the *WELD-PROBER*, for removing specimens from butt-welded joints for visual, physical and chemical analysis has recently been added to their line.

The Fidelity and Casualty Company of New York, 80 Maiden Lane, N. Y. This company, for more than sixty years, has provided insurance protection for power plants throughout the United States and Canada. Every boiler and machinery policy-holder receives an engineering service which provides for periodic inspections of the insured equipment. This alone is of inestimable value in that defects and improper conditions are discovered before causing possible loss of life and property. The engineers are equipped with the latest type of instruments for properly inspecting the insured equipment.

***Freeport Sulphur Co., 122 East 42nd St., New York, N. Y.** Mines and markets crude sulphur or brimstone. Product is better than 99½% pure and free from selenium, tellurium and arsenic. Mines located near deep water at Port Sulphur, La., and Freeport, Texas. Ample stocks assure prompt delivery. Company's subsidiary, Cuban-American Manganese Corp., produces manganese ores for open hearth and blast furnace use. Stocks at Baltimore, Md., and Mobile, Ala.

***Frick Company, Waynesboro, Pa.** This company builds ice-making and refrigerating machinery and air-conditioning equipment and uses electric and oxyacetylene welding in making various parts of their systems. Pressure vessels, such as ammonia, carbon dioxide, methyl chloride and Freon-12 receivers, oil separators, accumulators, condensers and brine coolers are all of welded construction; also continuous pipe coils, headers and bends are welded. They use welding for tanks, pans and for repairing castings and are interested in the art in all its phases.

General Electric Company, General Offices, Schenectady, N. Y. Manufactures a wide variety of electrical and arc-welding equipment including: Single-operator generators direct connected to electric motors, gas or diesel engines, or belt drive. Multiple-operator, constant potential generators, motor driven. Alternating-current welding transformers. Resistor arc welders. Semi-automatic and automatic arc welders, including complete machines for welding tanks, pipes and other products. Automatic and manual atomic-hydrogen arc welders for quality welds. Welding accessories, including cable and complete line of extruded and bare electrodes.

***Grinnell Company, Inc., 260 West Exchange St., Providence, R. I.** Executive Offices are in Providence, but Sales Offices and Branches are located in the principal cities of the United States and Canada. Maintains Prefabricating Plants in Atlanta, Ga., Cranston, R. I., and Warren, Ohio, where complete facilities are available for prefabrication of all classes and types of piping including Bending, Van Stoning, Welding and Heat Treating of carbon and alloy steels used in Power Plants, Process Plants and Oil Refineries.

***Handy & Harman, 82 Fulton Street, New York City.** Products include, Sterling Silver, 999 "Plus" Fine Silver Anodes, Grain Silver, Gold and Silver Wire, Karat Golds and Gold Solders, Gold Bars, Silver Bars, Platinum Metals, "Handy" Silver Solders, Sil-Fos and Easy-Flo low temperature brazing alloys, Powder Metal Products, Handy Flux. We also refine all kinds of manufacturers' scraps, sweeps and other waste containing precious metals, also old gold, silver and platinum.

***Klaus L. Hansen,** Consulting Electrical Engineer, specializing in development of electrical machinery and processes, inventor of the P & H Hansen arc welding generator manufactured by the Harnischfeger Corporation of Milwaukee, available in capacities from 100 to 600 amperes in the electric motor driven machines and 150 to 400 in the gas engine driven welders. General consultant on arc welding problems; technical expert in patent litigation.

Harnischfeger Corporation, 4400 W. National Avenue, Milwaukee, Wisconsin. Manufacturers of the P & H Hansen Arc Welder, famous for its single current control and patented Stabil-

Buy "Proven Fluxes" with Years of Guaranteed Satisfaction behind them

The Trade-Name is "ANTI-BORAX"

Ask For Them ***Unequaled for Quality***

A Flux for every metal: Cast Iron Welding Flux No. 1.; Brazing Flux No. 2; Braz-Cast Flux No. 4, for bronze-welding cast iron; "ABC" Aluminum Flux No. 8 for sheet Aluminum and all alloys of Aluminum; Stainless Steel Flux No. 9; Silver Solder Brazing Flux No. 10; "Anti-Borax" Tinning Compound No. 11.

Send for Free Samples

ANTI-BORAX COMPOUND COMPANY
Fort Wayne, Indiana

SAVE MONEY . . . BY WELDING

Jaw Plates, Gyratory and Roll Crushers, Shovel Teeth, Hammers, Tractor Tread Grousers
with

MANGANAL

Reg. U. S. Pat. Office. U. S. Patents 1,876,738—1,947,167—2,021,945

11 to 13½% Manganese Nickel Steel

WELDING ELECTRODES, WEDGE and APPLICATOR BARS, HOT ROLLED PLATES

STULZ-SICKLES CO. *Sole Producers* 134-142 Lafayette St., Newark, N. J.

Sold Thru Distributors Only

ized Arc. Available in capacities from 100 to 600 amperes in the electric motor driven machines and 150 to 400 in the gas engine driven welders. Welding positioners, all welder accessories and P & H Smootharc welding rod are also produced, making Harnischfeger one of the leaders in the Welder Industry. Offices and service facilities in all principal cities.

*Harris Calorific Company, 5501 Cass Avenue, N. W., Cleveland, Ohio. Pioneer manufacturers of Welding and Cutting Torches for utilizing Oxygen in combination with Acetylene, City Gas, Natural Gas, Propane and other Fuel Gases; famous "Multi-Stage" and other Regulators for controlling either high or low pressure gases, "Gasavers" and Automatic Acetylene Generators, Automatic Torches, Pipe-Scribers, Oxygen Manifolds and Scarfing Torches. Distributors in all principal cities.

*Hartford Steam Boiler Inspection and Insurance Company, Hartford, Connecticut. Insurers against loss from accidents to boilers, unfired pressure vessels and power apparatus. The Company endeavors to reduce the likelihood of accidents by means of periodic inspections of each insured object and it also makes shop inspections of pressure vessels of all kinds during construction. It checks designs of pressure vessels for insurance acceptability and to make certain that they comply with the several construction Codes and State Laws.

Haynes Stellite Company, Kokomo, Indiana, a Unit of Union Carbide and Carbon Corporation, manufactures wear-resistant and corrosion-resistant alloys, metal-cutting tools, special cast and rolled products, hard-facing materials for protecting metal-wearing surfaces. Brand names of products are Haynes Stellite hard-facing rod, Haynes Stellite "2400" and J-Metal metal-cutting tools, Hascrome hard-facing rod, Haystellite cast tungsten carbide products and Hastelloy nickel-base alloys for corrosion resistance. Haynes Stellite hard-facing rods and Hascrome hard-facing rods are available through all apparatus shipping points of The Linde Air Products Company.

The Heltzel Steel Form and Iron Co., Thomas Road, Warren, Ohio. Complete steel fabricating and engineering service. General steel plate fabricating for such industries as machinery, welding, rubber, steel mills, coal, including machine bases, stacks, breechings, pipe line, etc. Standard products include steel forms for roadway, curb and gutter, sidewalks, foundations, sewers and tunnels; steel bins, weighing and conveying equipment for concrete aggregate and bulk cement, both portable and stationary; concrete buckets, subgrade planers and testers, concrete finishing tools; a complete line of gasoline or electric powered concrete finishing and strikeoff machinery.

*Hercules Motors Corporation, General Offices, Canton, Ohio. Factories, Canton, Ohio, and Muskegon, Mich. Oil Field Sales and Service Branches, Houston and Kilgore, Texas. Manufacturers of a complete line of modern gasoline and Diesel Engines for Heavy Duty Service—2, 4 and 6 Cylinder models, ranging from 4 to 200 hp. World's largest manufacturer specializing in the design and production of Internal Combustion Engines, exclusively. Hercules engines, both gasoline and Diesel, are widely used in the Welding Industry.

The Hobart Brothers Co., Hobart Square, Troy, Ohio. Manufacturers of Hobart "Simplified" Arc Welders featuring Remote Control; Compact, End-Bearing Design; All Welded Steel Construction; Automatic Stabilization and End to Center Ventilation. Built for operation with electric or gasoline power or for connection to customer's own power unit. Portable and stationary models are offered to meet or exceed N. E. M. A. and U. S. NAVY specifications in 150, 200, 300, 400 and 600 ampere ratings.

Hollup Corporation, 3357 West 47th Place, Chicago, Illinois. Manufacturers of (1) the SUREWELD line of Protected Arc electrodes, (2) Wanamaker and Rex lines of Coated and Processed electrodes, (3) Gas welding wire and supplies. A welding rod for every job in electric and oxyacetylene welding. Engineering service to users of welding available from Chicago and other principal cities.

*Industrial Publishing Co., 812 Huron Road, Cleveland, Ohio, publishers of Industry and Welding, distributed to 27,000 executives, supervisors, welding engineers and shop men. Deals with the economic side and the technical side of welding—new developments, news of the industry and technical articles on the practical phases of all types of ferrous, non-ferrous, alloy welding and cutting.

*Inland Steel Company, Chicago, Illinois. Open Hearth Steel Manufacturers: Semi-Finished Products, Merchant Bars, Concrete Reinforcement Bars, Plates, Inland 4-Way Floor Plates, Shapes, Steel Sheet Piling, Rails and Track Accessories, Hot and Cold Rolled Sheets and Strip, Galvanized Sheets, Zinc-Alloy Sheets, Cold Reduced Tin Plate, Tin Mill Black Plate, Terne

National

Carbide

DEPENDABLE

EFFICIENT

ECONOMICAL

FOR WELDING and CUTTING

Use National Carbide in the Red Drum

Lincoln Building

NATIONAL CARBIDE CORPORATION

New York, N. Y.

Plate, Rail Steel Products, Fence Posts, Inland Copper-Alloy, Inland Hi-Steel. Inland Ledloy Bulletins gladly sent upon request. District Offices: Detroit, Milwaukee, Kansas City, St. Louis, St. Paul, Cincinnati.

*International Harvester Company, 180 North Michigan Avenue, Chicago, U. S. A. The International Harvester Company make farm machines, farm tractors, dairy equipment, oil engines, farm wagons, motor trucks, industrial tractors, crawler tractors, Diesel engines and power units.

*The International Nickel Co., Inc., 67 Wall Street, New York, N. Y. Manufacturers of Inco Nickel, Monel and Inconel welding wire for oxyacetylene welding; Inco Monel gas welding flux and Inconel gas welding flux for oxyacetylene welding of Monel and Inconel, respectively; flux-coated electrodes for metal arc electric welding of nickel, nickel-clad steel, Monel, Monel-clad steel, Inconel and Inconel-clad steel. Producers of nickel, Monel and Inconel bars, rods, sheets, strip, tubes, plate, pipe and forgings.

*Jackson Electrode Holder Co., 6553 Woodward Ave., Detroit, Michigan. Manufacturers of completely insulated electrode holders for use in electric arc welding. Insulation consists of heat-resisting shields, protecting all metal parts, eliminating flashes due to contacting holder with grounded metal. Features claimed are safety, avoidance of work spoilage, long life, coolness, lightness. Rod capacities vary from 1/16" to 3/8", currents, 200 to 500 amps. The line also includes five bare type models of corresponding capacity.

*Johnson Supply Co., 1414 Wazee St., Denver, Colo. We are the exclusive distributors of Lincoln Electric Company, welding equipment and electrodes in Colorado, part of Wyoming, western Nebraska, two Western terra counties of Kansas and as far south as Albuquerque, New Mexico. We employ a welding technician, and have four men calling on the trade constantly. We specialize entirely in Lincoln Electric products.

*The Kimball Safety Products Company, 7314 Wade Park Avenue, Cleveland, Ohio, manufactures Accident Prevention Equipment for the prevention of injuries to workmen in industry. All types of personal protection for the workmen such as goggles, helmets, fire and acid resisting clothing are manufactured.

J. B. Klein Iron & Foundry Company, Oklahoma City, Okla. More than 30 years ago, J. B. Klein Iron & Foundry Co. started with one small shop. Today it is a complete modern iron and steel fabricating plant, housed by more than ten structures covering two square blocks, furnishing and fabricating all types of steel for buildings, roads and bridges. Carries a complete stock of reinforcing steel materials, structural steel and steel building specialties. Operates an ornamental iron shop, gray iron and non-ferrous foundry.

*The Lincoln Electric Company, Cleveland, Ohio. Specializes in manufacture of "Shield Arc" dual continuous control welders, single-operator variable voltage machines in motor-driven, gas-engine and belt-driven models of 100 to 600 amperes, capacities N. E. M. A. rating; automatic welders utilizing "Electronic Tornado" process of carbon arc welding; electrodes for all applications of arc welding; accessories and supplies of all types for manual and automatic arc welding; also "Linc-Weld" motors for A.C. current; starters and controls.

*The Linde Air Products Company, 30 East 42nd Street, New York, N. Y., a Unit of Union Carbide and Carbon Corporation, produced the first commercial oxygen by the liquid air process in America and is the world's largest manufacturer of this product. Its 70 plants and 97 warehouse stocks serve every industrial community in the country. A complete engineering and practical service is maintained to assist customers in applying the oxyacetylene process effectively. Linde also markets Oxweld, Prest-O-Weld and Purox welding and cutting apparatus and supplies, Prest-O-Lite dissolved acetylene, Union Carbide, Carbid processed carbide, acetylene generators and flood lights.

Link-Belt Company, Chicago, Philadelphia, Indianapolis, San Francisco, Atlanta. Materials handling machinery; power transmitting equipment; dryers; sand washing and sizing machinery; coal tipples and washeries; stokers; foundry sand preparation and mold handling machinery; coal and ashes handling equipment and water screens for boiler plants; speed reducers; variable speed transmissions; chains; sprocket wheels; babbitted and anti-friction bearings; pulleys; commercial castings; elevators and conveyors; locomotive and crawler shovels-draglines-cranes; crushers; gates and special machinery for many purposes.

*Lukenweld, Inc., Division of Lukens Steel Company, Coatesville, Pa. Designers and fabricators of welded steel structures for use in various types of machinery and equipment. Lukenweld, Inc., has specialized in the design and construction of the difficult and unusual in welding, such as welded steel Diesel engine frames; underframes and other structural parts for high-speed trains; railway truck frames, bolsters and spring planks, continuous welded frames for presses; welded steel gears, and paper machine driers.

Machinery and Welder Corporation, offices and warehouses located at Chicago, Illinois, Moline, Illinois, Milwaukee, Wisconsin, St. Louis, Missouri, Kansas City, Missouri, and Wichita, Kansas. Distributors of electric and oxyacetylene welding equipment. A complete stock of both acetylene and electric welding supplies, including machines, accessories and welding wire, is carried at each warehouse.

Magnolia Airco Gas Products Co., P. O. Box 318, Houston, Texas. Produces and supplies Airco high-purity oxygen, nitrogen, hydrogen, argon, neon, helium, krypton and xenon, atmospheric gases, acetylene, carbon dioxide in liquid and solid ("DRY-ICE") form, carbide, Airco welding and cutting torches, pressure regulators and automatic welding and cutting machines, acetylene generators, welding and cutting supplies, gas welding rods, electrodes and Wilson Electric Arc Welders. Maintains a nation-wide oxyacetylene welding and cutting and electric arc welding supply service distributed through 6 district sales offices, 6 distributing stations and 5 plants.

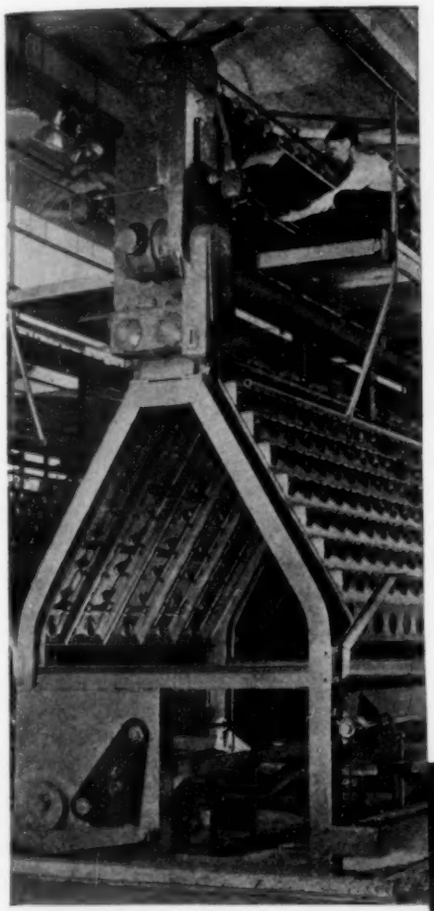
*P. R. Mallory & Co., Inc., Indianapolis, Indiana. Manufacturers of resistance welding electrodes for all kinds of similar and dissimilar metals. Elkonite, Elkaloy, Mallory 3, 22, 53, 73, 84, 100 and 333 metals are electrode materials and Soldaloy #8 is soldering iron tip material. All are alloys of extreme hardness and wear resistance with high electrical and thermal conductivity. Recent contribution to literature on welding is the book, "Engineering Data—Resistance Welding Theory and Practice."

*Maryland Casualty Company, Baltimore, Md. Providing insurance and inspection service to boiler and machinery clients, this Company has a force of 170 trained engineers throughout United States and Canada, in addition to an efficient Home Office engineering staff. Constant effort, since organization in 1898, has

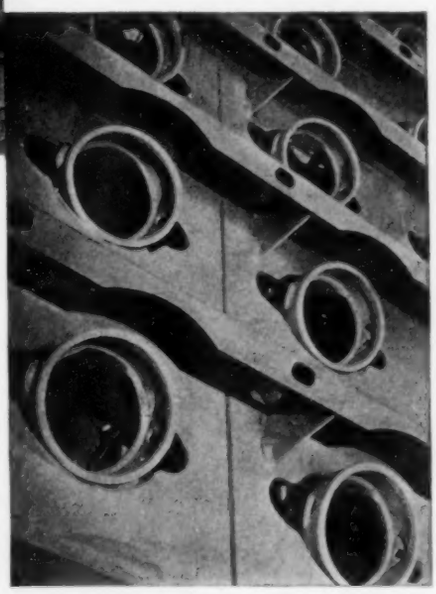
REGO
WELDING AND CUTTING EQUIPMENT
• Welding Torches
• Cutting Torches
• Economizers
• Regulators
• Outfits
FOR PLUS PROFIT SPECIFY REGO
Send for Catalog R-120 — with it you will have all the specifications you need for the buying of highest quality welding and cutting equipment.
The BASTIAN-BLESSING Company
282 E. ONTARIO CHICAGO

THOMSON
HEADQUARTERS FOR
Manual and Automatic SPOT, SEAM, BUTT, FLASH, PROJECTION, PORTABLE GUN, FABRIC and SPECIAL RESISTANCE WELDING PRODUCTION EQUIPMENT
Write for FLASHES, a monthly magazine of Resistance Welding News and information. Sent free on request by
THOMSON-GIBB ELECTRIC WELDING CO.
General Offices: 168 PLEASANT ST., LYNN, MASS.

All-Welded, Rolled Steel Construction Makes *Miracle Machine* Possible!



Set-up of the all-welded structural frames for the continuous spinning process machine units—operator's walkway at the process panel level not yet installed, but its supporting brackets are in place. Each unit has an individual motor drive and is composed of 5 intermediate A-frames, one end-drive A-frame with machinery box, 10 process panels, spin frames, spin transmission beams, machinery brackets and various other miscellaneous structural parts.



Here's the heart of the newest, most revolutionary production cycle the viscose rayon industry has seen in years. A close-up view of the stepped process panel showing pressed steel cups welded to the inclined skin plate. These cups, counterbored to a tolerance of .001 inch diametrically and held to a close vertical alignment and parallelism, act as retainers for flanged bearings supporting the process reels. Accuracy, rigidity and perfect alignment of the hundreds of moving parts on the panel were obtained through an unusual job of jig welding.

Sets new standards of precision . . . at approximately half the cost of previous methods of construction

THEY said it couldn't be done. But here it is! The world's first commercial installation for the continuous spinning, processing, drying and twisting of viscose rayon yarn in one machine. 96 of these machines were installed in the new Painesville, Ohio, Plant of the Industrial Rayon Corporation. They will eliminate handling and interruptions, and produce a higher quality, uniform yarn more economically than ever before.

And all-welded construction with U·S·S Rolled Steel made it economically possible! Rolled steel construction effected a large reduction in weight . . . and a clean-cut structure devoid of complicating details and multiplicity of small parts.

The structural steel frames for these machines were fabricated by American Bridge Company. Each of the 96 units is 10 ft. 1 in. wide, 45 ft. 1 in. long and about 19 ft. high. Not merely the quantity involved, but the extreme accuracy required to assure perfect alignment needed constituted a problem which was economically solved by a resourceful, efficient combination of welding and U·S·S rolled shapes.

This revolutionary use of rolled steel construction opens a new realm of possibility for the fabrication and design of industrial equipment. From the experience gained on this contract, it is believed that many types of machine frames and supports, hitherto thought impractical to fabricate by welding, may now be undertaken with confidence.

We offer special steels in widest range. And our experience in putting these steels to work is yours for the asking. Investigate the economies, the added life, the strength, and speed of fabrication that rolled steels can give to equipment you fabricate.

Here are a few of the variety of special steels with special properties to overcome destructive forces in your rolled steel construction:

U·S·S HIGH TENSILE STEELS
to carry high unit stresses and to reduce weight to a minimum at low cost.

U·S·S ABRASION RESISTING STEELS
to reduce wear and cut replacements.

U·S·S CARILLOY ALLOY STEELS
to carry tremendous bearing pressures.

U·S·S HEAT RESISTING STEELS
to endure high temperatures that spell disaster to other metals.

U·S·S STAINLESS STEELS
to resist corrosive environments, to assure long life. Special analyses can be welded with no loss in corrosion resistance.

CARNEGIE-ILLINOIS STEEL CORPORATION, *Pittsburgh and Chicago*
COLUMBIA STEEL COMPANY, *San Francisco*
TENNESSEE COAL, IRON & RAILROAD COMPANY, *Birmingham*
United States Steel Products Company, New York, *Export Distributors*

UNITED STATES STEEL

been to prevent accident or explosion by periodic, thorough inspections, supplemented by intensive safety education. On its staff are certified boiler inspectors, engineers, chemists, metallurgists and welding engineers, equipped with modern scientific instruments.

*Maurath, Inc., 7309 Union Ave., Cleveland, Ohio. George A. (Whitey) Maurath, President and General Manager. Manufacturers of stainless and heat-resisting electrodes, specializing and perfecting commercial production and use of electrodes. They have a complete plant controlling all the operations from the hot mill to the finished product. With their capacity, their own wire mill places them in a unique position for producing on short notice all the various high alloys. Their product is used world wide.

*The McKay Company, Pittsburgh, Pennsylvania. Plants at McKees Rocks (Pittsburgh), Pa., and York, Pa. Manufacturers of Shielded Arc Welding Rods, Automobile Tire Chains, Commercial and Industrial Chain of all descriptions.

*Metal & Thermit Corporation, 120 Broadway, New York, N. Y., manufacturers of materials and equipment for Thermit welding of heavy sections of various metals: railroad and street railway rails, housings, frames and beds of heavy machines, crankshafts, flywheels, steel mill rolls and pinions, etc. Manufacturers of Murex heavy coated electrodes for arc welding: mild steel, manganese, stainless, nickel, carbon molybdenum, chrome molybdenum steels, etc. Producers of M & T metals and alloys: ferro titanium, vanadium, pure chromium, etc.

Midwest Piping & Supply Co., Inc., St. Louis, Mo. Plants: St. Louis; Passaic, N. J.; Los Angeles, Cal. Fabricators and Erectors of complete piping systems—Power Plants, Oil Refineries, Gas Lines and Industrial Processes. Manufacturers of a complete line of Welding Fittings, consisting of 90° or 45° Ells, Tees, Crosses, Laterals, Concentric or Eccentric Reducers, VanStone Nipples, Shaped Nipples, Ellipsoidal Heads, Flanges, Reinforcing Saddles and Reinforcing Sleeves. Welding Fittings are available in all principal Cities through distributors' stocks.

National Carbide Corporation, Lincoln Building, opposite Grand Central, New York, N. Y. Manufactures and distributes National Carbide through approximately 250 warehouses located in all principal cities. National Carbide is packed in 100-lb. heavy steel drums, for use in generating acetylene, for oxy-acetylene processes and for house lighting, and also in 100-lb. and 25-lb. drums and 10-lb. and 2-lb. cans for use in miners' and other carbide lamps. Also distributors of railroad and contractors' carbide lights of all description.

*National Cylinder Gas Company, 205 West Wacker Drive, Chicago, Illinois, operates directly and through subsidiaries, thirty-two plants, and engages in the manufacture and sale of oxygen and acetylene, and sells gas and electric welding and cutting equipment and supplies, as well as hydrogen, nitrogen, propane and CO-2. This organization is particularly active in promoting the oxygen jet cutting process and the flame hardening of metals.

*National Tube Company, Frick Building, Pittsburgh, Pa. Seamless and Welded Steel Tubular Products; Plain Carbon and Alloy Steels for Pipe and Pressure Tubes. A wide range of diameters, wall thicknesses and lengths, for Water, Steam, Gas, Oil, Air, Sprinkler, Refrigeration, high temperature and high pressure service and other industrial uses. Suitable for bending, coiling, flanging and special fabrication. Ends prepared for welded, threaded or special types of connections. Made to Standard Specifications—A. S. T. M.—A. S. M. E.—A. P. I.—U. S. Government.

*New Orleans Public Service, Inc., 317 Baronne Street, Public Utility generating and distributing electrical energy; distributing gas and operating combined street car and bus system, all in the city of New Orleans.

*Page Steel and Wire Division of American Chain & Cable Co., Inc., General Offices, Monessen, Pennsylvania. Manufacturers of Page Armco and Page Steel Electrodes and Gas Welding Wire; Page Hi-Tensile Electrodes which produce welds conforming to A. W. S. specifications; Hi-Tensile Gas Wire; Page-Allegheny Stainless Steel Electrodes and Gas Wire; also Rods, High and Low Carbon Wires, Special Analysis Wires, Flat Wire, Special Shapes, Bond Wires, Strand, Stainless Steel Wire for mechanical purposes; Farm Fence, Industrial and Residential Chain Link Fence; Panel Partitions, Traffic Tape and Stainless Steel Tennis Nets.

Tips of MALLORY 22 METAL

Show up in --- the show down

Here's a factual record told in pictures that can't be ignored by anyone who wants reduced welding costs.

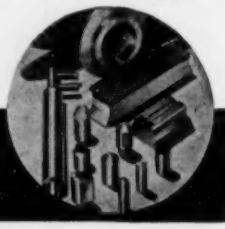
The test tips marked set No. 1 are both made of Mallory 22 Metal. After 5050 welds each, the slight point deformation shows them to be good for at least another 5,000 or 10,000 welds before redressing.

Test tips marked set No. 2. Specimen "A"—Mallory 22 Metal. Specimen "B" made of a competitive material. Redressing needed for Specimen "B" after only 1850 welds.

Test tips marked set No. 3. Specimen "C"—Mallory 22 Metal. Specimen "D" also of a competitive material. Redressing needed for Specimen "D" after only 1726 welds.

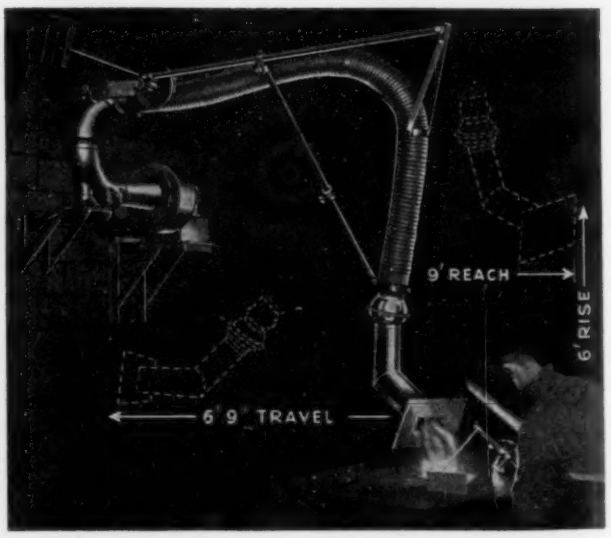
Those are the facts the picture tells. If you are not already using Mallory 22 Metal . . . write today for complete information.

P. R. MALLORY & CO., Inc.
INDIANAPOLIS INDIANA
Cable Address—Pelmallo



P. R. MALLORY & CO. Inc.
MALLORY

RUEMELIN
WELDING FUME COLLECTOR

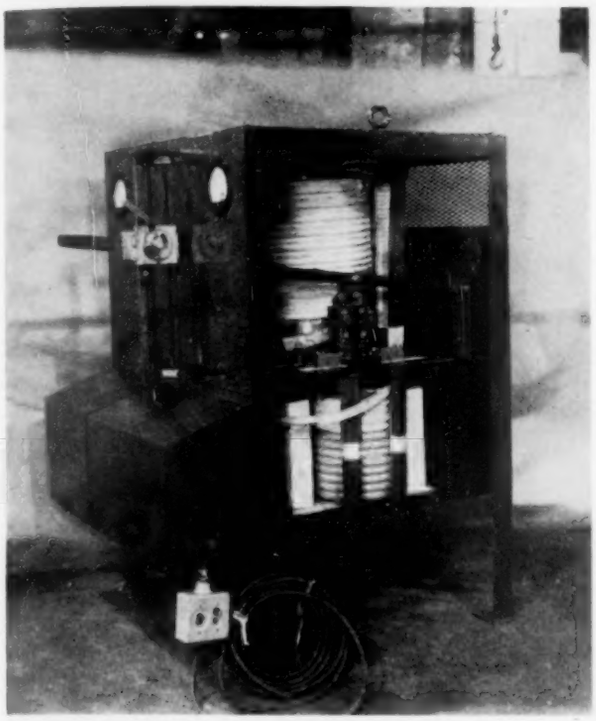


Effectively removes welding fumes at the source! Noxious smoke, gas and heat *can be* controlled with this proven equipment. . . . Leading manufacturers are sending us repeat orders, testifying to the practical results secured.

Write for Bulletin No. 37-C just released

RUEMELIN MFG. COMPANY
3881 North Palmer St. Milwaukee, Wisconsin

ALL ALONG
the ALTERNARC Line



Remote Control, coordinated control units can be used together for submerged arc or preheating—normalizing or separately as arc welders

SMOOTH UNINTERRUPTED REGULATION

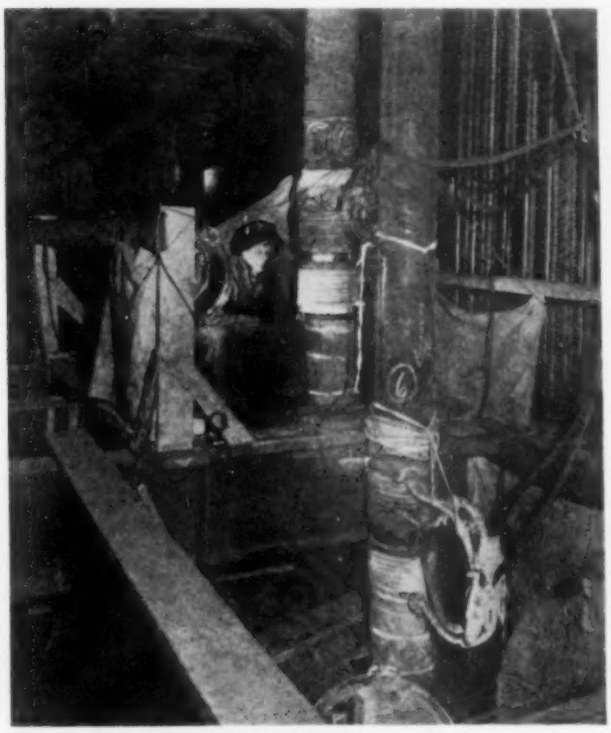
of current and voltage automatically supplying proper voltage by sliding a portion of the core of the transformer welder in or out. This betters power factor and lessons demand saving up to 50% but of primary importance is the constant heat accomplishment of this development which makes welding smooth and reliable meters which record the power demand and also the Nema rated capacity are provided on the machine. Only the higher grade insulation pure silica Mica and long fiber asbestos are used guaranteeing the machine to withstand heavy overloads.

ELECTRICAL PREHEATING ADVANCES WELDING ARC

This method of preheating allows higher speed, minimizes strains and stresses, improves the physical and metallurgical properties of the weld metal and metal adjacent to the joint. Postheating eliminates the strains and stresses.

In welding carbon molybdenum piping, commonly used for high pressure, high temperature steam lines, this preheating and postheating is absolutely essential. This system has revolutionized powerhouse pressure piping, marine piping, and process piping of all kinds. Automatic control profitable.

Let us have a chance at your problem. Equipments rented complete with option to purchase if desired.



System for Preheating and Stress-Relieving Pipe Joints

ELECTRIC ARC CUTTING & WELDING CO.

152-162 JELLIFF AVENUE NEWARK, NEW JERSEY

*Penton Publishing Company, Penton Building, Cleveland, Ohio. Publishes business papers serving metalworking industries, including Steel, a weekly magazine, dealing with production, processing, distribution and use of iron, steel and supplementary materials; Machine Design, monthly technical journal covering a design as it affects engineering production sales; The Foundry, monthly technical journal found wherever metals are cast; Daily Metal Trade, daily business paper of ferrous and nonferrous industries; and New Equipment Digest, a monthly publication devoted to new industrial equipment.

*The Petroleum Iron Works Company, Sharon, Pa. PIW fusion-welded pressure vessels of all kinds constructed of carbon or alloy steel for high pressure, high temperature and corrosion resistant service built in accordance with the A. S. M. E. and A. P. I.-A. S. M. E. Unfired Pressure Vessel Codes requiring X-raying of seams and stress relieving; fabricators and erectors, general steel plate work, welded or riveted; oil refinery equipment; chemical processing equipment. Also manufacturers of steel barrels and drums for liquids, semi-liquids and solids.

*Philadelphia Electric Company, 1000 Chestnut Street, Philadelphia, Pa. Supplies electric service in Philadelphia and adjacent territory embracing an area of about 1600 square miles with an estimated population of approximately 2,800,000. Gas service is supplied in the suburban area, and the Company supplies steam service in central Philadelphia and two suburban communities. The Company and subsidiaries have 1,176,125 kw. electric generating capacity, including 252,000 kw. installed in the Conowingo hydroelectric development, which is one of the largest in the United States.

*Pittsburgh Piping & Equipment Company, 10—43rd Street, Pittsburgh, Pa. Manufacturers of Fabricated Piping and Pipe Fittings; Fabricating and installing of complete piping systems for central stations and industrial plants, including welding of carbon and alloy steel piping for the highest pressures and temperature. Manufacturers of creased bends and superflexible corrugated pipe tangents for expansion bends. Manufacturers of all types of stainless steel process piping and stainless steel fittings.

*Pittsburgh Steel Company, Grant Building, Pittsburgh, Pa. Manufacturer of wire and wire products, and seamless tubing. Electric and gas welding wire a specialty. Special finishes on welding wire for coated electrodes produced according to customers' individual coating requirements. Specializing in plain carbon, mild alloy and Stainless Steel wire for all manufacturing purposes. Also manufacturer of reinforcing fabric, woven and welded wire fence and nails. Seamless tubing for oil country goods, boiler tubes and special fabrication of tubular parts in complete line of the desired steel specifications for the respective markets.

*The William B. Pollock Company, 101 Andrews Avenue, Youngstown, Ohio. Established in 1863: Engages in the Engineering, Fabricating and Erecting of Steel Plate Construction for Blast Furnaces, Steel Plants, Oil Refineries, Chemical Plants and Allied Industries. Also in the manufacture of Hot Metal Cars, Cinder Cars and Ladles. The Pollock Company pioneered the use of Welded Rolled Steel Construction and use it extensively in all their products as well as in the manufacture of large and small machinery parts for others.

*Pullman-Standard Car Manufacturing Company, Chicago, Ill., producer of railway passenger and freight cars, of conventional or light-weight design, fabricated by riveted or welded methods. Also producers of street cars and trolley busses. Makers of wheels, equalizers, and forged and machined parts for railway use. Has passenger car plants in Chicago, Ill., and Worcester, Mass.; freight car plants in Hammond, Ind., Michigan City, Ind., Butler, Pa., Bessemer, Ala., Baltimore, Maryland; and wheel foundries in Hammond, Ind., Michigan City, Ind., and New Orleans, La.

*The Pusey and Jones Corporation, Wilmington, Delaware, has been incorporated since 1848. The building of ships and paper making machinery are the leading classes of manufacture. All types of ships up to 400 feet in length have been built at the plant and large yachts are their specialty. In the paper machine division they have built the largest and fastest news print and kraft machines in the country.

Reed Roller Bit Company, P. O. Box 2119, Houston, Texas. Manuacturers of Rotary Drilling Tool, Valves, Engines and Pumping Units. Among these products are: DK Roller Bits for all formations, Rotary Reamers, The new Kor King Core Drill for both soft and hard formations, Tool Joints, Welded-in Blade drag bits with 2, 3 and 4 blades, both pin and box. The B-R Drilling and Coring bit with Wire Line Retractable Core Barrel. Replaceable Blade Bit, Drill Collars, Substitutes and Basket Bits.

*Republic Steel Corp., Cleveland, Ohio. Divisions include Berger Manufacturing Division, Union Drawn Steel Division, Steel and Tubes Division, and Niles Steel Products Division. Subsidiary: Truscon Steel Co. Third largest producer of iron and steel products. Specialties include Enduro Stainless Steel, Toncan Iron, Republic Alloy Steels, Republic Double Strength Steel, Silicon Steels, and Republic Electric Resistance Weld Pipe. Recent contribution to literature on welding is the free booklet, "The Welding of Enduro Stainless Steel."

The Resistance Welder Manufacturers Association with executive office at 505 Arch Street, Philadelphia, is an association embracing all principal manufacturers of resistance welding equipment in the U. S. A. The sole purpose of the Association is to standardize methods of resistance welding and the equipment required. It cooperates with engineering societies in the establishment of engineering standards, test methods and procedure and other engineering activities for the advancement of the art of resistance welding.

John A. Roebling's Sons Company, Trenton, N. J. Manufacturers of welding wire, high- or low-carbon or high manganese steel; reels for automatic or semi-automatic welding equipment; electric arc-welding machine cable; electrode holder cable. Manufacturers of wire rope, wire rope slings for heavy or light duty, bare or insulated copper wires and cables. Engineering data and catalogs sent upon application.

*The Benjamin F. Shaw Company, 503 East Second Street, Wilmington, Delaware, are Industrial and Power Plant Piping Fabricators. Cutting, threading, flanging, bending and welding are some of the operations performed in their fully equipped shops. Certified welders are employed in the welding shop. The various alloys in all forms are fabricated, as well as piping for all pressures and temperatures. A modern shop and an experienced personnel are your assurance of a satisfactory job.

*Shawinigan Products Corporation, New York City. Pure materials and expert craftsmanship developed during a period of over years assure the satisfactory performance of Shawinigan Carbide. This exact performance of the product has resulted in the growth of three huge plants and an efficient organization to deliver Shawinigan Carbide when, where and how you want it.

*Shell Oil Company, 100 Bush Street, San Francisco, Calif., producers, refiners and marketers of petroleum products. Distributing facilities in California, Oregon, Washington, Nevada, Arizona, Utah, Idaho, Montana, Wyoming, New Mexico, British Columbia and Territory of Hawaii. Manufacturers and marketers of Shell Brand of gasoline, distillates, kerosene, lubricating oils, greases and fuel oils. Refineries at Martinez, Coalinga, Dominguez and Wilmington, Calif. Marine loading terminals, San Francisco Bay, San Pedro Harbor, Portland and Seattle.

*A. O. Smith Corporation, Milwaukee, Wisconsin. Manufacturers of welded steel products. Pioneered electric welded high-pressure vessels for oil cracking and now the leading supplier of such vessels. Pioneered electric welded line pipe for gas, oil and gasoline. Manufacturers of high yield casing for oil wells. Leading producer of pressed steel automobile frames and glass-lined steel tanks for the brewing and food industries.

*S. Morgan Smith, York, Pa. Founded in 1877 by one of the pioneer builders of hydraulic turbines, S. Morgan Smith Company has grown to be one of the world's major producers of hydraulic turbines and related equipment. The company operates a modern manufacturing plant equipped with a wide variety of machine tools enabling it to produce large special machinery in addition to its regular products, and also a modern welding department especially equipped to handle large and complicated welding jobs.

Square D Company, Industrial Controller Division, Milwaukee, manufactures automatic control for all types of resistance welding machines. Some new developments are the Syncro-break contactors, which are adjustable to break the current at the minimum arcing point and thus reduce heating and prolong the life of the contacts; weld timers which are accurately adjustable in half cycle steps; sequence welder control which assures positive interlocking between air pressure and current flow.

*Standard Oil Company of California, 225 Bush St., San Francisco, Cal., is engaged in all phases of the oil industry, with distributing facilities throughout the Pacific West as well as in Alaska, Hawaii, utilizes welding to a high degree throughout the organization and has made a number of contributions to the development of welding equipment and technique; manufactures Calo Cutting Gas, a liquefied natural gas product widely used for metal cutting.

ALWAYS CONSIDER Federal RESISTANCE WELDING

Federal Flash Welder

1530 STEEL BARRELS IN ONE EIGHT HOUR DAY ON ONE MACHINE

That's the astonishing record of this Federal Flash Welder in constant use at a successful metal fabricating plant in Eastern Ohio during the past year. Compared with any previous methods, modern resistance welding has doubled the production in this plant by reducing man hours to 1.62 per hundred barrels, thereby increasing efficiency, lowering costs and at the same time making stronger and more uniform welds.

What Federal Resistance Welding Machines have done for this company is a production possibility that may be enjoyed by other manufacturers of steel drums, tanks and similar products. No tool placed in the hands of the metal fabrication industry has so increased the strength and beauty of metal products while speeding up production and lowering costs, as has resistance welding. Put this modern equipment to work for you. The consultant division of our engineering department offers its services in connection with your production problems.

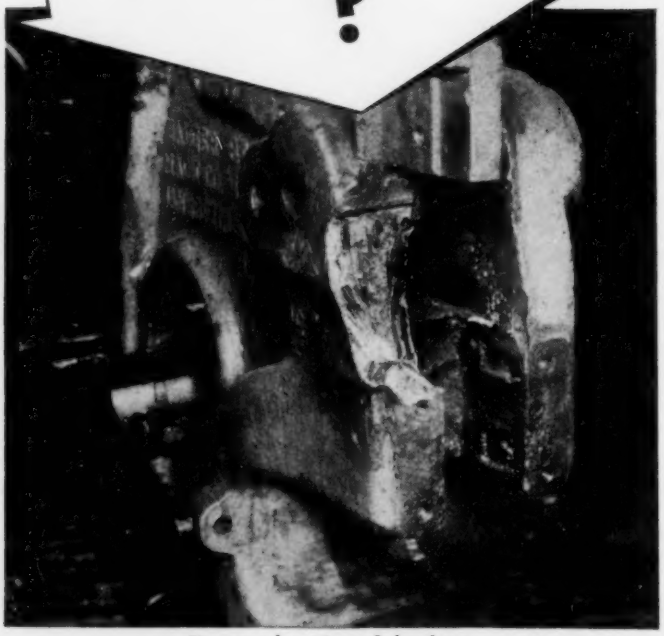
DESCRIPTIVE BULLETINS AND LITERATURE ARE AVAILABLE

The Federal MACHINE & WELDER CO.
Warren, Ohio

SALES OFFICES IN ALL PRINCIPAL CITIES

IN ENGLAND, MADE BY BRITISH FEDERAL WELDER & MACHINE CO. LTD., DUDLEY, ENGLAND

A *Tough* problem . . . can you solve it ?



Fractured casting of the shear

WHEN this 3″ thick shear casting fractured, repair welding with cast iron rods seemed to be the answer. Further consideration and inspection, however, revealed that if this method were used, it would be necessary to preheat the entire casting in a furnace to avoid uneven strains in the metal.

To do this was impractical for, after being heated, the casting would have thrown off so much heat that no one could have gone near it to do the work.

Why not figure how you would have done this job? Then check your answer with the solution on page 858.

29134

Check your answer with the solution on page 858

***Stoody Company, Whittier, California.** Manufacturers of hard-facing alloys, welding rods, grinders, electrode holders and sand blasting nozzles. Hard-facing materials include: Borium, Tube Borium, Cobalt Borium, Borod, Stoodite, Stoody Self-hardening, Sifram, Stoodex and the Numbered Stoodites: 45, 54 and 63. All Stoody hard-facing materials, with the exception of the Numbered Stoodites and Stoodex, which are for acetylene application only, are supplied either for electric or acetylene application.

Stulz-Sickles Company of Newark, N. J., pioneered the welding of 11 to 14% manganese steel with *MANGANAL* 11 to 13½% NICKEL MANGANESE STEEL WELDING ELECTRODES. *MANGANAL* is not only supplied in WELDING ELECTRODES, but APPLICATOR, WEDGE BARS and HOT ROLLED PLATE. Our products are sold exclusively through welding distributors and we are represented in practically all the principal cities of the United States.

Sun Oil Company, Philadelphia, Pa., established 1886. Producer, refiner and marketer of Blue Sunoco Motor Fuel, Sunoco Mercury-Made Motor Oil and Sunoco Greases. Sun also manufactures a full line of all types of petroleum products for industry, and maintains a staff of competent technical representatives available for consultation and technical advice regarding the application of petroleum to any mechanical operation or process. Sun operates Refineries at Marcus Hook, Pa., Toledo, Ohio, and Yale, Okla., maintaining 94 distributing plants in the Eastern United States, and markets its products in approximately 50 different countries.

***Sun Shipbuilding & Dry Dock Company, Chester, Pa.** Ships' hulls and equipment completely or partially welded. Welded steel products for oil refineries, chemical plants and kindred industries made to order for individual requirements. The facilities of this plant are unlimited for welded equipment as listed above and include automatic welding, X-ray and stress-relieving equipment for manufacturing largest units that can be shipped by rail or water. Extensive experience in manufacturing to the requirements of A. S. M. E. and A. P. I.-A. S. M. E. Codes.

***The Taylor-Winfield Corporation,** largest manufacturer of electric resistance welding machinery in the United States, has its main plant and home office in Warren, Ohio. Backed by forty years' experience in design and construction of all types of resistance welders, the company today offers a wide line of spot, projection, butt, flash, seam and special resistance welding and heating machines. A completely equipped fabricating division in Detroit, Michigan, produces the highest quality welded steel construction.

***Third Avenue Railway System, 130th Street and Third Avenue, New York, N. Y.** The Third Avenue Railway System carries 300 million passengers annually. It operates 250 miles of single track in New York City and municipalities north of it. It also operates 165 route miles of buses. Welding by electric arc, gas and thermit processes is extensively used in its shops and on its tracks. It regards the work of the AMERICAN WELDING SOCIETY of great value to the transit industry and, therefore, carries a sustaining membership in the Society.

Thomson-Gibb Electric Welding Co., Lynn, Massachusetts, manufactures a complete line of equipment based on the Thomson or Resistance Process of Electric Welding including standard spot, seam, flash, butt, projection, spotmatic and portable gun welders, equipment for Resistance or induction heating and fabric welders for producing road reinforcing mats and other steel fabrics from wires or rods in quantities as large as sixty tons per day. This company and the Mesta Machine Company are jointly responsible for the Thomson-Mesta Strip Steel Welder.

***Titan Metal Manufacturing Company.** Manufacturers of Bronze Welding Rods, Penn Bronze, Titan Bronze, Titan manganese Bronze and other special bronze rods for special conditions. Method of manufacture insures uniformity of all rods. The double deoxidizing process practically eliminates all fuming.

***Treadwell Construction Company, Midland, Pa.** Designers and Builders of heavy steel construction. Steel Plant Equipment including Ladles for Open-Hearth and Blast Furnaces, Welded Pipe, Processing Equipment, Homogeneous Bonded Lead Lined Vessels or Tanks, also Lead Covered Copper Coils. Boats, Barges, Caissons, Dredges and all types of Floating Equipment for inland waters. Plant located on Ohio River, making it possible to ship by water items of dimensions greater than railroad clearances will permit.

Una Welding, Inc., 1615 Collamer Avenue, Cleveland, Ohio. Manufacturers of a complete line of welding electrodes for code welding, general fabricating, hard-surfacing, stainless and other alloy steel welding; also of automatic and semi-automatic arc

ROEBLING

the custom-made wire for exacting welders

★ BARE WELDING ELECTRODES

★ COVERED WELDING ELECTRODES

★ GAS WELDING WIRE

ROEBLING WELDING CABLES:

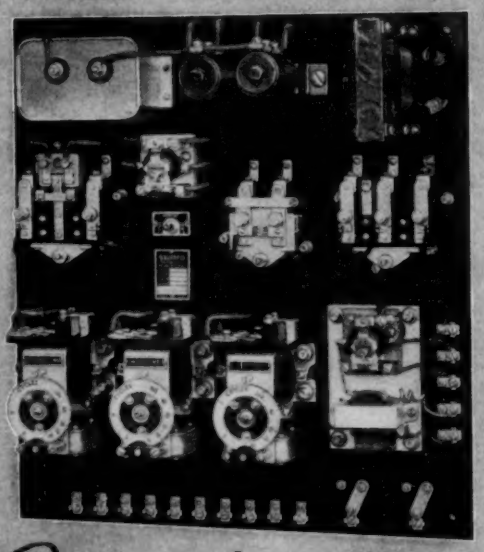
Made in a complete line of rubber and braided types for arc welding purposes.

JOHN A. ROEBLING'S SONS COMPANY, TRENTON, N.J.
Branches in Principal Cities

ONLY A FINE PRODUCT MAY BEAR THE NAME ROEBLING

New SQUARE D

SEQUENCE WELDER CONTROL



Positively INTERLOCKS

AIR PRESSURE with CURRENT FLOW!

● The Square D sequence panel is used with an air-operated welding machine to automatically perform the following duties:

1. Controls the solenoid air valve to automatically apply pressure to the work.
2. After a predetermined interval to allow the pressure to build up, it energizes the timer of the current control.
3. When the weld is completed, it determines the period during which the electrodes maintain pressure after the current is cut off.
4. It controls the period when the electrodes are open before the operating cycle is repeated.

For electronic equipment, a protective relay is necessary. This relay is energized from the load side of the power tubes and is specially designed to operate under a wide variation in voltage. This interlocking system prevents the electrodes from opening while any current passes through the electrodes. It also permits independent adjustment of the weld time without changing the other sequence intervals.

Squeeze time, hold time, and *off time,* are separately adjustable to suit the exact requirements of the welding operation. Faster production and better welds result.

The Square D sequence panel can be added to the control of any air-operated welding machine to give full automatic operation.

SQUARE D COMPANY

DETROIT - MILWAUKEE - LOS ANGELES

IN CANADA: SQUARE D COMPANY CANADA LIMITED, TORONTO, ONTARIO

welding equipment for a wide variety of products in the automotive, steel, railway and general metal industries. Attention is given to the development of the special wires, tapes and equipment that are needed to meet exacting requirements as to quality and speed in both light and heavy section welding.

*United States Rubber Company, 1790 Broadway, New York. Manufacturers of "U. S. Royal" Welding Cable with Tempered Rubber Sheath. The U. S. line includes Bare and Weatherproof Wire, Rubber Insulated Flexible Cords, Cord Sets, Soft Rubber Plugs, U. S. Royal Cords and Cables, U. S. Safecote Building Wire, Rubber Insulated Power Cable with braided, leaded or non-metallic sheath. Laytex Communication and Control Cables, Telephone Wire, and also many types of Rubber Insulated Wire and Cable for special applications.

*Union Carbide Company, 30 East 42nd Street, New York, N. Y., a Unit of Union Carbide and Carbon Corporation, pioneered in the commercial production of calcium carbide. Today, Union Carbide is the standard generator carbide for domestic and industrial uses. Warehouse stocks are maintained in 250 cities throughout this country. Union Carbide is sold through The Linde Air Products Company and The Oxweld Railroad Service Company, both Units of Union Carbide and Carbon Corporation.

*Universal-Cyclops Steel Corp., Bridgeville, Pa. & Titusville, Pa. Specializes in the manufacture of welding wire in all analyses of stainless and electric furnace steels. Complete facilities for melting, rolling and cold working. Also makes a full line of stainless, tool and other special carbon and alloy steels in the form of ingots, billets, plates, sheets, bars, wire and strip—both hot and cold finished. Manufacturers of special steels for all purposes.

Victor Equipment Company—Welding Equipment Division—844–50 Folsom Street, San Francisco, and 3821 Santa Fe Ave., Los Angeles, California, is engaged in the manufacture of the complete line of all types and sizes of welding and cutting torches, cylinder manifolds, acetylene gas generators, pressure reducing regulators for all cylinder gases, including regulators for laboratory purposes with delivery pressure ranges as high as 5000 lb. psi. VICTOR equipments have been manufactured for over a quarter of a century. Over 300 distributors from coast to coast.

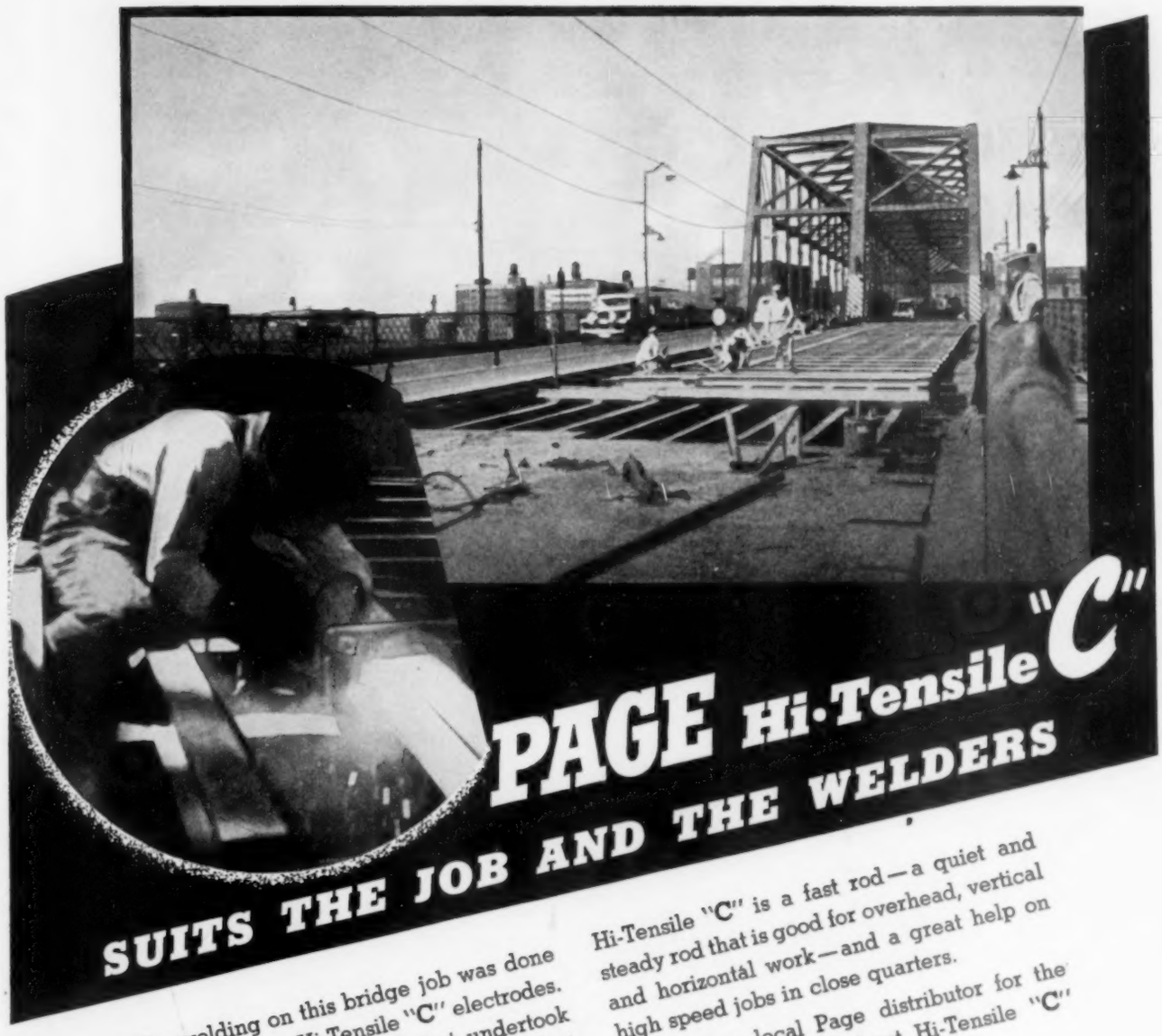
*Virginia Bridge Company, Roanoke, Va., plants at Roanoke, Va., Birmingham, Ala., and Memphis, Tenn. Designers, fabricators and erectors of steel structures of all kinds, including buildings, bridges, tanks, turn-tables, smoke-stacks, stand-pipes, penstocks, gas holders, airplane hangars, steel railway cars, car parts and underframes, cranes and derricks, steel stadiums.

*The Wellman Engineering Company, 7000 Central Avenue, Cleveland, O., manufacturers of coal and ore handling equipment, automatic gas producers, charging machines, coke pushers, floating cranes, material handling equipment, general steel mill machine, and the "Williams" line of clam shell and drag line buckets. Works at 7000 Central Avenue, Cleveland, O.

*Western Electric Company, Inc., 195 Broadway, New York, N. Y. Primarily manufacturers of telephone equipment, cable and wire; purchasers and distributors of supplies for the Bell System. Also manufacturers of certain by-products of the wire communications field including: Motion picture sound systems; public address and music reproducing systems; marine, police, aviation, and commercial broadcasting radio telephone equipment; vacuum tubes; audiphones, audiometers, electrical stethoscopes and train dispatching equipment. Principal factories: Hawthorne Works, Chicago, Ill.; Kearny Works, Kearny, N. J.; and Point Breeze Works, Baltimore, Md.

*Western Pipe & Steel Co., Los Angeles, Calif. Our two main plants, located at South San Francisco and Los Angeles, California, are well equipped for heavy plate, structural steel and light sheet fabrication. They are equipped with the latest X-ray equipment and stress-relieving furnaces for Class No. 1 welding. Principal products are heavy pressure vessels, electric welded pipe, welded and riveted tanks, bolted tanks, water well casing, structural steel and all types and descriptions of plate and sheet fabrication.

Westinghouse Electric & Manufacturing Company, East Pittsburgh, Pa., manufacturers of complete A.C. and D.C. arc welding machines, electrodes and accessories. Single operator D.C. welders, sizes from 150 to 600 amperes, multiple operator type from 500 to 2000 amperes. The A.C. transformer arc welders, sizes from 100 to 2000 amperes. All types of mild steel, stainless steel, hard surfacing and specialty electrodes. Automatic welding heads and control including complete equipment for the Linde Unionmelt Process. The Westinghouse Company's wide welding knowledge is available as a consulting welding service.



PAGE Hi-Tensile "C"

SUITS THE JOB AND THE WELDERS

● The welding on this bridge job was done entirely with Page Hi-Tensile "C" electrodes. That was because the firm that undertook the work knew from long experience that Hi-Tensile "C" suits the job and suits the welders.

This electrode easily takes 65,000 to 75,000-pound tensile strength tests. It shows 20 to 30 per cent elongation, 30 to 70 foot pounds impact resistance—and proves ready at any time for a fatigue resistance of 28,000 to 32,000 pounds per unit. It produces a job that will stand most rigid inspection.

Hi-Tensile "C" is a fast rod—a quiet and steady rod that is good for overhead, vertical and horizontal work—and a great help on high speed jobs in close quarters.

Ask your local Page distributor for the profit-building facts about Hi-Tensile "C" and other Page electrodes.

BUY ACCO QUALITY *in Page Welding Electrodes, Page Wire Fence, Lay-Set Preformed Wire Rope, Reading-Pratt & Cady Valves, Campbell Abrasive Cutting Machines, American Chains (Welded and Weldless) and Ford Hoists.*

PAGE STEEL AND WIRE DIVISION
MONESSEN, PENNSYLVANIA

AMERICAN CHAIN & CABLE COMPANY, Inc.

AMERICAN CHAIN DIVISION • AMERICAN CABLE DIVISION • ANDREW C. CAMPBELL DIVISION • FORD CHAIN BLOCK DIVISION • HAZARD WIRE ROPE DIVISION • HIGHLAND IRON AND STEEL DIVISION • MANLEY MANUFACTURING DIVISION • OWEN SILENT SPRING COMPANY, INC. • PAGE STEEL AND WIRE DIVISION • READING-PRATT & CADY DIVISION • READING STEEL CASTING DIVISION • WRIGHT MANUFACTURING DIVISION • IN CANADA: DOMINION CHAIN COMPANY, LTD. • IN ENGLAND: BRITISH WIRE PRODUCTS, LTD. • THE PARSONS CHAIN COMPANY, LTD. • *In Business for Your Safety*

Tobin Bronze is the answer to the problem on page 854

The shear casting after the repair weld had been made with Tobin Bronze.

THE repair weld on this shear casting was made in 3½ man hours including an hour's time required by one man to clean up the weld area with a chisel. The weld area was preheated with one torch while the welder deposited Tobin Bronze with another. Two men completed the welding in 2½ hours.

Tobin Bronze was the answer because of its low melting point (1625°F) which eliminates extensive preheating and minimizes danger of cracking and warping; because Tobin Bronze welds are strong, ductile, uniform.

All Tobin Bronze Welding Rods bear the trade-mark "Tobin Bronze Reg. U. S. Pat. Off." Look for this mark and be sure you are getting the genuine. 39184

ANACONDA

Anaconda Welding Rods

THE AMERICAN BRASS COMPANY, General Offices: Waterbury, Conn.

In Canada: Anaconda American Brass Ltd., New Toronto, Ontario
Subsidiary of Anaconda Copper Mining Company

***Wheeling Steel Corporation, General Offices, Wheeling, W. Va.** Manufacturers of Hot and Cold Rolled Sheets, Galvanized Sheets, Black and Galvanized Merchant Pipe, Line Pipe, Casing and Tubing; pipe for special uses including Wheeling COP-R-LOY pipe and Wheeling Open-Hearth Ammonia Pipe; Tin Plate, Terne Plate and Black Plate, including DUCTILLITE; Rods and Wire, including WELDING RODS, Wire and Wire Products, Wire Nails, Cut Nails; galvanized Range Boilers, Heater Tanks and Steel Barrels.

Whiting Corporation, Harvey, Illinois. Manufactures industrial products including electric traveling cranes; foundry equipment; pulverizing machinery; railroad shop equipment; domestic and commercial stokers; evaporators, crystallizers and filters for the chemical industries. Welding is used extensively, especially in evaporator and filtration equipment. The company has also built a great variety of special heavy machinery on a contract basis. Its welding facilities are modern and it has had wide experience in welding various kinds of alloy steels, nickel and nickel-clad plate; also copper and bronze.

***The Whitlock Coil Pipe Company, Hartford, Conn.,** designs and manufactures storage heaters, instantaneous heaters, closed feed water heaters, pressure vessels, coils, bends and heat exchangers both for high and low operating pressures and for high and low temperature service. X-ray equipment has recently been added and the Company is equipped to manufacture pressure vessels to meet all requirements of the A. S. M. E. Code. Copper, Everdur, steel and all workable alloys are fabricated, using qualified electric and oxyacetylene welders.

***Williams and Company, Inc., Pittsburgh, Cleveland, Cincinnati.** Warehouse Distributors who know the welding business and whose welding salesmen are experienced-trained welders. They offer for immediate delivery from stocks the correct materials for any welding job. Stocks embrace Electrodes of the shielded arc, dust or dipped coated types for Steel, Cast Iron, Manganese Steel, Monel, Nickel, Inconel, Nickel-Clad Steel, Stainless Steels Aluminum, Copper Alloys. Also Brazing Alloys, Fluxes, Carbon Plates, Welding Accessories, and Safety Equipment. Warehouse distributors of Westinghouse Flex Arc Welders.

Wilson Welder & Metals Co., Inc., 60 East 42nd Street, New York, N. Y. Manufacturers of a complete line of variable voltage welding machines, 75 to 800 Amperes, and constant potential multiple operator welding units, 500 to 3000 Amperes capacity and 150 to 1000 ampere A.C. transformer welders (conforming to N. E. M. A. Standards). Also a complete line of Wilson Color-tipt bare, coated and shielded arc electrodes and welding supplies. Machines distributed in all principal cities by the Air Reduction Sales Company.

***The John Wood Manufacturing Company, Inc., Conshohocken, Penna.** This company manufactures quality welded steel range boilers and tanks of every description. Back of that elusive word "quality" stands more than merely the desire to produce a good product. It involves skill, training, facilities, organization and a tradition of excellence in manufacture that cannot be acquired over night. The seventy-year old institution back of *Electric Weld Products* guarantees their quality.

Wyatt Metal & Boiler Works maintains plants at both Houston and Dallas, Texas for the manufacturing and fabricating of steel plate products of welded construction. Plants are equipped to handle heavy plate work completely shop fabricated or field erected. Stress relieving and Radiographing facilities for manufacturing vessels in accordance with A. S. M. E. and A. P. I.-A. S. M. E. Codes. Engineers thoroughly conversant with all phases of design are at the disposal of any one needing this service.

***York Ice Machinery Corporation, York, Pa.** This company builds a complete line of industrial and commercial refrigerating equipment; year 'round air conditioning systems for commercial, industrial and domestic use; dairy and ice cream machinery; ammonia and Freon valves, fittings and accessories. These products include a great variety of pressure vessels and piping in the fabrication of which welding is employed extensively. Branch sales offices or distributors in all principal cities.

***Youngstown Sheet & Tube Company, Youngstown, Ohio.** Manufacturers of carbon and alloy steel: blooms, billets, slabs, sheet bars, skelp, lap-welded, butt-welded, electric-welded and seamless pipe, square and rectangular tubing, mechanical tubing, hot and cold-rolled strip, sheets, auto body sheets, galvannealed sheets, roofing, tin plate, tin mill black plate, plates, railroad tie plates and track spikes, merchant bars and structurals, wire rods, wire, wire products, wire nails, rigid steel electrical conduit, electrical metallic tubing.

PROJECTION WELDING

Projection welding differs from spot operations insofar as a multiple of welds in predetermined locations are made at each operating cycle of the machine. This is accomplished by providing projections or dimples on the face of a component part to be welded. In this manner, fixtures are generally simplified and extremely uniform assembles are produced. The machine is much heavier in order to counter greater reactive forces, and the welding transformer of sufficient capacity to meet requirements. Pronounced savings over other type of operations are sometimes effected by applying this method.

Resistance Welder Manufacturers' Association

505 Arch Street — Philadelphia, Pa.

MEMBER COMPANIES

Acme Electric Welder Company, Los Angeles
American Electric Fusion Corporation, Chicago
Eisler Engineering Company, Newark, N. J.
Expert Die & Tool Company, Detroit
Federal Machine and Welder Company, Warren
Multi-Hydromatic Welding and Manufacturing Co., Detroit, Mich.
National Electric Welding Machines Co., Bay City
Progressive Welder Company, Detroit
Swift Electric Welder Company, Detroit
Taylor-Hall Welding Corporation, Worcester
Taylor-Winfield Corporation, Warren
Thomson-Gibb Electric Welding Co., Lynn
Welding Machines Mfg. Company, Detroit

ASSOCIATE MEMBER COMPANIES

P. R. Mallory and Co., Indianapolis
S-M-S Corporation, Detroit
Electroloy, Inc., New York
Welding Sales and Engineering Co., Detroit

CONSTITUTION AND BY-LAWS

Constitution of the American Welding Society
Adopted March 27, 1919; Amended October 18, 1937

ARTICLE I

Name

This Society shall be known and designated as the AMERICAN WELDING SOCIETY.

ARTICLE II

Objects

The objects of the Society are:

(*a*) To advance the science and art of welding.

(*b*) To afford its members opportunities for the interchange of ideas with respect to the science and art of welding, and for the publication of information thereon.

(*c*) To conduct research into the science and art of welding, cooperating with other societies, associations and governmental departments for the benefit of the industry in general.

(*d*) To acquire and dispose of property for the purposes aforesaid.

(*e*) To do all other things incidental or conducive to the attainment of the above-named objects, or of any of them.

ARTICLE III

Membership

Individuals, scientific societies, associations or governmental departments interested in welding are eligible for membership in this Society.

ARTICLE IV

Government

The management of the affairs of the Society shall be in the hands of a Board of Directors. The Society for the conduct of its affairs may adopt by-laws, rules and regulations not inconsistent with this Constitution, and may provide methods for amending or repealing such by-laws, rules and regulations different from the method of amending this Constitution.

ARTICLE V

Cooperative Research

In order to further its objects most effectively, the Society shall provide for the formation of a separate body to be known by some appropriate title for the purpose of joining with other societies, associations and governmental departments in cooperative research in welding.

ARTICLE VI

Amendments

This Constitution may be amended by a two-thirds vote of the members voting, expressed orally or in writing, at any Annual Meeting of the Society as set forth in the By-Laws, provided a notice stating the proposed amendment or amendments with the reasons therefore and any known objections shall have been sent to each member at least two weeks prior to said Annual Meeting.

ARTICLE VII

Expulsion of Members

Upon the written request of ten or more members with full rights of membership stating specific reasons therein, any member classifying under Section 2, Article I of the By-Laws may be recommended for expulsion by the Board of Directors. If, at a regular meeting or special meeting of the Board of Directors, it is decided that there is or are presented sufficient reason or reasons for such expulsion, the Board shall notify the accused of the charges against him, by mailing a communication to the address of the accused as it appears in the records of the AMERICAN WELDING SOCIETY. He shall then have the right to present a written defense, and to appear for trial, in person or by duly authorized representative, before a meeting of the Board of Directors, of which meeting he shall be notified at least thirty days in advance. Not less than two months after such a meeting, the Board of Directors shall finally consider the case, and if in the opinion of the Board of Directors the charges have been sustained, the accused may be expelled or suspended for such a period as the Board may determine, or he may be permitted to resign.

By-Laws of the American Welding Society

Adopted March 27, 1919. Amended April 28, 1921, April 14, 1922, January 23, 1924, December 23, 1925, July 20, 1927, March 14, 1931, December 26, 1933, November 4, 1934, May 19, 1935, April 10, 1936, November 18, 1937, April 28, 1938.

ARTICLE I

Membership

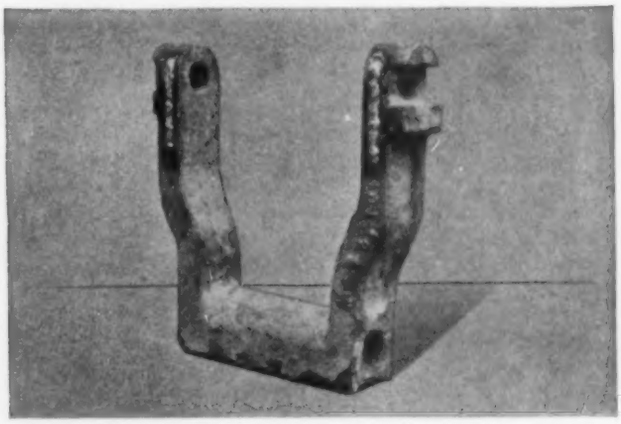
Section 1. Individuals having received the approval of the Committee on Admissions shall become members of the Society upon the payment of dues, except in the case of Honorary Members who shall be elected by unanimous vote of the Board of Directors.

Section 2. Membership shall be divided into six classes:

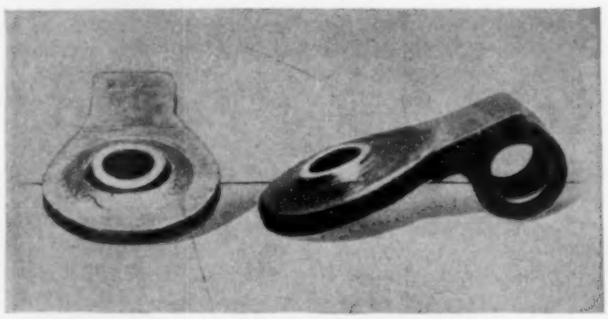
Sustaining Members.—A sustaining member shall be an individual designated by a corporation, firm or part-

Save money by reducing wear with Haynes Stellite Alloy

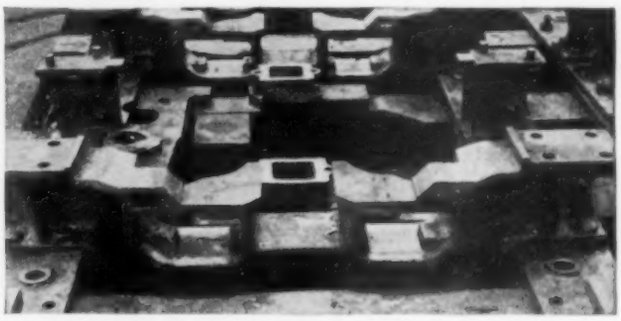
CONSTRUCTION—The teeth and side shoes of this skimmer scoop, hard-faced with Haynes Stellite alloy, are in perfect condition after 15 weeks of use. Plain teeth and shoes wore out in 10 weeks.



CEMENT—Hard-facing cement clinker drag-chain links with Haynes Stellite hard-facing rod makes them resist severe abrasion at high temperatures from 3 to 5 times longer than unprotected links.



GLASS—Experience has shown that each of these two drinking-glass neck molds, which have been hard-faced with Haynes Stellite alloy using the oxy-acetylene process, will outlast 10 molds which are not hard-faced.



RAILROAD—Eighteen wearing surfaces, hard-faced with Haynes Stellite alloy and only slightly worn after 3 years' service, are visible in this photograph of yokes and spacers for railroad car retarders.

Write for the 104-page book, "Hard-Facing With Haynes Stellite Products," which shows how these products can reduce wear and save money in your industry. There is no obligation.

PRODUCT OF A UNIT OF UCC UNION CARBIDE AND CARBON CORPORATION

"Haynes Stellite" is a registered trade-mark of Haynes Stellite Company.

HAYNES STELLITE TRADE MARK

Red-hard, wear-resisting alloy of cobalt, chromium and tungsten

HAYNES STELLITE COMPANY

Unit of Union Carbide and Carbon Corporation

UCC

Chicago · Cleveland · Detroit · Houston · Los Angeles · New York · San Francisco · Tulsa

General Office and Works—Kokomo, Indiana
Foreign Sales Department—New York City

Haynes Stellite hard-facing rods and information on other Haynes Stellite Company products also are available through all apparatus shipping points of The Linde Air Products Company

nership interested in the science and art of welding, with full rights of membership.

Members.—A member shall be an individual not less than 23 years of age, who shall have been for at least three years engaged in work having a direct bearing on the art or science of welding, and shall have made some contribution to the art or science of welding, with *full rights of membership.* Corporate members of any major engineering society are eligible.

Associate Members.—An associate member shall be an individual interested in the science and art of welding with right to vote, but not to hold office except in Sections as may be provided for by the By-Laws of the Section.

Operating Members.—An operating member shall be an individual who, by occupation, is an operator of welding or cutting equipment, without the right to vote or to hold office excepting in the Section as may be provided for by the By-Laws of the Section. Beginning October 1, 1938, no *Operating Member* may continue in this status for more than a total period of 2 years (consecutive or otherwise). At the end of the two year period, the Operating Member shall automatically be moved up to the "Associate" grade.

Resident Operating Members are those residing in the United States, Canada or Mexico. Foreign Operating Members are those outside of the United States, Canada, or Mexico.

Honorary Members.—An honorary member shall be an individual with full rights of Membership. Honorary Members shall be persons of acknowledged eminence in the welding profession, or who may be accredited with exceptional accomplishments in the development of the welding art upon whom the AMERICAN WELDING SOCIETY may see fit to confer an honorary distinction.

Student Members.—A Student Member shall be an individual who is actually in attendance at a recognized college or university, taking a course leading to a degree, without the right to vote or to hold office excepting in Sections as may be provided for by the By-Laws of the Section. At the termination of the fiscal year of status as a student, affiliation as "Student Member" shall cease.

ARTICLE II

Dues

Section 1. The Annual Dues shall be as follows:

Sustaining Members	$100.00
Members	15.00
Associate Members	10.00
Operating Members (resident)	5.00
Operating Members (foreign)	10.00
Honorary Members (there shall be no annual dues)	
Student Members	2.50

Section 2. All dues shall be payable in advance and shall cover the period of one year from the last day of the month in which a member joins.

Section 3. Funds in excess of those provided by the annual dues, which may be needed for research and other work, shall be secured by voluntary subscriptions to be solicited from its members, or from others specially interested.

ARTICLE III

Section Organization

Section 1. The AMERICAN WELDING SOCIETY, in pursuance of the objects set forth in its Constitution, provides for the formation of Sections.

Section 2. Not less than 25 members or eligible applicants for membership in the AMERICAN WELDING SOCIETY in the Sustaining, Member or Associate Member grades may apply for authorization for the organization of a Section.

Section 3. Each active Section of the AMERICAN WELDING SOCIETY shall receive from the Society each calendar year the sum of $100.00 commencing on the date of organization (that date on which authorization to operate as a Section is granted) and on each anniversary thereafter, plus 15% of the sum collected in dues during the calendar year from those members who are in the grades of Sustaining Member, Member and Associate Member in the Section.

No section shall be considered active unless it holds at least three meetings during a calendar year, and has a total paid membership of twenty-five, enrolled in the Sustaining, Member and Associate grades.

Section 4. The By-Laws of the Section shall not conflict with any provision of the Constitution or By-Laws of the AMERICAN WELDING SOCIETY.

ARTICLE IV

Officers, Nominations and Elections

Section 1. The Officers of the Society shall be a President, a First Vice-President, a Second Vice-President, five District Vice-Presidents, a Treasurer, three Junior Past-Presidents and twelve Directors-at-large. There shall also be a Secretary appointed by the Board of Directors.

Section 2. The President shall be elected by the Society for a term of one year and may be re-elected at any time for a second term of one year, but in no case shall any individual serve in this capacity for more than two consecutive years.

Section 3. A First Vice-President and a Second Vice-President shall be elected by the Society for a term of one year and each may be re-elected at any time for a second term of one year, but in no case shall he serve in this capacity for more than two consecutive years. The District Vice-Presidents shall be elected by their districts for a term of one year and each may be re-elected at any time for a second term of one year, but in no case shall he serve in this capacity for more than two consecutive years.

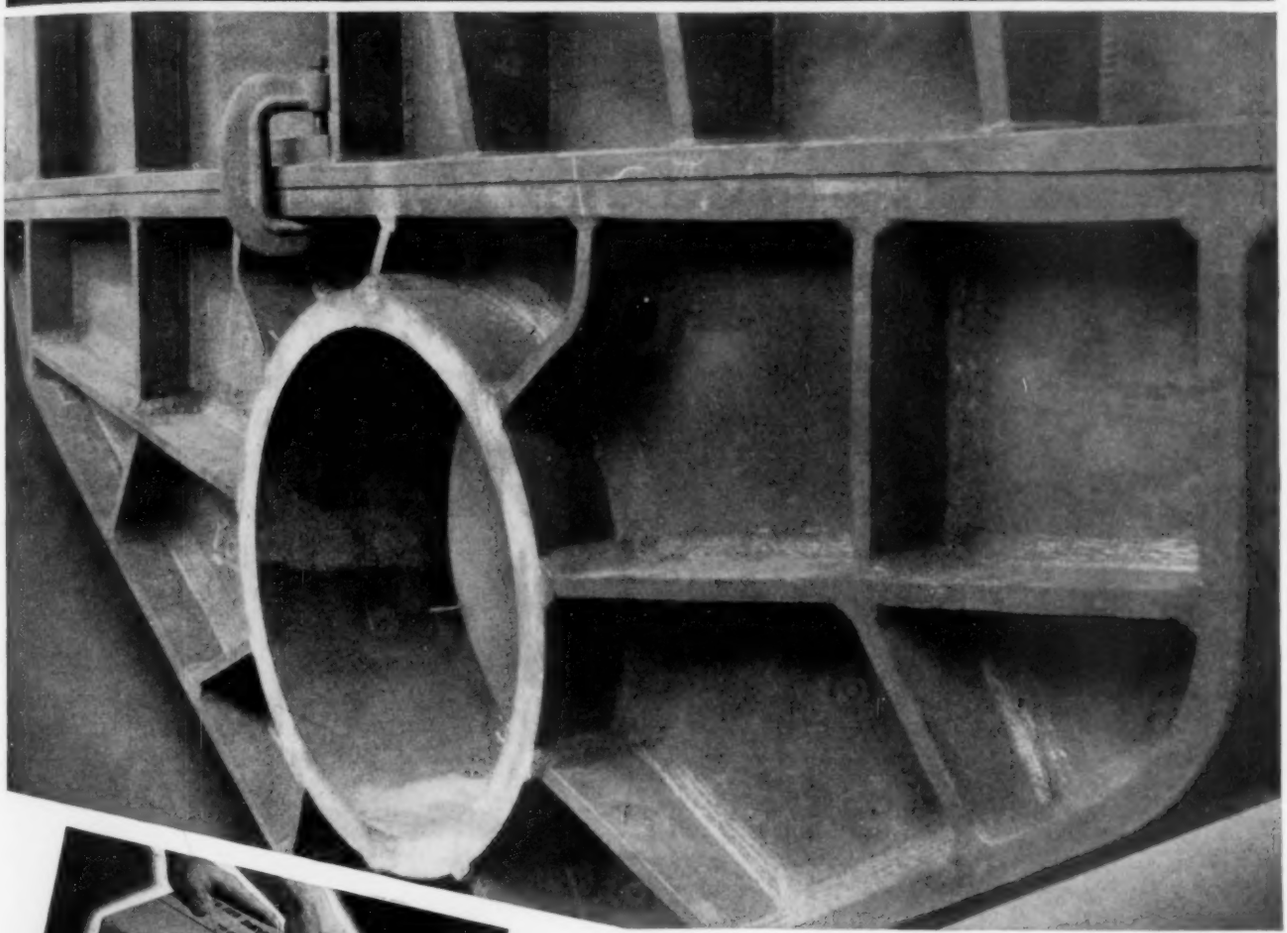
Section 4. A Treasurer shall be elected by the Society for a term of three years and is eligible for re-election.

Section 5. The Board of Directors shall consist of the President, the First and Second Vice-Presidents, the five District Vice-Presidents, the three Junior Past-Presidents, the Treasurer and the twelve Directors-at-large. The Directors-at-large shall be selected by the Nominating Committee with due consideration to geographical distribution. At each regular Annual Meeting, four Directors-at-large shall be elected to serve for three years. The incumbents elected prior to the adoption of these By-Laws shall continue in office until their term expires.

Section 6. Nominations and elections of the Officers shall proceed as follows:

(*a*) The Secretary shall mail on or before the first of June of each year to each member entitled to vote a notice that nominations for Officers must reach the Secretary not later than the first day of July. This notice shall outline the vacancies of elective offices that shall be filled at the next regular election and request nominations for such offices. This notice shall contain a copy of this

No Kicks Thanks to Champion Electrodes



CHAMPION Welding Electrodes are packed in a heavy corrugated cardboard shipping container which is sealed on all sides, and there is imbedded in the cover a wire with a loop to facilitate easy opening. Try CHAMPION Red Devil, Blue Devil, Gray Devil or Black Devil Electrodes—there is a type to meet your exacting requirements. . . . *Send for descriptive folder.*

THIS illustration shows the rear view of what is believed to be the heaviest piece of built up structural steel ever fabricated by electric welding. . . . The design of this structure called for very substantial fillet welds with exceptional physical properties. CHAMPION Electrodes were used throughout with ***complete satisfaction.***

THE CHAMPION RIVET COMPANY

CLEVELAND, OHIO **EAST CHICAGO, INDIANA**

Section and the list of nominees recommended by the Nominating Committee and information that no name shall appear on the final voting ballot unless proposed by at least 25 qualified members. The notice shall also contain the names of the members of the Nominating Committee.

(*b*) The nomination blank must be placed in a plain envelope and sealed; then inserted in a second envelope and sealed. The voter will then handwrite his name thereon in ink and mail it to the Secretary. The Secretary will certify to the competence and signature of all voters returning endorsed nomination ballots and will then deliver them to the Nominating Committee.

A nomination ballot without autographic endorsement of the voter written on the outside envelope is defective and shall not be canvassed by the Nominating Committee. The Nominating Committee shall first open and destroy the outer envelopes of all properly endorsed ballots, and shall then open the inner envelopes, canvass the ballots and certify the results to the Secretary. If no nominations, other than proposed by the Committee, are made by 25 qualified members, then the Secretary shall treat the nomination ballots as Election Ballots and certify the results to the Presiding Officer at the Annual Meeting.

(*c*) If a member, not proposed by the Nominating Committee, is duly nominated as provided for in paragraph (*a*) of this Section then the Secretary shall mail on or before the second Tuesday in July of each year to each member entitled to vote a ballot stating the names of the candidates for the several offices falling vacant and the time of the closure of the voting. The voter shall prepare his ballot by indicating by means of a cross opposite the name of each candidate he wishes to vote for. The voter shall enclose said ballot in an envelope and seal same. He shall then enclose the sealed envelope in a second envelope marked "Ballot for Officers," seal same and write his name thereon in ink, for identification. The ballot thus prepared and enclosed shall be mailed or delivered unopened to the Tellers of Election.

The Secretary shall certify to the competence and signatures of all voters. A ballot without the autographic endorsement of the voter written on the outside envelope is defective and shall be rejected by the Tellers of Election. A ballot which has choice indicated for more names than there are offices to be filled is defective and shall be rejected by the Tellers.

Section 7. The voting for the election of officers shall close at 1 o'clock in the afternoon on the last Thursday in August of each year. The Tellers shall not receive any ballot after the stated time for the closure of the voting. The Tellers of Election shall first open and destroy the outer envelopes and shall then open the inner ones, canvass the ballots and certify the results to the Presiding Officer at the annual meeting of the Society. The Presiding Officer shall then announce the candidates having the greatest number of votes for their respective offices and declare them elected for the ensuing year.

Section 8. The ballot sheet shall list the names of the candidates proposed by the Nomination Committee and the respective offices for which they are candidates and the names of candidates nominated by letter ballot as provided in Section 6 above. Candidates selected by the Nominating Committee shall be identified by an asterisk.

Section 9. In case of a tie in the vote for any office, the President, or in his absence, the Presiding Officer, shall cast the deciding vote.

Section 10. The term of all Elected Officers shall begin on the adjournment of the Annual Meeting of the Society. Officers shall continue in their respective offices until their successors have been elected.

Section 11. Tellers of Election. The President shall, on or before the last Thursday in August of each year, appoint three Tellers of Election of Officers, whose duty shall be to canvass the votes cast, and certify the same to the President, or Presiding Officer, at the Annual Meeting. Their term of office shall expire when their report of the canvass has been presented and accepted.

ARTICLE V

Geographical Districts

Section 1. There shall be five districts known as the New York and New England, the Middle Eastern, the Middle Western, the Southern and the Pacific Coast Districts, the boundaries of which shall be determined by the Board of Directors each year.

Section 2. No district shall be considered as "operative" unless it contains at least three active Sections.

Section 3. Should the territory of any Section lie in more than one district as defined above, then the entire territory of said Section shall be considered as belonging to the District in which the headquarters of the Section are located.

Section 4. To facilitate cooperation between the Sections there shall be an Executive Committee in each district including in its membership the Vice-President for that District as Chairman, a District Secretary to be appointed by the *District* Vice-President, and the Chairmen and Secretaries of the Sections within the district.

District Meetings

Section 5. District Meetings shall be held, upon the approval of the Board of Directors of the Society in each instance, under the supervision of the District officers and committees in accordance with the following general plan:

Upon expression by a District Executive Committee of a desire to hold a District meeting and approval by the Board of Directors, the responsibility for the meeting shall rest with the District Executive Committee. This Committee shall have full responsibility and authority for organizing and conducting such meeting in all its details, including the arrangement of sessions, the entertainment features, and the selection of papers. Papers for a District meeting may be obtained from District membership and if papers are desired from outside the District, they shall be correlated with the national Technical Program Committee. All papers included in the program shall be considered for publication in the Journal upon the same basis as papers for national meetings. The financing of District meetings shall be in accordance with a consistent general plan approved from time to time by the Board of Directors, after consideration of such recommendations as may be received from the officers and committees concerned.

District Nominations

Section 6. Before *January 1* of each year, each District Executive Committee shall appoint a District Nominating Committee. Each District Nominating Committee shall nominate a District Vice-President and send the name of the nominee to the National Secretary not later than *March 15th* of each year.

The nominations made by the Districts shall be published in the *April* issue of THE WELDING JOURNAL of the AMERICAN WELDING SOCIETY.

Independent nominations may be made by a petition

VICTOR

WELDING, soldering, lead burning, heating, de-scaling, flame hardening, cutting, scarfing or de-seaming torches have but one objective—to produce the right kind of a flame or flames required for the process and the operation involved.

And so—VICTOR offers "Flames For Sale"—the most dependable, economical and suitable flames for your job.

25 YEARS OF WELDING EQUIPMENT MANUFACTURE

DISTRIBUTING POINTS from COAST to COAST

VICTOR EQUIPMENT COMPANY

Executive Offices:
844-50 Folsom St., San Francisco

of twenty-five (25) or more members sent to the National Secretary not later than *May 15th;* such petitions for the nomination of District Vice-Presidents shall be signed only by members within the district concerned.

Each year the *National Ballot* for Nomination of Officers shall include the names of the District Vice-Presidents nominated by the District Nominating Committees and names of nominees nominated by petition in accordance with Section provision above.

Only District members shall be eligible to vote for District Vice-Presidents.

Vacancies

Section 7. The Executive Committee of each district shall have the power to fill a vacancy in the office of Vice-President of that district for an unexpired term, whenever such vacancy occurs.

ARTICLE VI

Duties of Officers

Section 1. In addition to the duties hereinafter set forth, the Officers shall perform such other duties as the Society or Board of Directors shall designate.

Section 2. The President shall preside at all meetings of the Society and of the Board of Directors; he shall have general active management and supervision of the affairs of the Society; shall see that all orders and resolutions of the Board are carried into effect; shall attend generally to its executive business and the supervision and direction of all the other Officers in the proper performance of their respective duties; shall submit a report of the operations of the Society for the fiscal year to the Board of Directors at their last regular meeting preceding the Annual Meeting, and to the members at the Annual Meeting in October and from time to time shall report to the Board of Directors all matters within his knowledge which the interests of the Society may require to be brought to their notice; shall be, *ex-officio*, a member of all standing committees; and shall have the general powers and management usually vested in the office of the President of a Society.

Section 3. The First Vice-President shall be vested with all the powers, and required to perform all the duties of the President in his absence.

Section 4. The Second Vice-President shall be vested with all the powers and required to perform all the duties of the First Vice-President in his absence.

Section 5. A Secretary shall be appointed by the Board of Directors for a term of one year and shall be the Office Manager of the Society. The Secretary shall attend all meetings of the Society and Board of Directors and record the proceedings thereof. The Secretary shall collect all moneys due the Society and deposit the same in depositories designated by the Board of Directors, reporting such deposit to the Treasurer. The Secretary shall conduct the correspondence of the Society and shall keep full records thereof. Under the President and Board of Directors the Secretary shall be in responsible charge of all property of the Society. With the approval of the Board of Directors, the Secretary shall engage such employees as may be necessary and shall be responsible for the work of all employees of the Society. The Secretary shall perform such other duties as may be assigned. The Secretary shall devote his entire time to the affairs of the Society, unless otherwise authorized by the Board of Directors. The Secretary shall be required to furnish bond for the faithful performance of his duties in an amount to be decided by the Board of Directors.

Section 6. The Treasurer shall keep full and accurate accounts of receipts and disbursements in books belonging to the Society, and shall deposit all moneys and valuable effects in the name, and to the credit of the Society, in such depositories as may be designated by the Board of Directors. All checks shall be signed by the Treasurer. He shall disburse the funds of the Society as may be ordered by the Board of Directors, taking the proper vouchers to such disbursements, and shall render to the President and Directors at the meeting of the Board of Directors, or whenever they may require it, an account of all its transactions as Treasurer, and of the financial condition of the Society, and at the last regular meeting of the Board of Directors preceding the Annual Meeting of the Society, a like report for the preceding year. He shall give the Society a bond in a sum and with one or more securities as required by and satisfactory to the Board of Directors for the faithful performance of the duties of his office, and the restoration to the Society in the case of his death, resignation or removal from office of all books, papers, vouchers, money or other property of whatever kind in his possession belonging to the Society.

Section 7. The President with the approval of the Board of Directors may appoint other paid Officers for such terms and duties and at such salaries as the Board of Directors may deem advisable.

ARTICLE VII

Board of Directors

Section 1. The Board of Directors shall have the power:

(*a*) To manage the affairs of the Society, except as otherwise provided by law, or by these By-Laws.

(*b*) To purchase or otherwise acquire for the Society any property, rights and privileges which the Society is authorized to acquire, at such prices and on such terms and conditions and for such considerations as they think fit.

(*c*) To appoint Executive Officers and fix their salaries.

(*d*) To determine who shall be authorized to sign, on behalf of the Society, notes, receipts, acceptances, endorsements, checks, releases, any and all contracts and other documents, and shall make such authorization.

(*e*) To perform such other acts as may be necessary to carry out the purpose of the Society.

Section 2. In each case of vacancy occurring in the Board of Directors through death, resignation, disqualification or other cause, the remaining Directors, by letter ballot, may elect, or appoint a successor to hold office for the unexpired portion of the term of the retiring Director; and in the event that a quorum of the Board of Directors does not exist, the affirmative vote of a majority of the remaining Directors shall become effective. However, if the retiring Director is a District Vice-President then the vacancy shall be filled as provided in Article V, Section 7.

ARTICLE VIII

Committees

Section 1. With the approval of the Board of Directors, the President shall appoint the following committees:

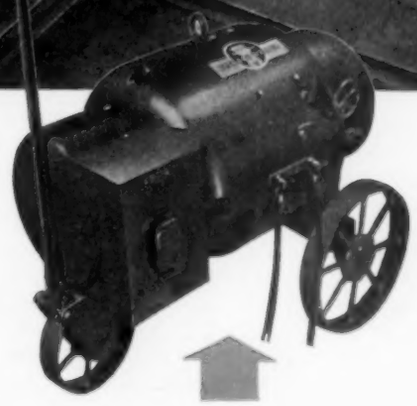
(*a*) An Executive Committee, which shall have such duties as may be prescribed by the Directors to facilitate their work and such power of the Board of Directors as they may delegate to it from time to time.

GIVE IT THE
"ROUGH RIDING"
TEST *
*TRY A
FLEXARC WELDER
ON YOUR OWN WORK—MAKE ANY COMPARATIVE TEST! YOU'LL FIND WESTINGHOUSE
ONE PRE-SET CONTROL
GIVES ADVANTAGES YOU CANNOT GET WITH ANY OTHER WELDER.

FREEDOM FROM "NUISANCE GADGETS" MAKES FLEXARC WELDER HUSKIER AS WELL AS FASTER

This welding machine will stand rougher handling, harder usage, and higher overloads than any conventional type welder, because it has no fragile meters, fine wires or delicate control parts to get out of order. Note the armored plate frame, the ruggedness of the whole machine. It will take the jars and jolts of moving from one job to another. And when the operator starts to weld — he sets only ONE control and the machine pours in the metal.

See and compare the **FLEXARC** — prove its ability to take the punishment of heaviest, everyday service — before you put your money into any welder. Westinghouse Electric & Mfg. Co., East Pittsburgh, Pa., Dept. 7N.



NOW—YOU CAN RENT A FLEXARC WELDER!

Rent a FLEXARC if you like — easy terms, and rental applies against purchase price if you decide to buy any time within one year. Mail the coupon for details.

J-20914

Westinghouse Flexarc Welders

Show Us!

☐ Send full details on rental plan.
☐ Arrange to demonstrate your machine.

Name_____
Company_____
Address_____
City_____

(*b*) A Finance Committee, selected from members of the Board of Directors, which shall pass upon all expenditures, prepare the annual budget and have general charge of the finances of the Society. The Treasurer shall be Chairman of the Finance Committee.

(*c*) A Membership Committee of five or more whose duties shall be to formulate and execute plans and prepare literature for maintaining and increasing the membership of the Society.

(*d*) A Committee on Admissions of five members whose duty it shall be to pass on the qualifications of all members to determine their eligibility and classification as members of the Society.

(*e*) A Meetings and Papers Committee which shall plan and make arrangements for the meetings of the Society. It shall also solicit and pass upon all papers offered for presentation to the Society and determine what papers shall be published. It shall also have power to edit papers before publication.

(*f*) A Nominating Committee of seven members. Announcement of the appointment shall be made by the President through publication in the JOURNAL on or before *March 1st*. The Committee shall consist of a chairman who shall be a Past-President of the Society and six members (not members of the Board of Directors), one of whom shall be a Past-President of the Society. The Committee shall deliver to the Secretary in writing on or before the last Tuesday in May the names of its nominees for the various elective offices next falling vacant together with the written acceptance of each nominee.

(*g*) A Revision of By-Laws Committee comprising a Chairman and two other members. This Committee shall receive suggestions for revision of the By-Laws and be responsible for the preliminary wording of such revisions. It shall see that the By-Laws of Sections of the Society shall not conflict with any provision of the Constitution and By-Laws of the AMERICAN WELDING SOCIETY. This Committee shall consider and report upon any petition for revision and shall transmit such petition to the Secretary to be presented at a Board of Directors' meeting as specified under Article XIV.

(*h*) A Committee on Code of Principles of Conduct consisting of five members comprising a Chairman, (member of Board of Directors) and four additional members. In addition to the function of formulating and administering a Code, it shall be the duty of the Committee to advise inquirers regarding questions of proper conduct, and to examine into and investigate any practice of any member of this Society which shall be regarded as prejudicial to the welfare of the Society, and report its actions and recommendations to the Board of Directors, which shall take such action as it may deem proper.

(*i*) A Committee on Awards consisting of three individuals each serving for a period of three years. The appointments shall at first be arranged in such a way that one member shall retire at each Annual Meeting. The member senior in respect to service shall be Chairman. It shall be the duty of this Committee to make the award and arrange for the presentation of the Samuel Wylie Miller Memorial Medal, the Lincoln Gold Medal and other awards.

(*j*) A Committee on Outline of Work, shall include five members of the Board of Directors, one of whom, preferably the Chairman, shall be either the First Vice-President or Second Vice-President. The duty of this Committee shall be to recommend the personnel of all newly appointed Technical Committees including the Chairmen, and to define the scope of the work of the Committees. It shall in general:

1. Determine the need for the continuance of a committee.
2. Determine whether a committee's work shall be expanded.
3. Decide questions of jurisdiction between various committees, and prevent overlapping of committee activities.
4. Determine the need for new committees.
5. Before recommending the appointment of new committees, determine the probable expense to the Society in carrying through such work.
6. Estimate and set the probable time for undertaking and completing work.
7. Correlate and control the standardization work and the activities of the committees in the development of codes and other regulations. This applies to procedure and not to details.
8. Recommend the appointment of representatives of the Society to act in connection with the development of codes, standards, practices, etc. by outside parties.

(*k*) An Educational Committee whose duties shall be to assist and stimulate the Sections in the preparation of lecture courses and in other educational activities.

(*l*) A Committee or Committees for a definite stated purpose or purposes may be proposed by any member of the Society in good standing together with the personnel of such a Committee or Committees but the final selection of the personnel must be by a majority vote (of the quorum) at one of the meetings of the Society as set forth under Article IX, Section 1, provided the necessary quorum, per Article IX, Section 3, is present and further provided that definite notice stating all purposes and objectives of such Committee or Committees shall have been sent to each member, and/or published in the JOURNAL OF THE AMERICAN WELDING SOCIETY for the preceding month, so as to reach the membership at least two weeks prior to said meeting at which the Committee or Committees will be proposed and elected.

(*m*) With the approval of the Board of Directors the President may appoint additional Committees for a *definite stated purpose or purposes*.

Section 2. All Committee appointments not otherwise specified shall expire with the Society year.

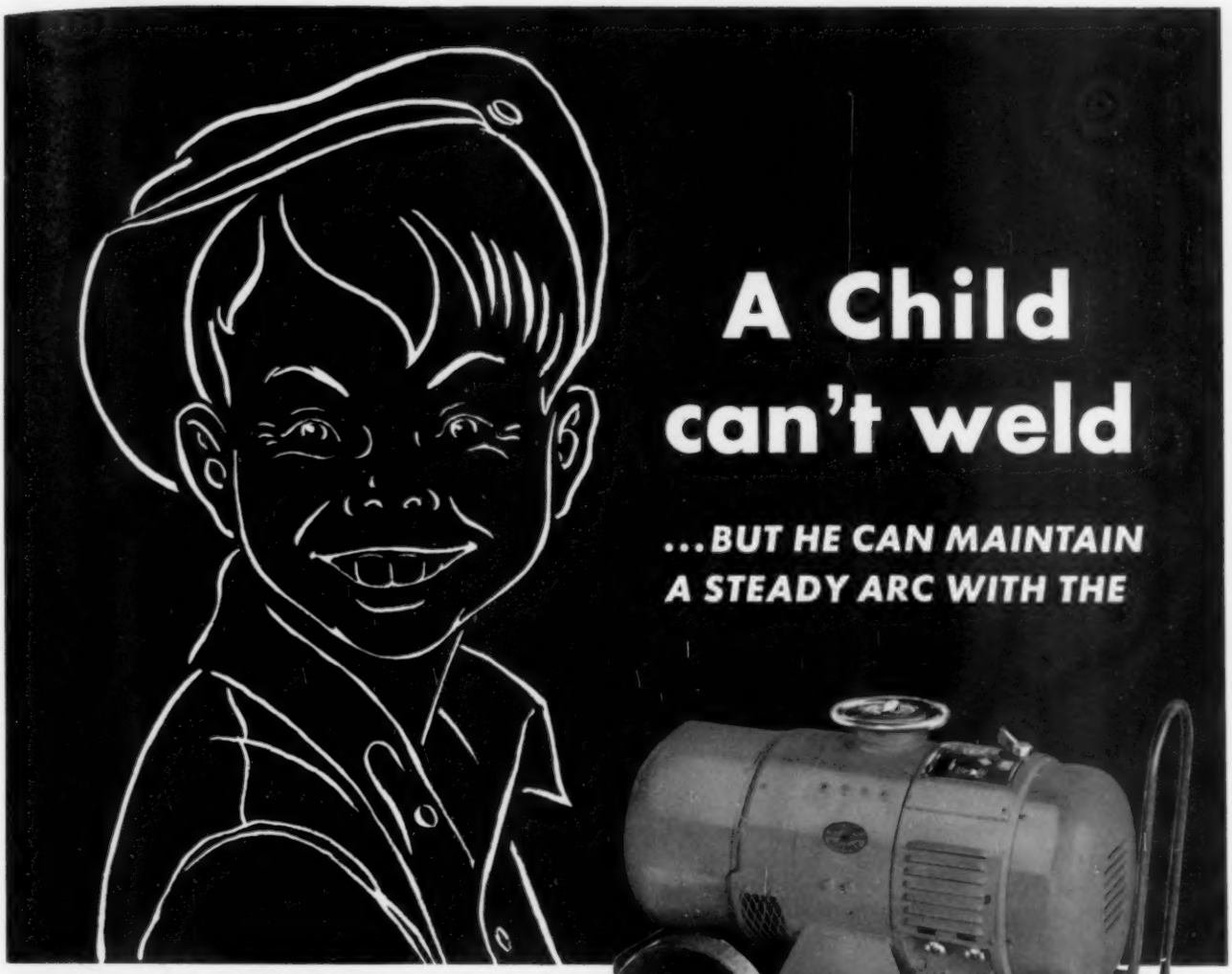
ARTICLE IX

Meetings and Notices

Section 1. There shall be an Annual Meeting of the Society for the election of Officers and other business, held sometime in the Fall of the year at such time and place as may be decided by the Board of Directors. The order of business at the Annual Meeting shall be such that the election of Officers shall be the last item of business at such meeting. Other meetings may be called at the discretion of the Board of Directors.

Section 2. Notices of all meetings of the Society shall be mailed to the members at least 30 days prior to the date of such meeting, and to the Board of Directors at least two weeks prior to the date of their meeting, and shall designate the time and place of the meeting and the principal business to come before it.

Section 3. Twenty members entitled to vote shall constitute a quorum at meetings of the Society and ten Directors shall constitute a quorum at meetings of the Board of Directors.



A Child can't weld

...BUT HE CAN MAINTAIN A STEADY ARC WITH THE

New WILSON "HORNET"*

Set the sturdy hand control on the new Wilson Welder and even a child can maintain a steady arc. Once the new Wilson "Hornet" control has been set, the operator can concentrate on his welding job. It is unnecessary for the operator to stop work to readjust the control because variations in the internal temperature of the machine have no material effect on current output. The complete story of this modern new welding generator will interest you. Write for your copy of the new bulletin on the Wilson "Hornet" the latest of the Wilson line of "machines that make the arc behave."

*TRADE MARK

WRITE FOR YOUR COPY

of the new Wilson "Hornet" bulletin featuring the newest of... "the machines that make the arc behave."

WILSON WELDER and METALS CO., INC.

***General Offices:* 60 East 42nd Street, New York, N. Y.**

DISTRIBUTED THROUGH AIR REDUCTION SALES COMPANY'S DISTRICT OFFICES IN PRINCIPAL CITIES

ARTICLE X

Voting and Proxies

Section 1. Voting by proxy shall not be allowed at any meeting of the Society or its Board of Directors or of any of its Committees except by a personal representative presenting written credentials at the particular meeting stated.

ARTICLE XI

Welding Research

Section 1. The AMERICAN WELDING SOCIETY shall provide the means for cooperative research in welding by creating or joining in the creation of a research department to be known as the Welding Research Committee. Other societies, associations and governmental departments may be asked to assist in this work.

Section 2. Funds for the work of the Welding Research Committee shall be secured by voluntary contributions, by solicitation or appropriated from the regular funds of the Society. Solicitation of any funds in the name of the AMERICAN WELDING SOCIETY may, however, only be done with the approval of the Board of Directors.

Section 3. Custodian of funds. The custodian of Research Funds shall be the Engineering Foundation, the AMERICAN WELDING SOCIETY or any other body designated by the Board of Directors.

Section 4. Publications. A separate section of THE WELDING JOURNAL of the AMERICAN WELDING SOCIETY may be created for the publications of the Welding Research Committee.

ARTICLE XII

Permanent Office

Section 1. A permanent office of the Society shall be maintained in the City of New York and shall be located in the Engineering Societies' Building at 29–33 West 39th Street, or at such other place as the Directors may determine.

ARTICLE XIII

Parliamentary Rules

Section 1. Roberts' Rules of Order shall be the governing parliamentary law of the Society in all cases not definitely provided for by its Constitution or its By-Laws or its own rules.

ARTICLE XIV

Amendments

Changes in these By-Laws may be made by petition signed by not less than ten voting members of the Society and approved by not less than two-thirds of the Directors present at a meeting regularly called for consideration of the same. Such changes shall be considered temporary and at the expiration of 60 days after proper letter ballot has been sent to the members of the Society shall be considered permanent, unless rejected by 20 per cent of the voting membership.

Rulings Adopted by Board of Directors

April 1920

Powers of Executive Committee—The Board of Directors delegated to Executive Committee its full power with understanding that Executive Committee will immediately report by letter to every member of the Board any action which it has taken and allow sufficient time for objection to such action before it is put into effect. Should a majority of the Board express objection within a week to any action, the President will then immediately call a special meeting of the Board and have the matter presented to them for confirmation.

Section Organization—The Officers of the Society are authorized to assist in formation of sections in any part of the United States under the adopted standard form of Constitution with variations to meet local conditions and to grant the necessary authority for the formation of such sections. When possible, the sections should be affiliated with the local general engineering society and where practicable, the organization work should be conducted from such society headquarters.

NOTE: It has usually been found inexpedient to authorize a Section unless there are at least 15 members in the Sustaining and/or Member grades.

January 1922, and revised December 1927

Delinquents—A delinquent member shall be carried on the rolls of the Society for a period of six months for the first two months of which he shall be supplied with the Proceedings and other literature, but that following the second month, he shall receive only notices of meetings.

Billing Membership Dues—Each member shall be sent a bill on the first day of the month in which his dues become due and a second bill shall be sent one month later stamped with the words "Final Notice, You Will Not Receive Publication if Your Dues Are Not Paid."

October 1922

Refund to New Sections—No money shall be turned over to a newly organized Section on dues received previous to the establishment of the Section from members located in that particular Section.

February 1923

Plans for establishing Sections of the AMERICAN WELDING SOCIETY which received approval at the annual meeting of the Society in April 1923, and were revised in June 1938, are available at National Headquarters. These embrace all steps to be followed under the headings given below:

1. Extracts from Constitution and By-Laws of Society relating to Section Organization.
2. Preliminary Meeting—Notice; order of business; application to National Organization for authority to operate as Section; suggestions to nomination committee.

WELDED STEEL makes a BETTER BREAD SLICER

Photos Courtesy Oliver Machinery Company

You can check these doubly significant bread-slicer facts in a report by Oliver Machinery Co., Grand Rapids, Mich.

For the *slicing-machine user,* welded steel makes these important "cuts"—

SAVING OF 60 SQUARE INCHES OF FLOOR SPACE PER MACHINE:—Welded steel reduces machine base area by compact construction.

REDUCTION OF 30% IN WEIGHT:—Welded steel much stronger per pound than previous structural material.

REDUCTION IN BEARING WEAR:—Welded steel increases accuracy of bearing alignment.

For the *slicing-machine manufacturer,* welded steel makes these important "cuts"—

REDUCTION IN RESISTANCE TO SALE:—Welded steel gives customers a machine that is better looking, lighter, easier to handle, and more durable.

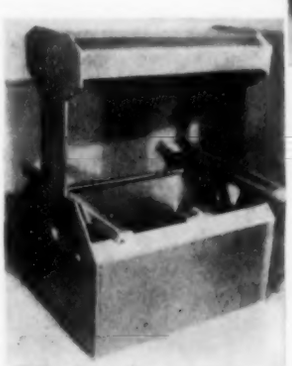
SAVING ON COST OF NEW PRODUCT DEVELOPMENT:—Welded steel provides simplest possible fabrication of trial designs. Easy changes during experimental assembly. No patterns.

SIMPLIFICATION OF MANUFACTURE:—Welded steel simplifies designing and detailing, speeds assembly by eliminating extra operations, saves handling and minimizes costs.

Check over the double benefits of welded steel applied to your own products. For valuable assistance, call in experienced Lincoln welding engineers and technicians.

Consult your nearest Lincoln office or mail the coupon.

ABOVE—Bread slicer; better looking, easier to handle, and more durable because *"Shield-Arc"* welded.



ABOVE—Old slicer frame; heavy, bulky, costly to manufacture.

LEFT—*"Shield-Arc"* welded slicer frame; less costly to manufacture and 30% lighter than old design.

Largest Manufacturers of Arc Welding Equipment in the World

THE LINCOLN ELECTRIC CO.

CLEVELAND, OHIO

THE LINCOLN ELECTRIC CO., Dept. DD-655, Cleveland, Ohio

Send particulars on designing machinery for arc welding (Bulletin 420).

Name _____ Position _____

Company _____

Address _____

City _____ State _____

3. Organization Meeting—Notice; order of business.
4. Method of Conducting Section Work—Affiliation with some engineering body so as to provide meeting place for monthly meetings and in that way give the Section a better technical standing; standard method for handling applications, financial records and reports.

May 1926

A Year Book shall be published as the December issue of the JOURNAL and bound in the same paper cover as the JOURNAL. It shall contain the Constitution and By-Laws of the Society, rulings adopted by the Board of Directors, rulings governing the Meetings and Papers Committee, and Board of Awards, description of Society, its functions, publication, relation to industry; value of membership; how to become a member; description of technical and special committees of Society; personnel of Society committees; Membership list (alphabetical and geographical) and list of "Sustaining Companies" with a description of their products.

December 1928

Routine for handling questions referred to A. W. S. Conference Committee on Welding by A. S. M. E. Boiler Code Committee. All replies prepared by the A. W. S. Conference Committee on questions relating to welding matters submitted to Conference Committee by A. S. M. E. Boiler Code Committee shall be approved by Executive Committee of Society, and in cases where questions referred to Conference Committee deal with matters of a legal nature, replies must be approved by Board of Directors, before submittal to A. S. M. E. Boiler Code Committee.

January 1929

Replies prepared by A. W. S. Conference Committee for A. S. M. E. Boiler Code Committee shall be published in A. W. S. JOURNAL after replies have been approved by A. W. S. and A. S. M. E., and if publication of replies meets with approval of A. S. M. E.

March 1932

Meetings and Papers Committee—In determining the suitability of a subject for presentation at Society meetings, the Meetings and Papers Committee shall be governed by the beneficial results to be obtained from the paper rather than whether or not the subject is controversial; where the Meetings and Papers Committee is in doubt regarding a subject of importance, the matter shall be referred to the Executive Committee for ruling thereon.

December 1935

Listing under "Sustaining Companies" in the Year Book shall be limited to companies supporting the Society to the extent of $100 in dues of "Sustaining Member" or the equivalent in dues of "Members" and "Associate Members," or through advertising in the Society JOURNAL.

November 1937

The President is empowered, when in his opinion it is deemed wise, to invite non-members to meetings of either the Board of Directors or Executive Committee.

Voting at Executive Committee Meetings is limited to Executive Committee members or their proxies. Members of the Board of Directors are invited to Executive Committee meetings for the purpose of participating in discussion.

July 1938

Establishment of Society policy on news items or radio broadcasts detrimental to welding—

It is preferable for the Society to remain silent until official report dealing with the subject in question has been received.

October 1938

The fiscal year of the Society is established commencing October 1st and ending September 30th.

December 1938

Journal be made available to anyone desiring it at the following subscription prices: U. S. and Possessions—$5.00; Canada and Mexico—$5.50; Foreign Countries—$7.00.

March 1939

That the AMERICAN WELDING SOCIETY reserves the right or privilege of publishing as an A. W. S. Standard any code, specification or standard prepared by its committees, either jointly or in cooperation with another Society or other Societies, associations or regulatory bodies.

September 1939

Consideration was given to proposal for establishment of policy for disbanding inactive Sections as covered by memorandum submitted to the Executive Committee on September 11, 1939, and it was decided each case should be acted upon on its merits after complete report had been rendered thereon by the Secretary.

SETS OF BOUND VOLUMES OF JOURNAL

In order to provide some of the new members of the Society with an opportunity to complete their library on Current Welding Literature, the AMERICAN WELDING SOCIETY is offering to these new members an opportunity to purchase the last 8 bound volumes for the years 1932, 1933, 1934, 1935, 1936, 1937, 1938, 1939, at $35.00 a set plus postage. The price to non-members for individual volumes is $6.50 and for the set $40.00 plus postage.

Each bound volume contains a Subject and Authors' Index and is bound in attractive imitation black leather covers. The set probably includes the most important welding information available in the literature.

THE ENGINEERING FOUNDATION

Welding Research Committee

Sponsored by the American Welding Society » » » » and American Institute of Electrical Engineers

Supplement to the Journal of the American Welding Society, January 1939

THE EFFECT OF OXYGEN ON THE Welding of Steel

A Review of the Literature to July 1, 1937

By W. SPRARAGEN* and G. E. CLAUSSEN†

This report is prepared under the auspices of

THE LITERATURE DIVISION OF THE ENGINEERING FOUNDATION—WELDING RESEARCH COMMITTEE

TABLE OF CONTENTS

	Page
Summary	1
Introduction	2
Oxygen Pick Up in Welding	2
Welding in Oxygen	4
Brittleness	4
Spatter and Porosity	5
The McQuaid-Ehn Test	5
Oxides in Welds	6
Forge Welding	7
Flame Cutting	7
Acknowledgments	7
Bibliography	7
Suggested Research Problems	7

Summary

THE EFFECT OF OXYGEN ON THE WELDING OF STEEL

THE oxygen content of industrial steels ranges from about 0.004 to 0.10% according to the method of manufacture and particularly the carbon content.

There is information on the oxygen content of welds, but the form in which the oxygen occurs has not been clearly resolved for all types of welds and filler metals. Again high oxygen content in weld metal is known to be associated with poor physical properties, yet other impurities, such as nitrogen, have obscured the precise part played by oxygen.

OXYGEN PICK UP IN WELDING

In bare wire welding a long arc promotes oxygen pick up whereas use of large diameter electrodes and electrodes of high manganese content tends to decrease the oxygen pick up. Deposits from bare electrodes contained 0.18 to 0.95% O_2, average 0.2%. As low as 0.035% O_2 has been reported for deposits from covered electrodes. On the average, oxyacetylene welds contain about 0.05% O_2, but as low as 0.007% has been found.

The oxygen pick up was reduced 50% by increasing the thickness of coating. Restricting the access of air by using small scarf angles and relatively few layers reduced the oxygen pick up by as much as 30%. The meager information available suggests that coatings, carbon, and manganese reduce oxygen pick up. Of these, manganese is the most effective.

BRITTLENESS

Although the brittleness of arc welds made with bare or ineffectively coated electrodes often has been attributed to nitrogen pick up, one author showed that oxygen pick up, as well, may account for low ductility. The FeO rather than the nitrogen seemed to be the effective agent in reducing ductility and Izod value. Oxygen is partly responsible for aging brittleness in arc weld metal. Oxygen was the most dangerous of all elements in reducing the mechanical properties of oxyacetylene welds in mild steel.

There is a belief that oxygen is responsible for the hot shortness of low grade weld metal. Bending at 1050° C. revealed that bare electrode weld metal was hot short.

THE McQUAID-EHN TEST

Oxygen in the weld deposit is regarded as an important cause of abnormality in the McQuaid-Ehn test. In one test a bare electrode deposit was exceptionally abnormal; the deposit from a cellulose covered electrode was normal.

* Secretary, Welding Research Committee.
† Research Assistant, Welding Research Committee.

The Effect of Oxygen on the Welding of Steel

INTRODUCTION

OXYGEN is involved in practically all welding processes. In some welding processes (oxyacetylene and thermit) oxygen is a means of supplying heat. In arc and flash welding the arc is drawn in air, although in some processes the arc atmosphere through which the metal is transported may be deprived of uncombined oxygen. There is a layer of oxide, however thin, that coats all steel prior to resistance welding (spot and butt). Welding fluxes and slags nearly always are based on oxides. Finally, there is oxygen, dissolved or as oxide, in the steels used as base metal and filler rods.

Oxygen is ordinarily a molecule consisting of two atoms. As the temperature is raised the exceedingly small concentration of ozone (O_3) or atomic oxygen gradually increases but appears to remain small even at the temperature of the iron arc. If iron is heated in oxygen, the surface is first converted to FeO (ferrous oxide) then to Fe_3O_4 (magnetic oxide) lastly to Fe_2O_3 (ferric oxide). FeO melts at about 1370° C., Fe_3O_4 and Fe_2O_3 melt at about 1550° C., but mixtures of FeO and Fe_3O_4 may melt as low as 1200° C. FeO decomposes to Fe and Fe_3O_4 below 570° C. The liquid slag formed on melting iron in air often is called FeO, although the slag may have a wide range of oxygen contents (22 to 27% O_2 at 1600° C.). Iron dissolves up to 0.94% FeO (=0.21% O_2), which lowers the melting point 10° C., according to C. H. Herty, Jr. (*Metals Handbook*, 1936 Edition, 277–278). At 1950° C. the solubility of oxygen in iron increases to 0.8% O_2, any additional oxygen appearing as the liquid called FeO. To what extent oxygen dissolves in solid iron is not known accurately. It appears that 0.1% O_2 is soluble at 800° C., but much less at room temperature.

The oxygen content of industrial steels ranges from about 0.004 to 0.10% according to the method of manufacture and particularly the carbon content. All indications are that the N_2 and O_2 content of arc furnace steel is related to NO formed in the arc as well as to agitation of the bath and to the ingress of air. The oxygen content is usually higher the lower the carbon, silicon and manganese contents. Although oxygen is usually removed from steel by an element such as aluminum, having a greater affinity for oxygen than iron, the FeO may also be removed by the Stokes principle, centrifuging, or diffusion to a slag of low FeO content. The Stokes principle always acts during the solidification of the ingot; the slower the rate of solidification the more complete is the removal of FeO from the ingot.

Only recently have reliable quantitative methods been developed for determining the form in which oxygen occurs in steel. The total oxygen content of a steel may have little relation to the proportion of oxygen atoms dissolved or occupying positions in the lattice of the steel. For example, aluminum and silicon may hold most of the oxygen in combined form as alumina or silicates. As dissolved oxygen, FeO, or Fe_3O_4, oxygen has no effect on the critical points, does not cause hot shortness, except indirectly in rendering the manganese unavailable for MnS formation, and decreases the notch impact value, especially at low temperatures. As oxide inclusions, oxygen probably has an effect in decreasing the time for initiating the decomposition of austenite on cooling and in increasing the range of temperatures to which the steel may be heated without serious grain growth. For reasons not completely elucidated, oxygen or oxides are a cause of abnormality in the McQuaid-Ehn test. Strain aging (S. Epstein and H. L. Miller, *Metals Handbook*, 1936 Edition, 471–478) of low-carbon steel appears to be attributable to precipitation of oxygen as a compound from solid solution at room temperature following cold work. Oxygen decarburizes the surface of red hot steel.

The oxygen problem in steel refining and steel treatment even now is not completely solved. In welding, the oxygen question is in the same plight. There is information on the oxygen content of welds, but the form in which the oxygen occurs has not been clearly resolved for all types of welds and filler metals. Again, high oxygen content in weld metal is known to be associated with poor physical properties, yet other impurities, such as nitrogen, have obscured the precise part played by oxygen. These topics are discussed in subsequent sections of the review together with the available information on the effects of oxygen in the weld puddle. Welding slags, although they are based on oxides, are not discussed in the present review because the oxygen radical is less important than the metal radical. Several welding phenomena, such as boiling, are discussed in the review but the viewpoint has been primarily the effect of oxygen. Other factors than oxygen that may influence complicated actions, such as spatter, have not been fully discussed.

OXYGEN PICK UP IN WELDING

The oxygen content of different types of welds mainly in mild steel in air is shown in Table 1. The total oxygen only is reported (see section on Oxides in Welds for Fractional Analyses). The analyses were performed by hot vacuum extraction. Deposits from bare electrodes contained 0.18 to 0.95% O_2, average 0.2%. Hodge[3] stated that bare electrode weld metal is supersaturated with oxygen. As low as 0.035% O_2 has been reported for deposits from covered electrodes. On the average, oxyacetylene welds contain about 0.05% O_2, but as low as 0.007% has been found.

Despite the statement by Doan and Schulte[4] that covered electrodes deposited on iron pick up 0.15% O_2, the welding atmosphere, including electrode coverings, appears to be the most important factor influencing oxygen pick up. The oxygen pick up was reduced 50% by increasing the thickness of coating in Zeyen's tests. Restricting the access of air by using small scarf angles and relatively few layers reduced the oxygen pick up by as much as 30%. With bare electrodes, according to Csilléry and Péter,[14] a long arc increases the oxide content of the weld.

The effect of diameter of electrode on oxygen pick up is shown in Tables 2 and 3 by Losana.[15] The oxygen was determined by hydrogen reduction at 1000° C. The welding conditions were maintained constant for a given

Table 1—Oxygen Pick Up in Welding

References	Weight % Oxygen	Welding Conditions
		BARE ELECTRODES
Zeyen,[2] 1938	0.210	Electrode negative, d.c., deposit had 0.03 C, 0.20 Mn, 0.02 Si.
Métallurgie,[3] 1936	0.3	
Doan and Schulte,[4] 1936	0.265	High purity iron electrode in air.
	0.276	Steel electrode in air.
Larson,[5] 1936	0.227	
Reeve,[6] 1935	0.298	Deposit had 0.02 C, 0.09 Mn, 0.014 Si; electrode had 0.10 C, 0.56 Mn, trace Si.
Kleinefenn,[7] 1933	0.2370	D.c., deposit had 0.01 C, 0.01 Si, 0.00 Mn; electrode had 0.02 C, 0.01 Si, 0.04 Mn.
	0.2100	D.c., deposit had 0.04 C, 0.01 Si, 0.12 Mn; electrode had 0.08 C, trace Si, 0.47 Mn.
	0.1780	D.c., deposit had 0.04 C, 0.01 Si, 0.12 Mn, 1.98 Ni; electrode had 0.05 C, 0.02 Si, 0.46 Mn, 2.30 Ni.
Hensel and Larsen,[8] 1932	0.785	Deposit contained slag, electrode had 0.02 C.
	0.183	Deposit had 0.028 C, 0.003 Si, 0.27 Mn, 0.003 Al; electrode had 0.29 C. (175 amps.).
	0.259	Deposit had 0.027 C, 0.004 Si, 0.006 Mn, 0.006 Al; electrode had 0.02 C. (175 amps.).
A. R. E. E.,[9] 1926	0.25 to 0.55	Deposit had 0.05 C, 0.18 Mn, 0.011 Si, electrode had 0.16 C, 0.56 Mn, 0.016 Si.
		COVERED ELECTRODES
Zeyen,[2] 1938	0.180	Lightly covered, electrode negative, d.c.; deposit had 0.04 C, 0.02 Si, 0.20 Mn.
	0.140	Medium heavy covered; electrode positive, d.c.; deposit had 0.04 C, 0.04 Si, 0.25 Mn.
	0.099	Heavy covered electrode, shielded arc, electrode positive, d.c.; deposit had 0.06 C, 0.07 Si, 0.36 Mn.
Hackert and Zeyen,[10] 1937	0.083	Cored alloy electrode, 10 layers, 90° V, 0.79-inch plate, 0.16-inch electrodes.
	0.060	Ditto but 3 layers, 60° V, 0.47-inch plate.
	0.045 to 0.080	Covered electrodes, 25 layers, 90° V, 0.79-inch plate, 0.16-inch electrodes.
	0.035 to 0.080	Ditto but 5 to 6 layers, 60° V, 0.47-inch plate.
Woirin,[11] 1937	0.036	Average at Penhoet, France, for boiler welds.
Larson,[5] 1936	0.0448	
Doan and Schulte,[4] 1936	0.265	Covered high-purity iron electrode in air.
	0.152	Covered high-purity iron electrode in argon.
Métallurgie,[3] 1936	0.07	Good grade of coated electrodes.
Reeve,[6] 1935	0.257	Ferrous silicate covered electrode; deposit had 0.038 C, 0.09 Mn, 0.04 Si.
	0.162	Ditto with additional deoxidizer, deposit had 0.07 C, 0.28 Mn, 0.06 Si.
	0.052	Gas-shielded type of electrode; deposit had 0.07 C, 0.54 Mn, 0.25 Si.
Hoyt,[12] 1935	0.06	Cellulose covered electrode.
Kleinefenn,[7] 1933		C Mn Si
		Coated electrode 0.02 0.04 0.01
	0.1140	Deposit, d.c. 0.04 0.67 0.03
	0.1240	Deposit, a.c. 0.04 0.62 0.02
		Coated electrode 0.08 0.47 trace
	0.1020	Deposit, d.c. 0.11 0.72 0.03
	0.1170	Deposit, a.c. 0.10 0.67 0.03
		Coated electrode 0.05 0.46 0.02 1.98 Ni
	0.0447	Deposit, d.c. 0.21 1.43 0.01 1.97 Ni 0.22 V
	0.0468	Deposit, a.c. 0.18 1.19 0.01 1.95 Ni 0.22 V
		OXYACETYLENE WELDS
Zeyen,[2] 1938	0.037	Deposit had 0.10 C, 0.02 Si, 0.49 Mn
Métallurgie,[3] 1936	0.07	
Kleinefenn,[7] 1933		C Si Mn
		Rod 0.02 0.01 0.04
	0.0507	Deposit 0.02 0.01 0.03
		Rod 0.10 0.02 0.35
	0.0507	Deposit 0.05 0.01 0.28
		Rod 0.12 0.01 0.25 2.00 Ni 0.2 Mo
	0.0591	Deposit 0.04 0.01 0.16 2.00 Ni 0.09 Mo
		Rod 0.15 0.03 0.35 3.50 Ni 0.75 Cr
	0.0239	Deposit 0.09 0.03 0.24 3.10 Ni 0.68 Cr
Hensel and Larsen,[13] 1932	0.054	Deposit had 0.033 C, 0.003 Si, 0.14 Mn, 0.006 Al.
		ATOMIC HYDROGEN WELDS
Hensel and Larsen,[13] 1932	0.053	Deposit had 0.033 C, 0.008 Si, 0.15 Mn, 0.009 Al, 0.26 W.

diameter of electrode, but it was not made clear to what extent the current was varied.

Table 2—Effect of Diameter of Bare Electrode on Oxygen Pick Up in Automatic D. C. Welding, Losana[15]

Composition of Electrode, %			Oxygen Pick Up, Weight %: Diameter of Electrode = 0.04 Inch	Diameter of Electrode = 0.08 Inch	Diameter of Electrode = 0.16 Inch	Diameter of Electrode = 0.24 Inch
C	Mn	Si				
0.08	0.32	0.06	0.953	0.805	0.512	0.293
0.09	0.73	0.18	0.720	0.550	0.302	0.105
0.38	0.46	0.21	0.748	0.522	0.321	0.203

The oxygen pick up was decreased 70% by increasing the diameter of the bare electrode from 0.04 to 0.24 inch. As the electrode diameter increases at constant arc length, the metal transported is less exposed to the air. A neutral coating decreased the oxygen pick up by 75%. Additions of Mn were slightly more effective than carbon in reducing oxygen pick up. Oxygen pick up was reduced to practically nil by a coating containing 10% Mn. The weight of the coating was 25% of the weight of the rod.

Manganese as an alloying element in the filler rod also tends to reduce oxygen pick up, according to Fry,[16] Table 4. The welds were single layer V butt in mild steel (0.11 C, 0.55 Mn, trace Si, 0.24 inch thick). The covered electrode used by Reeve, Table 1, contained 0.10

C, 0.56 Mn, trace Si, and there was no Mn in the coating. Reeve concluded that 0.56 Mn is insufficient to react with the FeO in the weld, if loss of Mn by volatilization and in the slag is taken into account. Kleinefenn's tests, Table 1, revealed no pronounced effect due to Mn up to 0.7%, but deposits with 2% Ni generally had low oxygen content. With covered electrodes the lowest oxygen content was found in the Ni-Mn-V deposit. Hoffmann[17] stated that as the carbon content of the filler rod was increased from 0.05 to 1.10% C, the oxygen pick up was decreased 50% in both gas and bare electrode welding. A similar effect was exerted by 1¾% Ni added to the filler rod. Unfortunately, he did not describe his experimental procedure.

The meager information available suggests that coatings and carbon, manganese, and nickel reduce oxygen pick up. Oxygen is picked up by weld metal as dissolved FeO and as oxides which have been entrapped in the rapidly solidifying weld metal. Although electrode tips or drops flicked from the electrode have not been analyzed for oxygen, presumably oxygen may attack the metal at the tip of the electrode, during transport of globules, and while the molten weld puddle is agitated by the arc force. Evidently the diameter of the electrode has a great effect. Coatings or fluxes protect the tip of the filler rod and form a slag that covers most of the weld puddle. Possibly there is a slag coating on each globule transported, which would prevent access of oxygen to the metal. As the globule touches the surface of the puddle the slag should peel off or separate readily to the slag covering on the puddle. Carbon in the coating (cellulose covered electrode, for example) or in the rod and weld metal reduces the FeO in the steel forming a gas which escapes from the weld. The carbon in the weld metal is the determining factor for FeO content, but carbon is probably ineffective in reducing other oxides such as Al_2O_3 or TiO_2. Manganese reduces the FeO content of steel by forming a MnO-FeO solid solution which has a negligible solubility in molten steel compared with FeO alone. The FeO-MnO solid solution must have time to rise from the bath in order that manganese may reduce the oxygen content of weld metal. Similar considerations doubtless apply to other deoxidizing agents. That nickel may reduce oxygen pick up has no ready explanation. At steel making temperatures nickel is not oxidized in the presence of iron. Nickel slightly widens the range of solidification, and may alter gas solubility relationships or characteristics of the arc atmosphere, or may form a layer of pure nickel on the globule.

As the speed of welding increased from 20 to 60 ft./hr., the oxygen pick up by a bare electrode deposit increased from 0.142 to 0.164%, according to Leitner.[18] The bare electrode was 0.16 inch diameter and the hot extraction method of analysis was used. The current was maintained constant. It was said that the number of globules per second passing through the arc space was doubled as the welding speed increased within the range studied. As a result a greater surface of metal was exposed to the atmosphere. As the current was increased in Okamoto's[19] tests on commercial covered electrodes, the oxygen pick up was increased (original report not available to reviewers). On the contrary, Paterson and Blair[20] stated that the oxygen content (no details) of any weld decreases rapidly as the current is increased, that is, as the rate of deposition is increased. Paterson and Blair believed that the lower the melting point of the electrode the greater is the opportunity for oxygen pick up. It would appear that the carbon content should have a more important influence than the melting point. If a bare mild steel electrode is cold worked, according to Kessner and Specht,[21] the oxygen pick up is not affected.

Table 4—Oxygen Pick Up as a Function of Manganese Content of Welding Rod. Fry[16]

Composition of Welding Rod, %			Oxyacetylene Weld	Bare Electrode Weld	Dipped Electrode Weld
C	Si	Mn			
0.04	0.05	0.05	0.016	0.180	0.078
0.08	0.01	0.52	0.016	0.120	0.070
0.04	0.01	0.72	0.017	0.078	0.066
0.08	0.12	1.62	0.007	0.095	0.065

WELDING IN OXYGEN

According to Holland,[22] dense clouds of brown Fe_2O_3 fumes are evolved when a bare mild steel electrode is arced in oxygen. It was impossible to do any welding because once the arc was ignited combustion occurred, which was prevented by smearing the electrode with sodium silicate. About twice as much slag was produced in oxygen as in air with bare or lightly fluxed electrodes. With covered electrodes the slag volume was the same in oxygen as in air. In Goodger's[23] experiment with arc welding in oxygen, two-thirds of the electrode was burnt to oxide. The deposited metal was said to contain inclusions of iron-iron oxide eutectic, which was said to be needle-shaped. Deposits made in oxygen with a mild steel electrode by Shamovsky and Prokorov[24] were distinguished by extremely low notch-impact value.

None of the experimenters determined the characteristics of the arc in oxygen. Doan[52] has shown that oxygen must be present in the atmosphere of an iron welding arc if the arc is to be stable and if penetration is to be secured. According to Suits,[53] oxygen forms an oxide film on the cathode from which electrons for the arc are liberated more easily than from bare iron. Newman,[54] however, could strike an arc between iron electrodes in impure hydrogen or air at low pressure (0.1 mm. mercury) only if the surfaces of the electrodes were clean. If the iron were coated with a film of iron oxide, the cathode fall was increased and the arc could not be struck. Films of fused salts did not affect the striking of the arc in air.

BRITTLENESS

Although the brittleness of arc welds made with bare or ineffectively coated electrodes often has been attributed to nitrogen pick up, Reeve[6] showed that oxygen pick up, as well, may account for low ductility, Table 5. The FeO rather than the nitrogen seemed to be the effective

Table 3—Effect of Diameter of Coated Electrode and of C and Mn in Coating on Oxygen Pick Up in Manual A. C. Welding. Losana[15]

Diameter of Electrode,* Inch	Neutral Coating	Oxygen Pick Up, Weight %												
		Weight % Carbon in Coating							Weight % Mn in Coating					
		1	2	4	5	6	8	10	2	4	6	10	20	30
0.08	0.190	0.120	0.062	0.034	0.030	0.028	0.030	0.027	0.070	0.042	...	0.009	0.005	0.006
0.16	0.150	0.071	...	0.031	...	...	0.020	0.017	0.058	0.030	0.018	0.006	0.006	0.005
0.24	0.121	0.06	...	0.024	...	...	0.014	0.008	...	...	...	...	...	...

* Rod contained 0.08 C, 0.32 Mn, 0.06 Si.

Table 5—**Average Mechanical Properties of All-Weld-Metal as a Function of Oxygen and Nitrogen Contents. Reeve[4]**

Electrode	Composition of Weld Metal, % C	Mn	Si	N_2	Fractional Gas Analysis, % O_2 as FeO	O_2 as MnO	O_2 as SiO_2	O_2 as Al_2O_3	Total O_2	Tensile Strength, Lb./In.^{2}	Elongation in $1^1/_2$ In. %	Izod Impact Value, Ft.-Lb.
Bare	0.02	0.09	0.014	0.181	0.282	...	trace	0.016	0.298	45,000–58,000	4–12	3–8
Covered with ferrous silicate	0.04	0.09	0.042	0.036	0.208	...	0.032	0.017	0.257	52,000–63,000	12–18	20–30
Ditto with additional deoxidizer	0.07	0.28	0.06	0.056	trace	0.063	0.088	0.011	0.162	58,000–70,000	20–25	30–40
Gas-shielded type	0.07	0.54	0.25	0.010	nil	0.006	0.036	0.01	0.052	63,000–72,000	25–35	40–60

agent in reducing ductility and Izod value. Reeve pointed out that only oxygen as iron oxide can be expected to produce age hardening.

In welds made with alcohol flowing down the electrode, according to Brophy,[25] the weld contained hydrogen and nitrogen (no details), yet the weld had high ductility. Brophy regarded oxide as the chief embrittling agent. Doan and Schulte[4] stated that oxygen is partly responsible for aging brittleness in arc weld metal. In Miller's[26] experience, oxygen was the most dangerous of all elements in reducing the mechanical properties of oxyacetylene welds in mild steel. Cracks in welded aircraft steel were vaguely attributed by Hoffmann[27] to high oxygen content as well as to segregations. If, however, the manganese content was 0.7 to 1.5% there were no cracks even if the combined sulphur and phosphorus content was 0.08% and the oxygen content higher than 0.008%. His experimental procedure was not adequately described. Stieler[28] found that 0.13 to 0.15% oxygen had no effect on cracking of fillet welds during welding, yet Hoyt[12] attributed shrinkage cracks in high-oxygen weld metal to finely dispersed FeO.

There is a belief that oxygen is responsible for the hot shortness of low-grade weld metal. Bending at 1050° C. revealed that bare electrode weld metal was hot short, according to Hoyt,[12] who considered FeO to be the cause of the hot shortness. Schuster[29] objected to the statement that 0.25% O_2 is permissible in weld metal on the grounds that certain commercially pure irons with a much lower oxygen content than 0.25% are red short. Reeve[4] also stated that the hot shortness of bare electrode weld metal is most probably caused by iron oxide.

SPATTER AND POROSITY

It is generally believed that spatter and porosity are expressions of gas-forming reactions in the globules or weld puddle. Depositing bare electrodes in an atmosphere containing not less than 6% oxygen, Alexander[30] observed that the outer portions of the metal in the arc crater bubble because oxidation is occurring. Presumably the bubbles are CO. The center is boiling and intermediate regions are quiet. The heat of oxidation permits the surface of the solidifying metal to remain fluid. Consequently, gas escapes and there is no porosity. Porous deposits were obtained with iron electrodes in a neutral gas, such as argon. In Boetcher's[31] opinion, porosity in metal arc welding is due to CO formed by reaction of air with carbon in the electrode.

Blow-holes in automatic 90° V butt welds in $^1/_4$-inch plate made with a $^3/_{16}$-inch bare mild steel electrode were bright and probably contained gas, J. S. Johnson[32] found. A mineral flux was used (no details). Chilling the plates with water or using copper, chromium-iron (18% Cr), or refractory backing strips had no effect on porosity, which was eliminated by removing mill scale from the plate. Johnson believed porosity was caused by a reaction between carbon and oxygen in the solidifying steel. On the other hand, Kessner and Specht[21] found that increasing cold work given to a bare electrode increased spatter and porosity, the explanation being connected with the decreased thermal and electric conductivity. The part played by oxygen was not mentioned. Abnormal steels as bare electrodes lose 11.5% by spatter, acccording to Feszczenko-Czopiwski,[33] whereas normal steels under identical circumstances lose only 5.25%. It was belived that the dissolved oxygen in abnormal steel increased the loss of C, Si and Mn by burning, as Pilarczyk[34] also pointed out. The burning of carbon would involve a gas-forming reaction.

Spatter in oxyacetylene welding was believed by Streb[35] to be caused sometimes by the reaction of carbon with oxygen dissolved in the steel to form CO. The chief cause of spatter was FeO which is easily reduced. The spattered particles contained 61% Fe_2O_3, 32% FeO, 7% Fe, no analysis apparently having been made for Fe_3O_4. Unquestionably, the particles were much richer in iron at the instant of projection. A high-carbon rod 0.08 inch diameter containing 0.76 C, according to Streb, may not spatter at all, whereas a low-carbon rod (0.02 C) may spatter greatly. Streb believed that the solubility for gases increased with temperature, the FeO content of the low-carbon metal being greater than that of the high-carbon metal.

Two hypotheses for spatter in oxyacetylene welding were discussed by Stursberg:[36] 1. FeO and Fe_3C react with explosive violence to form CO under the influence of the sudden temperature rise in the rod; 2. FeO in the rod is reduced by hydrogen in the flame to form an explosive mixture of hydrogen and oxygen. The first hypothesis is in agreement with Feszczenko-Czopiwski's[33] observation that the sparking and bubbling of metal from abnormal steel rods in oxyacetylene welding decreases with the extent to which the steel has been deoxidized in the furnace. Whether oxygen from the welding atmosphere is not equally effective as oxygen in the rod in affecting spatter and boiling was not discussed. Both the steam and the CO hypotheses were supported by Stursberg.

THE McQUAID-EHN TEST

The McQuaid-Ehn test has been successfully applied to many problems of steel treatment and it is not surprising that welding investigators often have used the test, as early as 1922, according to McQuaid (discussion of paper by Harder[37]). Oxygen in the steel is regarded as an important cause of abnormality in the McQuaid-Ehn test. In Hoyt's[12] investigation the bare electrode deposit was exceptionally abnormal; the deposit from the cellulose covered electrode was normal. Hoyt believed oxygen (FeO) was the cause of abnormality because, as the FeO content of Armco iron was increased from nil to 0.2%, the structure changed from normal to abnormal in the McQuaid-Ehn test. The tests made by Hodge[38] were similar to Hoyt's. Covered electrode, short arc weld metal was normal (950° C.) except for a few slightly abnormal areas.

Oxyacetylene and metal arc weld metal in Roux's[39] case hardening experiments had normal cases if Mn was at least 0.40% (up to 0.81%). Nitrides were found in the abnormal case of one weld. The standard McQuaid-Ehn test was not used; instead carburizing was done for 6 hr. at 925° C. in Houghton Liquid Salt. Roux recommended the test for qualitative analysis of the degree of oxidation of different parts of the weld. In some specimens the change from an abnormal to a normal area was abrupt. Normal specimens had good hardenability without soft spots.

Without supplying details, Harder[37] stated that deposits from bare electrodes containing nickel or a little molybdenum were completely abnormal. The deposit from an electrode containing titanium had no hypereutectoid zone in the McQuaid-Ehn test, and resembled a steel melted in an alundum crucible. Weber[40] found that deposits from bare electrodes in air, CO, CO_2 and nitrogen were abnormal, but deposits made in helium were normal in the McQuaid-Ehn Test. Weber believed nitrogen as well as oxygen accounted for abnormality but his experimental procedure was not suited to the maintenance of atmospheres free from water vapor, which probably was an unrecognized contaminant in all his tests, as Alexander[40] pointed out. The latter investigator found normal or only slightly abnormal structures in bare electrode deposits made in the following dehydrated gases: hydrogen, helium (traces of O_2 and N_2), carbon monoxide, nitrogen and a mixture of 60% argon, 40% nitrogen. Abnormal structures were observed in deposits made in carbon dioxide, a mixture of 99.5% argon, 0.5% oxygen, and a mixture of 89% argon, 11% nitrogen. Armco or low-carbon steel (0.16 C, 0.4 Mn, 0.02 Si) electrodes were used. It was concluded that oxygen was much more effective than nitrogen in promoting abnormal structure in the McQuaid-Ehn test. The composition of the weld deposits was not stated. Zieler[40] believed that good covered electrodes deposited weld metal having a normal structure. Excess of oxygen or acetylene appeared to have little effect on the abnormality of the oxyacetylene welds that were tested (no details).

Abnormal rods were said by Hoffmann[41] and a German welding firm[42] to yield unsatisfactory oxyacetylene welding results.

OXIDES IN WELDS

The total oxygen content of weld metal is discussed in the section on Oxygen Pick Up in Welding. Unless the form in which the oxygen is present is known, the total oxygen content loses much of its significance. The method of determining the amount of oxygen existing in different forms in steels is known as fractional oxygen analysis. In its entirety the method consists of comminuting the steel at room temperature to evolve gaseous oxides, followed by chemical, electrolytic, or hot vacuum extraction of oxygen from solid oxides and solid solution. Using the Reeve method of fractional oxygen analysis, Hoyt[12] found, Table 6, that FeO was the predominant oxide in deposits from bare and slag covered electrodes. The FeO is precipitated from solution during cooling as minute globules. The alumina content was not explained. The silica and alumina in the slag coated electrode deposit were believed to counteract the bad effects of FeO. The inclusions were a duplex iron silicate and

Table 6—Fractional Vacuum Fusion Oxygen Analysis of Arc-Weld-Metal. Hoyt[12]

	Bare Electrode	Slag Coated Electrode	Cellulose Covered Electrode
C	0.02	....	0.08
Si	0.01	....	0.23
Mn	0.09	....	0.55
N_2	0.17–0.19	....	0.006–0.010
H_2	0.0001–0.001	....	0.0008–0.0014
% O_2 as FeO	0.282	0.208	....
% O_2 as MnO	....	"very small"	0.010
% O_2 as SiO_2	0.0004	0.032	0.052
% O_2 as Al_2O_3	0.016	0.017	0.008
Total	0.298	0.257	0.070

the characteristics of the weld metal were closer to wrought iron than to oxidized, low-carbon steel. The oxygen in the deposit from the cellulose covered deposit was present as a dispersion of fine silica, the dispersion being created as a result of the solubility of SiO_2 to some extent in liquid weld metal and its insolubility in solid weld metal. Three types of oxides in weld metal were distinguished by Hoyt. Type 1. Large oxide and slag inclusions trapped in the metal during solidification. Type 2. Small, unimportant inclusions. Type 3. Uniform dispersion of minute particles. Reeve's[6] fractional analyses, Table 5, are similar to Hoyt's.

The possibility was suggested by Leitner[43] that silica may improve the notch impact value of arc weld metal deposited by different electrodes but containing the same amounts of C, Si, Mn, S, P and N_2. The nitrogen contents were 0.08% = both welds, yet weld *A* had 10.2 mkg./cm.^{2} DVMR, whereas weld *B* had only 4.0 mkg./cm.^{2}. Weld *A* contained 0.036% total oxygen and 0.073% SiO_2; weld *B* had 0.012% total oxygen and 0.0095% SiO_2. The microstructures were the same and in both welds the proportion of the surface occupied by inclusions was 0.01%. Leitner offered no explanation.

Using the vacuum fusion method for total oxygen and the Fitterer residue method for oxides, Hensel and Larsen[8] found, Table 7, that the residue method returned much higher oxide content for the coated than for the bare electrode deposits. No analysis was made for FeO, and it was believed that most of the MnS in the coated electrode deposit was $MnSiO_3$. Hensel and Larsen did not make clear whether or not the results of the Fitterer analysis were reported as weight per cent oxygen. The absence of silica in the bare electrode deposit was confirmed by Hoyt and Reeve. The principal oxide in the oxyacetylene weld was MnO; the principal oxide in the atomic hydrogen weld was Al_2O_3. The difference between the two deposits was not explained.

Table 7—Total Oxygen and Oxides in Unalloyed Steel Weld Metal. Hensel and Larsen[8]

	Weight % Oxygen by Vacuum Fusion	Fitterer Residue Analysis				
		SiO_2	Al_2O_3	MnO	MnS ($MnSiO_3$)	Total by Residue Method
Bare electrode	0.183–0.259	0.001	0.003	0.073	0.009	0.086
Coated electrode	0.064–0.133	0.096–0.128	0.011	0.015–0.018	0.084–0.091	0.296–0.247
Oxyacetylene	0.054	0.008	0.002	0.035	0.013	0.058
Atomic hydrogen	0.053	0.002	0.016	0.007	0.026	0.051
Unwelded Armco iron	0.08	0.001	0.003	0.012	0.014	0.030

Large oxide inclusions in bare electrode welds were identified as FeO by Moses.[44] Glassy silicates were found in welds made with heavy covered electrodes. X-ray diffraction patterns of bare electrode deposits revealed Fe_3O_4, according to Miller.[45] Doubtless the FeO referred to by all the other investigators is Fe_3O_4 at room temperature. Goodger's[23] arc welds made in oxygen contained needles which he believed were oxides but likely were nitride.

Inclusion counts of weld metal were not useful in Hoyt's[12] experience. On the other hand, Fuchs[44] used the percentage of a cross section occupied by inclusions as a measure of the quality of bare electrodes. The higher the inclusion content of a bare electrode, the better were the welding characteristics. Bare electrodes with 0.10 C, 0.3 Mn, trace Si with 0.186 to 0.245% of the cross section occupied by inclusions could not be welded with the electrode negative. An electrode with 0.02 C, 0.13 Mn, trace Si, and 0.835% of the cross section occupied by inclusions could be welded on the negative pole, and a bare electrode connected to the negative pole and containing 0.08 C, 0.09 Mn, trace Si, with 3.16% of the cross section occupied by inclusions produced high-grade welds. The inclusion percentages were averages of hundreds of micrographic determinations. According to Leitner,[43] the proportion of a metallographically polished surface occupied by inclusions in weld metal deposited by covered electrodes is 0.013 to 0.021%.

Inexpert welding which resulted in oxide inclusions at and near the surface of the welds caused rapid localized corrosion of the oxyacetylene welded mild steel chlorine tanks examined by Leroy.[47] Texter and Speller[48] stated that oxides in steel may be redistributed under the influence of welding heat to form planes of weakness (no details). Over-reduced steel is not suitable for filler rods, according to Swinden,[49] because the steel is sluggish. On the other hand, a German firm[42] recommended that oxyacetylene welding rods should not have inclusions high in FeO. Silicate inclusions were also believed to be disadvantageous.

FORGE WELDING

According to Schäfer,[50] the oxygen content of steel has little effect on forge welding characteristics (no details). Roeckner[51] stated that iron oxide slags penetrate along the grain boundaries of mild steel during forge welding. He is alone in this observation.

FLAME CUTTING

Oxygen in steel should not have much effect on flame cutting, although scale on the surface to be cut increases the preheating time (see review of literature on Flame Cutting).

ACKNOWLEDGMENTS

Contributions to this review have been received from:
L. W. Schuster, British Engine Boiler & Electrical Insurance Company, Manchester, England.
W. A. Spindler, University of Michigan.

BIBLIOGRAPHY

1. Hodge, J. C., American Welding Society Journal, **11** (10) 36–40 (1932).
2. Zeyen, K. L., *Stahlbau*, **11** (6) 41–46; (7/8) 59–64 (1938).
3. *Métallurgie*, **68**, No. 10 FP, 17–23 (1936).
4. Doan, G. E., and Schulte, W. C., American Welding Society Journal, **15** (9) Suppl., 5–11 (1936).
5. Larson, L. J., American Welding Society Journal, **15** (10) 14–20 (1936).
6. Reeve, L., Iron & Steel Inst. Welding Symposium, **II**, 517–529 (1935).
7. Kleinefenn, W., *Carnegie Schol. Mem.*, **22**, 97–133 (1933).
8. Hensel, F. R., and Larsen, E. I., American Welding Society Journal, **11** (4) 28–32 (1932).
9. *Proc. Assn. Rwy. Elec. Engrs.*, **17**, 124–131 (1926).
10. Hackert, R., and Zeyen, K. L., *Techn. Mitt. Krupp*, **5**, 22–31 (1937).
11. Woirin, M. R., *Bull. Tech. Bureau Veritas*, **19**, 14–18 (1937).
12. Hoyt, S. L., *Trans. Amer. Soc. Metals*, **23**, 61–84 (1935); see also Motok, G. T., *Trans. Amer. Soc. Metals*, **25**, 466–480 (1937); discussion by Scheil, M. A., 483–487.
13. Hensel, F. R., and Larsen, E. I., *Trans. Amer. Soc. Steel Treating*, **19**, 639–664 (1932).
14. Csillény, D.v., and Péter, L. v., *Elektroschwg.*, **7**, 46–53, 64–70 (1936).
15. Losana, L., *Metallurgia Italiana*, **26**, 391–403 (1934).
16. Fry, A., *Kruppsche Monatsh.*, **12**, 201–214 (1931).
17. Hoffmann, W., *Autog. Metallb.*, **20**, 18–27, 33–44 (1927); *Eidg. Mat. Prüf. Anst. Zürich*, Diskuss. Ber., **12**, 40–41 (1926).
18. Leitner, F., *Arch. Eisenhüttenwes.*, **7**, 311–314 (1933–34).
19. Okamoto, T., Nishimura, H., and Katayama, H., *Yosetsu-Kyokwai-Shi*, **7**, 403–405 (1937); *Metals & Alloys MA98R9*, Feb. 1938.
20. Paterson, J. H., and Blair, H., *Jnl. Soc. Chem. Ind.*, **38**, 328T–330T (1919).
21. Kessner, A., and Specht, H., *Arch. Eisenhüttenwes.*, **9**, 231–239 (1935).
22. Holland, R. A., *Mech. & Weld. Engr.*, **6**, 201–207 (1932).
23. Goodger, A. H., *Weld. Engr.*, **13** (2) 39–43 (1928).
24. Shamovsky, E. C., and Prokorov, A. A., *Autog. Delo*, **7**, (4) 15–18; (10) 8–11 (1936).
25. Brophy, G. R., American Welding Society Journal, **6** (9) 67–76 (1927).
26. Miller, S. W., *Trans. A. S. M. E.*, **43**, 199–209 (1921).
27. Hoffmann, W., *Z. V. D. I.*, **79**, 1145–1148, 1486 (1935).
28. Stieler, C., *Stahl u. Eisen*, **58** (13) 346–350, Mar. 31, 1938.
29. Schuster, L. W., Iron & Steel Inst. Welding Symposium, **II**, 640–643 (1935).
30. Alexander, P., American Welding Society Journal, **5**, (12) 11–14 (1926).
31. Boetcher, H. N., *Trans. A. S. M. E.*, **56**, 11–19 (1934).
32. Johnson, J. S., American Welding Society Journal, **14** (7) 10–14 (1935).
33. Feszczenko-Czopiwski, I., *Spawanie i ciecie Met.*, **7**, 70–74 (1934).
34. Pilarczyk, J., *Przeglad Mechaniczny*, **1**, 15–19 (1935).
35. Streb, E., *Autog. Metallb.*, **25**, 52–61 1932).
36. Stursberg, E., *Autog. Metallb.*, **30**, 353–361, 369–380 (1937).
37. Harder, O. E., Weber, L. J., and Jerabek, T. E., *Trans. Amer. Soc. Steel Treating*, **13**, 961–982 (1928).
38. Hodge, J. C., Iron & Steel Inst. Welding Symposium, **II**, 391–417 (1935).
39. Roux, A., *Proc. Twelfth Intnl. Acet. Cong.*, 1177–1182 (1936).
40. Weber, L. J., *Trans. Amer. Soc. Steel Treating*, **11**, 425–440 (1927); Discussion by Alexander, P. P., *ibid.*, 441–446; see also Zieler, W., *Stahl u. Eisen*, **52**, 236–239 (1932).
41. Hoffmann, W., *Elektroschwg.*, **1**, 165–168 (1930).
42. *Autog. Metallb.*, **22**, 243–255, 265–270 (1929).
43. Leitner, F., *Z. V. D. I.*, **80**, 851–856 (1936).
44. Moses, A. J., American Welding Society Journal, **14** (4) 5–19 (1935).
45. Miller, J. W., American Welding Society Journal, **15** (2) 5–10 (1936).
46. Fuchs, J., *Elektrotech. u. Maschinenbau*, **46**, 527–534 (1928).
47. Leroy, A., *Proc. Twelfth Intnl. Acet. Cong.*, 600–605 1936).
48. Texter, C. R., and Speller, F. N., American Welding Society Journal, **10** (6) 21–24 (1931).
49. Swinden, T., *Welding Industry*, **4**, 427–435 (1936).
50. Schäfer, R., *Die Konstruktionsstähle*, Springer, Berlin, 125–129 (1923).
51. Roeckner, M., *Stahl u. Eisen als Werkstoff*, **II**, 75–79 (1928).
52. Doan, G. E., and Young, S., American Welding Society Journal, **17** (10) Suppl., 61–67, Oct. 1938.
53. Suits, C. G., American Welding Society Journal, **17** (10) Suppl., 35–38, Oct. 1938.
54. Newman, F. H., *Phil. Mag.*, Ser. 7, **3**, 796–799 (1926); 26 (173) 187–188 (1938).

SUGGESTED RESEARCH PROBLEMS

There are a good many statements in the review, made in good faith by the investigator in each instance, that nonetheless will not convince critical readers. Yet it hardly seems worth while to list these controversial subjects as suitable problems for research in view of the need for new departures in the study of the effects of oxygen on welding. Consequently, only a few of many obvious problems, albeit difficult of solution, are listed. In any research on oxygen in weld metal it is important to keep in mind the caution issued by one investigator who states, "Too much research is based on the fallacy that weld metal is necessarily homogeneous. Too little attention is paid to the varying content of constituents due to the difference in conditions in laying the beads of weld metal, the difference in grain formation at the bottom and the top of the weld, and the difference in thermal conditions at the beginning and the end of a weld. It may be necessary to remelt weld metal in a crucible to secure a uniform distribution of constituents, at the risk, however, of altering initial conditions in the weld metal."

1. Fractional oxygen analyses of weld metal as a function of speed and technique of welding and of the composition of welding slag and coating or flux are required.

2. What is the oxygen content of the solidified globule at the end of a covered electrode when welding has been interrupted? Has the globule a higher or lower oxygen content than weld metal deposited by the electrode? The spattered globules might also be analyzed for oxygen. In this way the origin of oxygen pick up might be found and some conception of the efficiency of the slag in fluxing the oxides might be gained.

3. The cause of shrinkage cracks in weld metal is said by some investigators to be iron oxide; by others oxygen is not considered to have any effect. Are the shrinkage cracks related to hot shortness created by oxygen? High-temperature (500–1300° C.) tensile or bend tests of weld metal containing different amounts of oxygen combined in different forms might provide the solution of the problem.

4. Although oxygen is a major constituent of the gas found in weld metal, it is not clear whether high oxygen content increases or decreases the likelihood of porosity and shrinkage cavities. In this connection it has not been demonstrated whether the rimming reaction is a factor in weld metals, and if so, to what extent.

THE EFFECT OF ALUMINUM ON THE Welding of Steel

A Review of the Literature to July 1, 1937

By W. SPRARAGEN* and G. E. CLAUSSEN†

This report is prepared under the auspices of

THE LITERATURE DIVISION OF THE ENGINEERING FOUNDATION—WELDING RESEARCH COMMITTEE

TABLE OF CONTENTS

	Page
Summary	8
Introduction	9
Welding Carbon-Aluminum Steels	9
Filler Rods	9
Aluminum Killed Steel	9
High Iron-Aluminum Alloys	10
Forge Welding	10
Resistance Welding	10
Flame Cutting	10
Welding Nitralloy	10
Welding Cr-Al Heat Resisting Steels	11
Bibliography	11
Suggested Research Problems	11

Summary

THE EFFECT OF ALUMINUM ON THE WELDING OF STEEL

ALTHOUGH there are aluminum steels in which the aluminum is added as an alloying element to improve nitriding, electrical, or scale-resisting characteristics, the major importance of aluminum from the welding standpoint is its rôle as deoxidizer and grain size controller. If aluminum or, more likely, alumina is present, the steel may resist austenite grain growth at higher temperatures than if none is present.

WELDING CARBON-ALUMINUM STEELS

Filler Rods

Aluminum has been used as a deoxidizer in the form of a thin wire in the coatings of electrodes, one experimenter objecting to the use of aluminum as deoxidizer in coatings because the reaction was too violent.

A rod for "activated" oxyacetylene welding recommended by one author contains 0.27 C, 0.80 Mn, 0.25 Si, 0.20 Al. The weld metal contained:

0.20 C, 0.66 Mn, 0.11 Si, 0.06 Al—neutral flame.

British Standard Specification No. 640—1935 recommends that bare rod or wire electrodes for metal arc welding wrought iron and steel contain not over 0.02 Al.

Aluminum-Killed Steel

Whether or not the steel has been killed with aluminum has little effect on the properties of the weld.

High Iron-Aluminum Alloys

If calorized mild steel is welded with an 18–8 or 25–12 filler rod, the ductility is excellent. There is difficulty with wrought iron or mild steel rods because the slag is viscous and the weld is brittle. Calorized steels were easy to cut with the torch if the calorized layer was relatively thin. Steel with 10–15% Al could not be flame cut even if both oxygen and steel were preheated.

WELDING NITRALLOY

Nitralloy is used for nitrited parts and contains 0.15–0.35 C, 1 Al, 1.5 Cr, 0.2 Mo. There has been difficulty in welding these steels so that the weld can be nitrided.

WELDING CR-AL HEAT RESISTING STEELS

There was no trouble in oxyacetylene or arc welding these steels to each other or to plain carbon and low-alloy steels.

* Secretary, Welding Research Committee.
† Research Assistant, Welding Research Committee.

The Effect of Aluminum on the Welding of Steel

INTRODUCTION

ALTHOUGH there are aluminum steels in which the aluminum is added as an alloying element to improve nitriding, electrical, or scale-resisting characteristics, the major importance of aluminum from the welding standpoint is its rôle as deoxidizer and grain size controller. Thermit welding is based on the energetic deoxidation of iron by aluminum. Although there is little, if any, loss of aluminum by vaporization from molten steel (boiling point of aluminum = 1800° C. at 760 mm. mercury), aluminum reduces small quantities of iron oxide dissolved in molten steel. At 1577° C. an increase in aluminum content of the steel from 0.001 to 0.01% reduces the solubility of FeO from 0.004 to 0.001%. The solid product of the reaction is alumina (Al_2O_3, melting point = 2000–2050° C.). Alumina is a neutral constituent of slag unless the alumina is combined with quantities of basic or acid constituents. Among the compounds of alumina with other slag constituents are:

$MgO \cdot Al_2O_3$	(melting point = 2130° C.)
$(CaO)_5 \cdot (Al_2O_3)_3$	(melting point = 1450° C.)
$(SiO_2)_2 \cdot (Al_2O_3)_3$	(decomposes at 1810° C.)
$(CaO)_3 \cdot Al_2O_3 \cdot SiO_2$	(decomposes at 1335° C.)
$CaO \cdot Al_2O_3 \cdot (SiO_2)_2$	(melting point = 1550° C.)
$(MgO)_2 \cdot (Al_2O_3)_2 \cdot (SiO_2)_5$	(decomposes at about 1400° C.)
$FeO \cdot (Al_2O_3)_2$	(melting point = 1610° C.)

For purposes of deoxidation or grain size control, aluminum is generally added in the ladle or mold and appears in the product as finely dispersed alumina, with perhaps some aluminum in solid solution in the steel. It is difficult to distinguish accurately between aluminum and alumina in chemical analysis. Consequently, when the aluminum content is below a fraction of 1%, analyses for aluminum, unless performed by special methods, include both the aluminum in solid solution and the aluminum combined as oxide. So far as the analyst is aware, the aluminum may be in either form. If aluminum or, more likely, alumina is present, the steel may resist austenite grain growth at higher temperatures than if none is present. Aluminum added to the mold more often appears as uncombined alumina in the steel than when it is added in the furnace or ladle. The inoculating effect of alumina or some action of aluminum—the matter is not yet clearly explained—reduces the depth of hardening of aluminum-treated steels. Although bauxite is added to steelmaking slags to increase the fluidity, aluminum steels, say 1% Al, have bad casting characteristics because the fluidity of the metal is decreased by a skin of alumina when the molten metal is exposed to the air

Aluminum carbide is never found in steel, aluminum being a graphite-forming element. Aluminum nitride (AlN) is more stable than iron nitride, and is decomposed only at about 2000° C. under nitrogen at ordinary pressure. To AlN is attributed the extraordinary hardness of several nitriding steels, and the denitrifying action of aluminum in the steel furnace. Aluminum appears to have no effect on hydrogen or sulphur, Al_2S_3 being less stable than FeS. The Ac_1 and Ar_1 points are raised by aluminum to equal extents.

In the almost complete absence of tonnage grades of aluminum alloy steel, the most important aspect of aluminum is deoxidation, which is discussed first in the present review. What little is known of the welding of aluminum steels is summarized in the concluding sections.

WELDING CARBON-ALUMINUM STEELS

Filler Rods

Iron-aluminum alloys, on being deposited as electrodes by Hess,[1] who wished to obtain blocks of weld metal, formed quantities of Al_2O_3, the heat of reaction of which maintained the deposited metal in the fluid condition so that subsequent deposited metal flowed to both sides. It was difficult to obtain a sample with 0.25% Al. A little less difficulty was experienced with atomic hydrogen welding but deposits with over 0.50% Al could not be prepared. Okada[2] found that deposits from an aluminum-coated mild steel electrode had low hysteresis loss but were extremely brittle.

In 1917 Armstrong[3] referred to electrodes which had been on the market for four years, in the coating of which there was a thin aluminum wire for deoxidation. Blomberg[4] objected to the use of aluminum as deoxidizer in coatings because the reaction was too violent. Aluminum formed a tough skin, probably alumina, on the weld puddle in Ruder's[5] experiments.

A rod for "activated" oxyacetylene welding recommended by Keel[6] contains 0.27 C, 0.80 Mn, 0.25 Si, 0.20 Al. The weld metal contains:

0.20 C, 0.66 Mn, 0.11 Si, 0.06 Al—neutral flame
0.14 C, 0.50 Mn, 0.07 Si, 0.075 Al—oxidizing flame (10% excess O_2)

The greater recovery of aluminum with the oxidizing flame than with the neutral flame may be related to the increased speed of welding with the oxidizing flame. Perhaps a part of the aluminum reported as metal was in the form of oxide in the deposit. Keel stated that the aluminum greatly improved the recovery of manganese. The tensile strength of the deposit was 88,000 lb./in^2. Welding with the aluminum steel rod (10% excess oxygen in flame) was 25% faster than with the customary mild steel rod and neutral flame. Good oxyacetylene welds could not be obtained by Diegel[7] with a mild steel rod containing 0.77 Al because an oxide film coated each drop. As a result, the deposits were brittle at elevated temperatures, although the metal had adequate fluidity. Diegel made no tests on rods with intermediate aluminum contents. A patented rod containing 0.20–0.40 C, 0.55–0.80 Mn, 0.01–1.0 Al was mentioned by Lurie.[8] British Standard Specification No. 640—1935 recommends that bare electrodes for metal arc welding wrought iron and steel contain not over 0.02 Al.

Aluminum-Killed Steel

Whether or not the steel has been killed with aluminum has little effect on the properties of the weld, as Hopkins and Blumberg[9] demonstrated. They used covered electrodes to butt weld 1-inch plates hot rolled from 100 to 123-ton heats. The plates contained 0.17–0.30 C, 0.38–0.66 Mn, trace to 0.26 Si, 0.015–0.15 Al, 0.007–0.045 Al_2O_3 and were silicon killed with or without small or large additions of aluminum, or semi-killed, or rimmed. Boiler Code tests, including Charpy tests down to −100° C. showed that the properties of the welds were governed entirely by the electrode. In multilayer welds the heat-affected zone always had finer grain size than base metal, and ample Charpy value at low temperatures. It was found that in rare instances weld metal passed every test including X-ray, but failed conspicuously in bend ductility. The explanation was

believed to lie in Al_2O_3 inclusions, which were transferred from base metal to the weld deposit. Motok[36] found that a bad welding rod contained 0.044 wt. % O_2 as Al_2O_3 compared with 0.015 O_2 in a good rod, but he offered no explanation. Welds in aluminum-treated S. A. E. X1020 tested by Kinzel, Crafts and Egan[10] had good Izod properties (above 10 ft.-lb.) down to −78° C. Heat-affected zone as well as weld metal was tested. At −120° C. the Izod value was often below 10 ft.-lb. The plates were $^1/_2$ to $1^1/_2$ inches thick, the McQuaid-Ehn grain size of unwelded base metal was 5 to 8, and mineral coated (Ni-Mo) or combustible coated electrodes were used.

Aluminum-killed steels were said by Houdremont and Schrader[11] to have low sensitivity to welding cracks because these steels are shallow hardening. Much more experimental evidence must be gathered before their statement can be accepted, especially in view of Bardenheuer and Bottenberg's[12] results with Cr-Mo aircraft steels in which aluminum increased cracking (for details see review of literature on Welding Chromium Steels).

According to a welding handbook[13], not over 2 oz. Al per ton should be added to steel for good weldability unless the steel has been semi-killed with silicon. "If silicon is added to the proper extent (no details) prior to the aluminum addition, the non-metallic inclusions coming out of the steel during welding will have sufficiently low melting point to be self-fluxing and will not form slag pockets along the edge of the weld." In the absence of more exact information about the inclusions the statement has limited application. It was observed that aluminum has a beneficial effect in reducing porosity in silicon killed welds, no details being given about how the silicon and aluminum were introduced into the weld.

An endeavor was made by Stine[14] to show that aluminum is 10 times more effective as a deoxidizer in welding than silicon, and that arc welding difficulties are experienced if the aluminum content of the steel exceeds 0.01% at 0.55 C, and 0.001% at 0.10 C. Stine believed that aluminum increases the solubility of gases in steel, for which there appears to be no evidence, and also considered that porosity, curiously enough, consisted of holes originally occupied by solid particles of slag of high melting point and high alumina content. In some way the slag disappeared after the weld metal had solidified around it, and left a hole instead. Stine invariably found porosity in automatic shielded carbon arc welds made without filler rod but with fibrous "autogenizer" fed through the arc, if the steel had been aluminum killed and contained appreciable quantities (no details) of Al_2O_3. In mild steels with up to 0.18 C, 0.005 and 0.014 Al did not produce porosity but 0.116 Al did. Since chromite inclusions, which have the same melting point as Al_2O_3, also produced porosity, Stine was led to formulate the slag hypothesis for porosity.

In discussion of Stine's results, Llewellyn[15] stated that steel killed with 11.6 oz. Al per ton behaved satisfactorily in carbon arc welding and pointed out that the deoxidizing characteristics of an element depend closely on the other elements present. Llewellyn and Stine himself emphasized the importance of electrode coatings in modifying any effect created by aluminum treatment. For example, porosity obtained in carbon arc welds in steel killed with an excessively large quantity of aluminum (53.5 oz. per ton) was corrected entirely by a sodium silicate layer on the surface.

Although Diegel[7] in 1922 found that up to at least 0.21% Al in base metal had no effect on the oxyacetylene welding of mild steel, Lewenz[16] believed that 0.01% Al or more may spoil the welding qualities of a steel. Diegel supported his statement with physical tests; Lewenz did not. The difficulty of dealing effectively with the small percentages of aluminum that are suspected of having baleful effects on welding probably accounts for the fantastic statements that have been made about the element. For instance, a railroad group's report on arc welding[17] recommended that the aluminid content of medium low-carbon welding rod shall not exceed 0.04%. The Chairman of the group admitted that aluminid was a strange term to him.

High Iron-Aluminum Alloys

If calorized mild steel is welded with an 18–8 or 25–12 filler rod, according to Holler,[18] the ductility is excellent. There is difficulty with wrought iron or mild steel rods because the slag is viscous and the weld is brittle. Calorized 1.5% Mo steel, according to Clark and Brown,[19] may be seal welded on the calorized surface by using a special coated rod containing 25 Cr, 12 Ni.

Iron containing 15% Al was stated by Weinman and Langmuir[20] to be successfully deposited by the atomic hydrogen process as a final layer on plain carbon steel weld metal. Carbon arc welding (0.24-inch carbon electrode, 250 amps. d. c.) was used by Lapidus[21] to weld magnet steel (25 Ni, 15 Al, 60 Fe) to low-carbon iron. The magnet steel was preheated to 650° C. to avoid cracks.

Forge Welding

In the smith's forge, Hahn[22] was able to obtain welds in steels containing 0.25 to 3.19 Al. With more than 1.40 Al the welds were brittle. The welds were made in forged bars 0.71–0.79 inch diameter, which contained 0.1 C, 0.15 Cu, 0.04 S, 0.04 P, approximately. Burgess and Aston[23] were unable to forge weld iron containing 2.3% Al. Jüptner[24] and Mars[25] both refer to early tests made by Hadfield on steels with 0.2 C, 0.1 Mn, 0.1 Si, which were rendered unweldable in the forge, if the aluminum content was 0.61% or over.

The killing of steel containing 0.15 C, 0.1 Si, 0.6 Mn, 0.02 P, 0.02 S with aluminum had no effect on water gas welding in Diegel's[26] tests.

Resistance Welding

No recent information is available. In 1889 Bramwell[27] had no trouble resistance butt welding ferro-aluminum of unspecified composition. Burgess and Aston[23] were able to resistance butt weld iron containing 2 to 2.3% Al, but had no success when the aluminum content reached 2.5%. On the other hand, Fry[28] had no difficulty in 1924 spot welding low-carbon steel containing 15% Al.

Flame Cutting

Wiss[29] was unsuccessful in oxyacetylene flame cutting mild steel in the core of which aluminum (several per cent) had segregated. On the other hand, calorized steels were easy to cut if the calorized layer was relatively thin (no details). Steel with 10–15% Al could not be flame cut even if both oxygen and steel were preheated. Pure aluminum could not be cut with an oxygen torch.

In an investigation of the bend ductility of machine oxygen-cut steels $^1/_2$, 1 and 2 inches thick containing 0.16–0.23 C, 0.36–0.57 Mn, Zimmerman[37] found that what little difference there was among killed, semi-killed, and rimmed steels was in favor of the rimmed steels. The slight differences were not explained nor were the alumina or aluminum contents determined.

WELDING NITRALLOY

Nitralloy is used for nitrided parts and contains 0.15–0.35 C, 1 Al, 1.5 Cr, 0.2 Mo. There has been diffi-

culty in welding these steels so that the weld can be nitrided. According to Hatfield,[30] welds with the requisite aluminum content can be made by the oxyacetylene and atomic hydrogen processes (no details), but not by the arc processes. In the atomic hydrogen process, Catlett[31] states, a rod of the same composition as base metal is used for building up the surface of Nitralloy parts which have been incorrectly machined. After the deposit has been heat-treated, machined and nitrided, the depth of nitriding is less deep in the weld metal than in unsurfaced base metal, but the hardness is the same. Catlett's weld metal had a pronounced dendritic structure, and the grain structure was coarse in the heat-affected zone. The results obtained by Lurie[8] in welding $^1/_4$-inch low-carbon steel plates by the oxy-illuminating gas process using Nitralloy rods of three types were not encouraging. The machined welds had only 57 to 89% of the tensile strength of unwelded base metal. The best specimen had 9% elongation in $1^1/_2$ inches.

WELDING CR-AL HEAT RESISTING STEELS

Some results on the oxyacetylene welding of steels for superheater tubes containing 6 Cr, $1^1/_2$ Al were discussed in the review of literature on Welding Chromium Steels (THE WELDING JOURNAL, **17** (7) 9, July 1938), which showed that satisfactory results were feasible. Schiffler[32] agreed that there was no trouble in oxyacetylene or arc welding these steels to each other or to plain carbon and low-alloy steels. A small, neutral flame should be used for gas welding, and the inner cone should not touch rod or puddle. According to Schimpke and Horn,[33] steels with 6 to 22% Cr, 0.5 to 4% Al (Sicromal) should be filed before arc welding to remove the oxidized surface, which has a high electric resistance. An acid slag with high FeO content was recommended as coating for the electrode, which should have the same composition as base metal. Martin[34] recommended special rods (no details) for the Sicromal steels which, he declared, had excellent welding characteristics. Lent[35] reported a yield strength of 50,000 lb./in.^{2}, tensile strength = 79,000 lb./in.^{2}, 75% reduction of area for oxyacetylene welded Sicromal (no details) 0.79 inch thick. The VGB notch-impact value of the heat-affected zone was 13 to 18 mkg./cm^2. At 530° C. the welded joints had a yield strength of 27,000 to 40,000 lb./in.^{2}, short-time tensile strength = 50,000 lb./in.^{2}.

BIBLIOGRAPHY

1. Hess, W. F., AMERICAN WELDING SOCIETY JOURNAL, **12** (10), 21–24 (1933).
2. Okada, M., *Tetsu-to-Hagane*, **20**, 535–547 (1934).
3. Armstrong, P. A. E., *Weld. Engr.*, **2** (1) 42–45 (1917).
4. Blomberg, H., *Elektroschwg.*, **7**, 121–128 (1936).
5. Ruder, W. E., *Gen. Elec. Review*, **21**, 941–946 (1918).
6. Keel, C. F., *Z. Schweisstech.*, **27**, 168–176, 208–213, 235–241 (1937).
7. Diegel, C., *Forscharb. Geb. Ingwes.*, **246**, 44 pages (1922).
8. Lurie, H. H., *Purdue Univ. Research Bull.*, **41**, Engg. Expt. Station, 114 pages, July 1932.
9. Hopkins, R. K., and Blumberg, H. S., *Natl. Petrol. News*, **28**, 56–63, Nov. 4, 1936.
10. Kinzel, A. B., Crafts, W., and Egan, J. J., *Trans. A. I. M. E.*, **125**, 569–583 (1937).
11. Houdremont, E., and Schrader, H., *Stahl u. Eisen*, **56**, 1416 (1936).
12. Bardenheuer, P., and Bottenberg, W., *Arch. Eisenhüttenwes.*, **11** (8) 375–383, Feb. 1938.
13. *Procedure Handbook of Arc Welding*, Lincoln Electric Co., Cleveland, Fourth Edn. (1936).
14. Stine, W. E., AMERICAN WELDING SOCIETY JOURNAL, **10** (9) 22–26 (1931); **11** (3) 18–20 (1932).
15. Llewellyn, F. T., AMERICAN WELDING SOCIETY JOURNAL, **10** (12) 30–34 (1931); **11** (3) 20 (1932).
16. Lewenz, H. I., *Electric Arc Welding Practice*, Crosby Lockwood & Son, Ltd., London (1936).
17. *A. E. R. A. Engg. Assn. Proc.*, 815–817 (1931).
18. Holler, H., *Autog. Metallb.*, **23**, 182–189 (1929).
19. Clark, C. L., and Brown, R. S., *Trans. A. S. M. E.*, **59**, 541–554 (1937).
20. Weinman, R. A., and Langmuir, I., AMERICAN WELDING SOCIETY JOURNAL, **5** (3) 32–46, (10) 69–76 (1926).
21. Lapidus, V. A., *Avtog. Delo*, **8** (5–6) 33 (1937).
22. Hahn, P., *Stahl u. Eisen*, **45**, 7–9 (1925).
23. Burgess, C. F., and Aston, J., *Electrochem. & Met. Engg.*, **7**, 436–438 (1909).
24. Jüptner, H. v., *Grundzüge der Siderologie*, Leipzig, 337–347 (1902).
25. Mars, G., *Die Spezialstahle*, Second Edn., F. Enke, Stuttgart (1922).
26. Diegel, C., *Verein Beförderung Gewerbfleisses*, **79**, 233–250 (1918).
27. Bramwell, F., *Proc. Instn. Civil Engrs.*, **102**, 1–37 (1889–90).
28. Fry, A., *Kruppsche Monatsh.*, **12**, 201–214 (1931).
29. Wiss, E., *Autog. Metallb.*, **22**, 46–53, 58–66 (1929); **23**, 150–157, 166–173 (1930).
30. Hatfield, W. H., Iron & Steel Inst. Weld. Symposium, **II**, 363–371 (1935).
31. Catlett, J. T., *Metals & Alloys*, **2**, 272–276 (1931).
32. Schiffler, H. J., *T Z prakt. Metallb.*, **42**, 324–329 (1932); *Petroleum Zeitschrift*, **30** (21) 1–7 (1934).
33. Schimpke, P., and Horn, H. A., *Praktisches Handbuch der gesamten Schweisstechnik*, **2**, Second Edn., Springer, Berlin, 203–208 (1935).
34. Martin, W. H., *Trans. Inst. Marine Engrs.*, **46**, 27–36 (1934–35).
35. Lent, H., *Z. VDI.*, **81**, 1097–1104 (1937).
36. Motok, G. T., *Trans. Amer. Soc. Metals*, **25**, 466–480 (1937).
37. Zimmerman, J. H., AMERICAN WELDING SOCIETY JOURNAL, **15** (2) 21–25 (1936); *Mech. Engg.*, **57**, 423–427 (1935); *Proc. Intnl. Acet. Assn.*, **36**, 79–90 (1935).

SUGGESTED RESEARCH PROBLEMS

1. The fundamental effects of aluminum on the welding of steel are not clearly known. On the one hand, aluminum forms an oxide film around the molten drop of weld metal. The film is difficult to flux and there is no information on the relative fluxing effect of different fluxing agents in different types of welding. On the other hand, the preferential oxidation of aluminum protects silicon, manganese and similar elements from loss during deposition, to an extent that has not been studied. Weld metal containing about 0.5 Al has been found to be unusually fluid probably because the temperature was unusually high as a result of alumino-thermic reaction. The fluidity of weld metal containing aluminum has not been studied.

2. It is not certain whether the effect of alumina in increasing the rate of cooling required to produce martensite in heat-affected base metal is conducive to crack-free welding.

3. Porosity has been associated with alumina by several investigators but the evidence has not been conclusive. Examination of beads of weld metal deposited on steels with different alumina contents should reveal any tendency toward porosity with increasing alumina content.

4. The difference between killed and rimmed steels of the same carbon and manganese content for filler rods has not been determined accurately. In oxyacetylene welding a rod containing 0.20 C, 0.80 Mn, 0.60 Si has been recommended for high-quality work. However, there is little information on the difference, if any, between rimmed and killed steel for any type of welding.

5. Alumina has been said to be harmful to the bend ductility of welds. The distribution of alumina in weld metal might be studied from a viewpoint similar to that adopted by steel casting researchers in their study of the microstructural occurrence of alumina in steel castings containing different quantities of sulphur and other elements. At least one investigator believes that bad welding properties may be traced to high-alumina content of the electrode, but conclusive results remain to be obtained.

STRESSES IN SINGLE ANGLES*

By JOHN MERLE, JR.† and WALDO W. SMITH‡

IT IS believed that since welded single angle tension members are subjected to both tension and flexure, they should be designed for maximum section modulus rather than for minimum eccentricity alone. With this end in view, comparative tests using parallel fillet welds were conducted for the purpose of determining the stress distribution resulting from various types of connections.

In the design of any structural member, two factors must be considered: distribution of stress in the member, and distribution of stress in the connection. These tests were primarily an investigation of the first factor.

There are in general three important factors which must be determined in designing a parallel fillet weld connection for a single angle tension member:

1. What percentage of the area of the angle may be thought of as resisting a uniformly distributed stress?
2. What length of weld shall be applied at the heel and toe of the attached leg of the angle so that the cross section may be utilized most advantageously?
3. Shall the axis of the load coincide with the center of gravity of the welds, or with the center of gravity of the angle, and what shall be the relative positions of the two fillet welds?

A. R. E. A. specifications for attaching single angle tension members with rivets state that "the effective area of single angles in tension shall be assumed as the net area of the connected leg plus 50% of the area of the unconnected leg." In the case of welded connections there is no such general agreement. The values used range from 60 to 80% of the gross area, with some designers considering the entire area effective.

The conventional method of design—the one most widely accepted—for attaching single angles by means of parallel fillet welds is that the amount of weld to be applied on the heel and the toe of the attached leg of the angle is such that the resultant pull of the welds on the angle is coincident with the gravity axis of the angle. Stated mathematically, the rule is to so proportion the weld that the lengths of the welds applied at heel and toe of the attached leg are respectively inversely proportional to the distance from the center of gravity of the angle (Fig. 1).

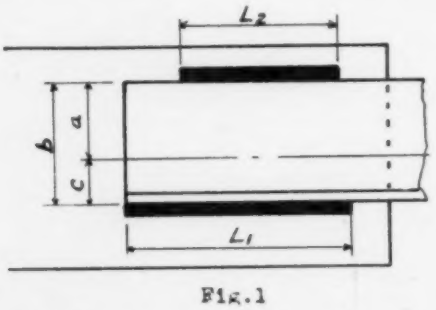


Fig. 1

L = total length

$L = L_1 + L_2$

$L_1 c = L_2 a$

$aL_2 = cL - cL_2$

$L_2 = \frac{cL}{b}$

$L_1 = \frac{aL}{b}$

Although it would seem that the most desirable condition would prevail when the line of pull coincides with the center of gravity of the angle, the tests described by Prof. Griffith** seem to indicate more eccentricity in the plane of the connected leg of the welded specimen than in the riveted connection.

One of the earliest attempts to arrive at some theoretical notion of the distribution of stress in an unsymmetrical section was made in 1880 by Prof. G. F. Swain of Harvard University. Others who have done much in this field were W. Ritter, Müller-Breslau, O. Mohr, R. Land and L. F. Johnson.

Of the three methods of handling the problem, two are analytical and one graphical. The first analytical method is derived from a consideration of the principal moments of inertia.[1]

The second method evolves an expression for stress in which the use of the principal moments of inertia is not considered. The general equation for a member subject to combined tension and bending is for maximum stress[2]

$$f = \frac{P}{A} + Pe \left[\frac{x(I_x \cos\Theta - J \sin\Theta) + y(I_y \sin\Theta - J \cos\Theta)}{I_x I_y - J^2} \right]$$

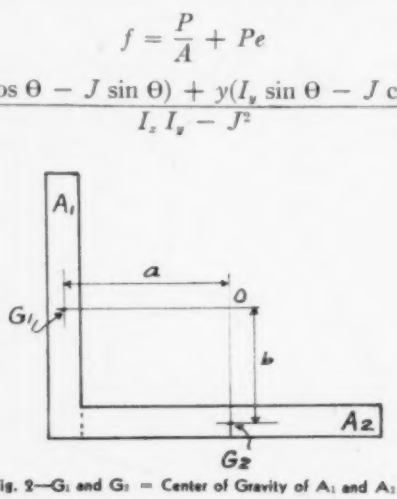


Fig. 2—G_1 and G_2 = Center of Gravity of A_1 and A_2

Of the symbols mentioned all are probably familiar to the designer except the product of inertia, J. This may be defined as summation of the products obtained by multiplying each infinitesimal area by the products of its coordinates. The following relation for angle sections is convenient because it is not necessary to know the coordinates of the centroid of the angle[3] (Fig. 2).

$$J = \frac{A_1 A_2 ab}{A_1 + A_2} \quad (1)$$

In the graphical method, the section modulus polygon is used to determine the magnitude of the section modulus and therefore the maximum and minimum stresses for any type of loading.

* Contribution to Fundamental Research Division, Welding Research Committee.
Thesis—Washington University, St. Louis, Mo.
† Hercules Const. Co.
‡ Stupp Bros. B. & I. Co.

** JOURNAL AMERICAN WELDING SOCIETY, Jan. 1931, pp. 46–47.

[1] Fuller & Johnson, *Applied Mechanics*.

[2] Johnson, L. J., "An Analysis of General Flexure in Straight Bar of Uniform Cross Section," *Trans. A.S.C.E.*, LVI, June 1906.

[3] Müller-Breslau, *Graphische Statik der Baukunstructionen*, I, pp. 43, 44.

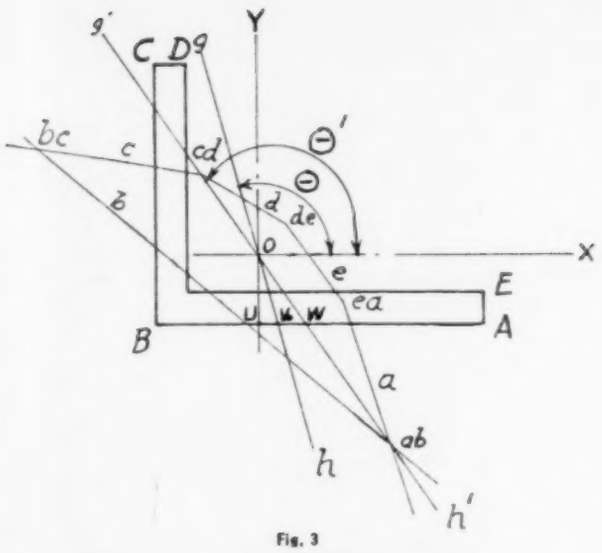


Fig. 3

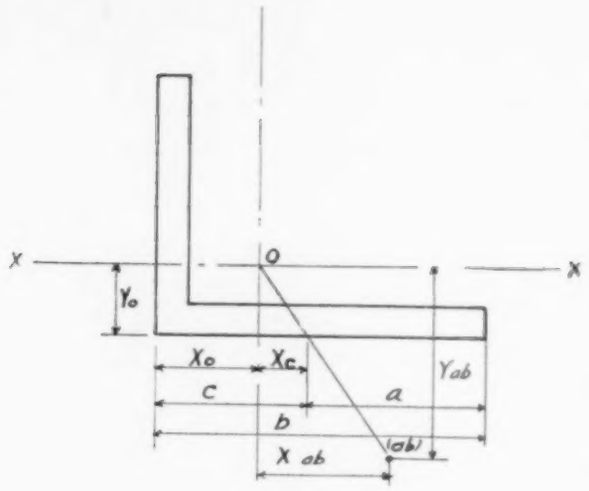


Fig. 4

An examination of the section modulus polygon for an angle section shown in Fig. 3[4] indicates that if the plane of the bending moment passes through the vertex (*ab*), the angle offers maximum resistance to bendings. Since stress is portional to the distance from the neutral axis, the neutral axis must be parallel to the leg *BA* if the stress across the leg *BA* is uniform.

The method of design proposed by the authors is to so proportion the length of weld to be applied at the heel and toe of the angle that the resultant pull of the welds passes through *W* of Fig. 3.

If the resultant load *P* is made to act through the point *U* of Fig. 3, as is done in present day practice, the value of the section modulus is much smaller than that obtained by causing the load to act through point *W*. In other words, it is possible to obtain a greater section modulus and consequently less stress due to bending for the proposed method than it is for the conventional method of design.

From Fig. 4 it is seen that the following relations are true by similar triangles:

$$\frac{x_c}{y_o} = \frac{x_{ab}}{y_{ab}}$$

It can be shown that

$$\frac{x_c}{y_o} = \frac{J}{I_x} \qquad \text{or } x_c = y_o\left[\frac{J}{I_x}\right]$$

then

$$c = x_o + y_o\left[\frac{J}{I_x}\right]$$

$$L_2 = \left[\frac{c}{b}\right]L$$

$$L_1 = \left[\frac{a}{b}\right]L$$

The steps in the design are:

1. Assume the effective area to be 60% of the gross section and select the size of angle,[5] and compute total length of weld, *L*.

2. Place the heel of the angle a distance $c = x_o + y_o\left[\frac{J}{I_x}\right]$ from axis of loading.

3. Place an amount of weld on heel equal to $L_1 = \frac{a}{b}L$.

4. Place an amount of weld on toe equal to $L - L_1$.

Of the factors necessary for the design all except *J* can be obtained from the handbook of any structural steel company. The values of *J* for many angles may be obtained from L. A. Waterbury's "Stresses in Structural Steel Angles," John Wiley & Sons, or calculated from Formula 1.

Fig. 5

A series of tests were run at Washington Un versity to check the advantages of the proposed method of design over the conventional. The specimens were made of $2^1/_2 \times 2 \times {}^1/_4$ angles with $^1/_2$ inch connection plates. The angles were painted with calcimine paint to show the distribution of strain lines. Strain gage readings were taken at loads of 5000, 15,260, 25,000, 28,240, 33,000 between the gage points indicated in Fig. 5. These readings were plotted on individual graphs some of which are shown in Figures 6 and 7. Specimen 1*A* was prepared according to the conventional method of design, while specimen 7*C* was prepared according to the method proposed in this paper. Figures 6 and 7 show critical loading conditions prior to yielding and at the point where some yielding has occurred.

From these graphs and others which were prepared the following facts are discerned:

1. In specimen 7 the stress in the attached leg is more uniformly distributed than in specimen 1.

2. The stress along gage line *E* is generally smaller in specimen 7 than in specimen 1.

3. The stress along gage line *A* is generally greater in specimen 7 than in specimen 1.

4. In section 3 the stress along gage line *A* and *B* is slightly greater in specimen 7 than in specimen 1. Along lines *C* and *D* the stress is about the same in both designs. Along *E* the stress in specimen 7 is less than that in specimen 1. Along *G* the stress is about the same for both designs. Along *H* the compressive stress in specimen 7 is slightly less than in specimen 1.

[4] See Fuller and Johnson, "Applied Mechanics" for construction.

[5] This value was concluded from the specimens tested and needs additional verification by future experiments.

5. In section 5 to the center line of angle, the stress along A, B and C of specimen 7 is greater than in specimen 1.

In section 2 local stress concentration occurs along the line E in specimen 1. In specimen 7 stress concentration occurs along A and E. Neither the conventional design nor the proposed design is intended to remove this local stress concentration.

As a result of these tests, the proposed method is more desirable than the conventional method for the following reasons:

1. The proposed method of design gives a more uniform distribution of stress in the attached leg of the angle.
2. For the same stress at the critical points, it is possible to resist greater loads in a member by the proposed design.
3. On the basis of yield point stress, the area over which the stress may be considered to be uniformly distributed is greater for the proposed method.

More experiments should be performed to verify the results of this series before this method could be generally accepted.

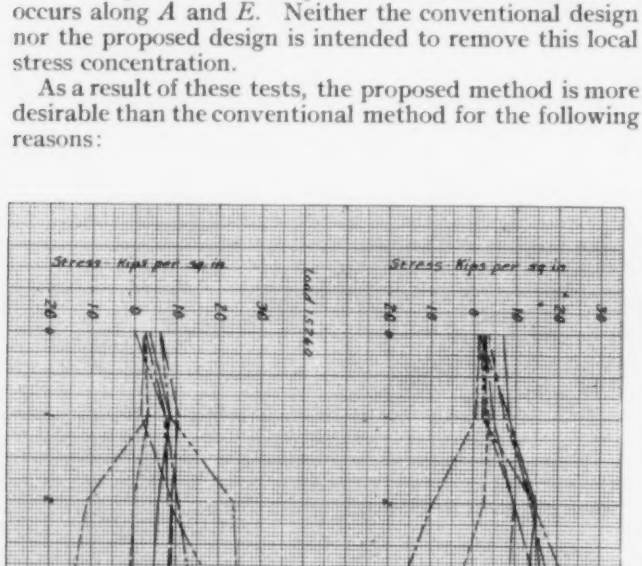


Fig. 6

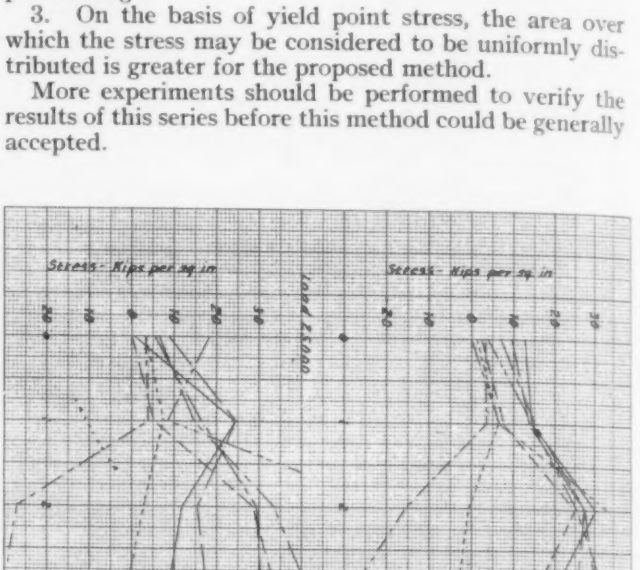


Fig. 7

Welding Low-Alloy Structural Steel*

By G. SCHAPER

IN VIEW of the large number of successful applications of welded low-alloy steel in bridges, buildings, boats and railway cars in Germany, the two instances in which cracks occurred are particularly interesting. One bridge was of rigid frame construction, 163 feet span, carrying heavy railway traffic in Berlin. The frames were about 10 feet high, the flanges being of nose-profile type, $2^1/_2$ inches thick, 20 to 24 inches wide. After the bridge had been in service about six months several cracks, each a fraction of an inch long, were found that originated in the fillet welds joining web to flange and that spread into web and flange, Fig. 1. The ends of the cracks were drilled out and the bridge carried 300,000 trains without further spreading of the cracks, after which the cracked frames were replaced. It was believed that the composition of the steel favored excessive hardening in the heat-affected zone. As a result the German Railways restrict the composition of low-alloy steel for welded bridges to a maximum of 0.20 C, 0.50 Si, 1.2 Mn, 0.55 Cu, 0.06 P, 0.0 S, P + S = 0.1. An additional 0.30 Mn or 0.40 Cr or 0.20 Mo is permissible.

The second bridge in which cracks occurred is at Rudersdorf, Germany, and was under construction at the time cracks appeared in the Berlin Bridge. Steel of the same composition was used in both bridges. Therefore, special precautions were taken in inspecting the welds. Nevertheless, after the bridge had been in service a short time two main plate girders the flanges of which were of the nose-profile type cracked suddenly on a particularly cold night. The cracks were long, showed no evidence of deformation, spread half way up the web from the cracked tension flange and occurred in a lightly stressed part of the girder where the flange

* Abstract of "Der Hochwertige Baustahl St 52 im Bauwesen," published in *Bautechnik*, **16** (48) 649–655, Nov. 11, 1938.

was only $1^1/_2$ inches thick. The cracks were attributed to tensile shrinkage stresses in the fillet weld joining web to flange which were at right angles to the tensile stresses created by dead load and traffic. The bi-axial stress system was believed to favor brittle failure.

In view of the two instances in which cracks occurred, a series of static and fatigue tests on welded low-alloy girders is being carried out. Preliminary static tests of girders welded under most severe conditions (flanges packed in ice during welding) failed to produce cracks. Instead, failure occurred by buckling.

To determine whether a low-alloy steel is suitable for welding, the quench bend test (steel quenched from 900°C. in oil at 20 to 500° C.) has not been so satisfactory as the bead bend test, in which a bead 12 inches long is deposited down the middle of a plate 28 inches long, 8 inches wide, 2 inches thick. Specimens of low-alloy steel welded at 20° C. failed suddenly with practically no bend angle (7 to 13°) whereas similar specimens with bead deposited on base metal preheated to 300° C. withstood over 90° bend without failure.

Quite similar results were obtained with unalloyed mild steel. Unpreheated specimens failed at a bend angle of 9° to 23°, whereas specimens preheated to 200° C. withstood over 90° bend without failure. Apparently the shrinkage stresses in the unpreheated specimens of both mild and low-alloy steels account for their brittleness in the bead bend test rather than the hardness, which was much lower in the mild steel than in the low-alloy steel. Since welded bridges of mild steel with thick flange plates have been completely satisfactory in service, the bead bend test as used at present is not necessarily a critical test for the weldability of low-alloy steel.

If the beads are deposited on both edges of the bend specimen instead of down the middle, the bend angles are large without preheating with both steels. The system of shrinkage stresses that is created by the beads on the edges does not seem to restrict deformation. Large bend angles are secured with specimens initially 8 inches wide with bead down the center, but planed to 2 inches wide after the bead has been deposited at 20° C. The reduction in width redistributes the shrinkage stresses in a manner favorable to deformation.

Annealed low-alloy steel (slowly cooled from 850° C.), 2 inches thick, is far more ductile in the bead bend test than as-rolled steel of the same composition. Whereas as-rolled, bead-bend specimens fractured completely at about 3° bend angle, specimens with bead deposited on annealed steel at 20° C. showed cracks in the bead at 27–29°, and base metal exhibited no cracks even at a bend angle of 120°.

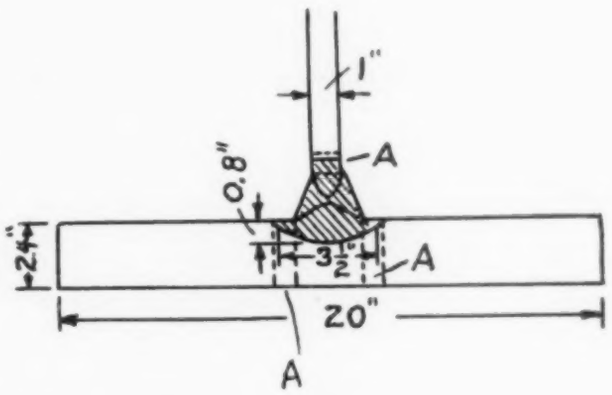


Fig. 1—Section Through the Crack in the Berlin Bridge. Shaded Area Represents Extent of Crack

A = drill holes $^3/_4$ inch diameter used to prevent spreading of the crack

The following conclusions are believed to be warranted by the above tests.

1. Fillet welds on the surfaces of thick plates of the type used in welding stiffeners to webs should not be used.
2. Preheating greatly reduces the shrinkage stresses.
3. Thin plates provide less difficulty than thick.
4. Annealed low-alloy steel is much more suitable for welding than as-rolled low-alloy steel.
5. Ribbed flange plates are satisfactory from the standpoint of shrinkage stresses.

Discussion of Paper on Carbon Arc Welding of Silicon Bronzes*

By J. J. VREELAND† and M. L. WOOD‡

OUR investigations using the authors' flux do not seem to yield the high strength welds as mentioned. Table 1 gives the authors' procedure and two comparison procedures. We were not quite satisfied with the authors' procedure and since results obtained were not as expected, the comparison procedures were used. These procedures appeared to be more logical for commercial application.

The results as obtained by our investigations are given in Table 2. The only high tensile values obtained were with samples having the weld metal subjected to peening operations. In all cases where peening was not used, the tensile values were far below those claimed by the authors.

We also wish to comment on the authors' statement that with the use of the comparison flux, that poor fluidity was obtained in the deposition of the first two or three beads in a multiple bead welding operation. In all of our experience, we have never encountered this condition unless insufficient heat is used. The deposition of the second bead is the most difficult to perform since the welding speed is approximately one-half that used when depositing the first bead. The reason for this is the necessity of weaving the electrode to obtain fusion to the base metal, and since the heat used is higher than for the first bead, danger of burning or sagging through is present. Because of this danger most welders have a tendency to shorten the arc or to increase the rate of welding. The result in either case is poor appearance and weld metal containing slag inclusions.

* Paper by E. S. Bunn, J. R. Hunter and W. G. Seidlitz, published in Welding Research Supplement to Oct. 1938 issue of THE WELDING JOURNAL.

† Metallurgical Engineer, Sales Promotion Division, Chase Brass & Copper Company

‡ Chase Brass & Copper Company.

Table 1—Welding Procedures

Weld Groove	Root Aperture	Bead No.	Authors' Filler Rod	Carbon Diam.	Closed Amperes	Circuit Volts
60°	1/8 In.	1	3/16	3/8	140	25–30
		2	5/32	3/8	150	25–30
		3	3/16	1/2	170	30–35
		4	1/4	1/2	200–210	35–40
		5	5/16	1/2	240	35–40

Weld Groove	Root Aperture	Bead No.	Comparison Filler Rod	Carbon Diam.	Closed Amperes	Circuit Volts
60°	3/32 In.	1	5/32	1/4	100–130	25–30
		2	3/16	5/16	150–160	30
		3	1/4	5/16	170–190	30–35
		*4	1/4	5/16	180–190	30–35
	* 4th bead filler rod would be 5/16 inch if peening is done after every bead except the first. (Amps.—200)					
75°	1/16 In.	1	1/8	1/4	100–120	25
		2	3/16	1/4	140–150	30
		3	1/4	5/16	160–180	30–35
		4	5/16	3/8	200	35–40
		*5	5/16	3/8	200	35–40
	* 5th bead required when peening is done after every bead except the first.					

During our welding investigations, which were done by an experienced welder of silicon bronzes, we noted that the apparent fluidity of the weld metal was lowered due to the lower fluidity of the slag formed while using the authors' flux. The presence of manganese boride evidently caused the resulting slag to harden quite rapidly with the result of inferior flowing qualities. It was necessary to increase the welding heats to overcome this condition in order to insure us of sound weld metal.

The number of test welds made were insufficient to draw any definite conclusions but the following conclusions appear to be as determined:

1. Results claimed were not obtained.
2. Comparison flux has greater fluidity.
3. Comparsion flux yields denser weld metal.
4. " " permits the use of lower welding heats.
5. In order to obtain high tensile values, cold working of the weld metal must be resorted to.
6. Resulting slags from both fluxes are easily removed.

Table 2

Gage Base Metal	Welding Groove	Flux	Welding Procedure	Tensile Strength, No. 1 Sq. In.	Free Bend, %	Remarks
1/2 In.	75°	Comparison	Comparison	57,000	76	Peened
"	"	Authors'	"	58,000	60.5	"
"	"	Comparison	"	48,200	69	Not peened
"	"	Authors'	"	45,800	47	" "
"	60°	Comparison	"	48,700	52	" "
"	"	Authors'	"	49,000	65.5	" "
"	"	"	Author's	45,600	51	" "

Composition of Comparison Flux—90% Powdered Fused Borax
—10% " Sodium Fluoride

All peened specimens relief annealed.

Authors' Reply

THE authors wish to acknowledge Mr. Vreeland's critical discussion of their paper. As an introduction to our reply, and particularly with regards to Mr. Vreeland's having pointed out that his investigation welds were made by an experienced silicon bronze welder, we wish to point out that oftentimes success in the use of any method of welding depends not only on experience but also on a thorough understanding of the mutual relationships of the variables that go to make up the method and a close tie-up between this understanding and the actual welding operation.

The paper presented was a report on work done over a period of a year, and the results reported are a typical few from many. In this respect we note in Mr. Vreeland's discussion that "the number of test welds made (by Mr. Vreeland and his associates) *were insufficient to draw any definite conclusions* but the following conclusions *appear* to be....."

As regards his reference to dissatisfaction with our procedure, and expanded orally by him, on the following points:

(1) A root aperture of 3/32 inch was claimed to be better than the authors' recommended 1/8 inch (approximate). In reply, the authors wish to emphasize the word "approximate." We feel that a difference of 1/32 inch is not sufficient to warrant much ado in such a practical field as welding. We find that the procedure and method discussed in our paper is applicable with good results if the root aperture is held to 1/8 inch plus or minus 1/32 inch.

(2) As regards our using a 3/16 inch diameter filler rod for the first bead and a 5/32 inch diameter filler rod for the second bead, an order the reverse of that claimed to be better by Mr. Vreeland, we wish first to call attention to the third paragraph of his written discussion in which he agrees with the authors on the matter of the second bead being the most difficult to deposit. We have found in depositing the first bead that by directing the arc directly on the 3/16 inch diameter filler rod (no weaving) the parent metal edges receive sufficient heat from the preheat of the arc and by conduction that they fuse and blend readily with the filler weld metal. Even thorough penetration is readily obtained. The 3/16 inch diameter rod so deposited provides a thicker first bead —a better bottom bead upon which to lay the troublesome second bead. The use of the smaller diameter rod for the second bead has an advantage in that less heat is required for the lesser amount of metal added and the sagging and burning-through dangers are minimized. Further, the welder's visual accessibility into the narrow confines of the lower part of the vee is not impaired by too much weld metal. The combined first two beads net the same result whatever order they are used in.

(3) As regards Mr. Vreeland's claims that the amperages recommended by the authors are too high, we

wish to call attention to Paragraph 2, Page 6, of our paper "The proper amperage to be used depends somewhat on the individual welder's experience and capabilities. Results indicate that the current may be varied over a fairly wide range as indicated in Figs. 9 and 10 providing that within practical limits the heat input per inch of weld (or bead) is constant." In our opinion, based on practical applications of the method and after working with amperages both above and below those indicated in Figs. 9 and 10, the amperage ranges are the optimum when the "arc length, the nature and solubility of flame gases, the effect of carbon transfer, and the relation between the arc length, current and rod diameter are considered with respect to their mutual relationship."

As to Mr. Vreeland's references in Paragraphs 2 and 3 of his written discussion on the fluidity of the flux used by the authors, we have found that the other ingredients of the flux have, in our opinion, more than made up for any adverse tendencies the manganese boride may have on the flowing qualities of the flux as a whole. In evaluating the importance of each of the constituents in the flux we have made test welds using manganese boride alone as a flux. Admittedly it alone did not possess the desired fluidity but despite this fact we obtained weld metal strengths of 56,000 psi (reduced section). We wish also to call attention to the fact that while manganese boride is used to the extent of 25% by weight of the flux, it comprises only 12% of the flux by volume. While the fluidity of the flux cannot be settled by argument we wish to point out that with the flux applied thinly and evenly as an alcoholic paste, relatively little flux and very little manganese boride is present to impair, as is claimed by Mr. Vreeland, the flowing qualities of the molten weld metal itself.

In regards to Mr. Vreeland (and his associates) using "comparison procedures that appeared to be more logical for commercial application" for testing our flux, we wish to state that no claims were made in the paper for the flux as a cure-all. The authors' paper reports only that "general improvement in the quality of welds was effected when such factors as arc length, number and thickness of beads, and the path of motion of the electrode were properly correlated" and "further improvement in (weld metal) density, mechanical properties and consistency of results was noted in welds made with" the authors' flux.

Finally, whatever differences of opinion may exist as to details of the method it is the end attained that matters and the authors feel that their results, obtained in the manner reported, speak for themselves.

SOME PRESENT-DAY PROBLEMS OF Strength in Structural Welding

By DR. ING. G. BIERETT

1. GENERAL CONSIDERATIONS

THE strength of a structure is the resultant of a number of individual factors arising from the quality of the material, the workmanship and the design. This basic rule applies to welded construction as well as to all other types. However, the load carrying capacity attained by the combined action of all individual factors depends very largely on the method of construction.

Comparing the two common types of structural construction: bolted or riveted joints or welded joints, we find that the factors involved are entirely different. The factors are more numerous and more interrelated in welded construction than in riveted.

The strength of riveted structures is determined by three factors quite distinct from each other—the quality of the steel, the quality of the riveting, determined principally by clamping and frictional forces, and the extent to which the structure as a whole and each riveted joint in particular has been correctly designed.

Although corresponding factors are operative in welded joints, they may be much more closely controlled through the media of material, workmanship and the conditions of construction. This close control and connection among the factors is possible because the weld is purely a material joint between the structural elements and the jointing element—the weld metal itself. The material factors in the weld zone are not only dependent on the base metal. The type of welding exerts more or less effect. The three factors affecting the material conditions are the base metal, the weld metal and the type of welding.

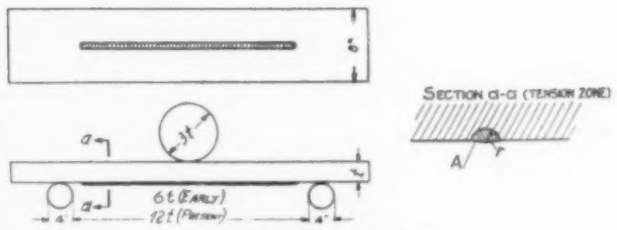


Fig. 1—Weld Sensitivity Test

A = single bead; diameter of electrode = 0.20 inch for thick plates
r = 0.16 inch

The strength factors originating in the type of construction—general location of parts, distribution of load, type and location of welds—combine with other factors created by workmanship which likewise affect stress distribution. In particular, there are the junction of weld with base metal, penetration and welding defects. The effect of these factors on stress distribution depends a great deal on the material factors in the region of the weld.

Furthermore, welded structures are afflicted with shrinkage stresses whose effect in soft steel is negligible but in higher carbon and alloy steel may be paramount under suitable circumstances. The thermal conditions determining shrinkage stresses in these steels are at the same time the determining factors for the welded zone, particularly for the material in the heat affected zone. The properties of the heat-affected zone, in turn, determine the magnitude of the shrinkage stresses.

NOTE: Prepared especially for the Welding Research Committee and translated by G. E. Claussen.

Fig. 2—Experimental Arrangements for the Sensitivity Test

All in all, there is a very close relationship between material factors, design factors and workmanship. If we are to be successful in the welding of large, heavily loaded structures, all these factors must be regarded from a common viewpoint.

2. SENSITIVITY OF STEELS TO WELDING

For the softer grades of steel, that is, those with low-carbon content and without alloying elements, it is quite feasible to divide the problem into three parts: (1) steel and filler rod, (2) design and (3) the welding operation. The relatively slight dependence of the properties of the welded zone on the type of welding on the one hand, and the large factor of safety against the effects of shrinkage stresses provided by the plasticity of the steel on the other hand, have resulted in the elimination of difficulties once high grade filler rods and correct methods of design had been developed. There still remains the problem of permissible sulphur and phosphorus content of soft steel for welds of guaranteed quality.

With the *higher strength structural steels* containing higher carbon and some alloying elements it is not clearly enough realized that conditions are very different with respect to shrinkage stresses than they are in the softer steels. The great deal of attention directed to this question in recent technical literature on welded structures is a result.* A complete investigation of these problems recently has clearly resolved the main influences at work.

First the necessary limitations of carbon and alloy contents were determined. For low-alloy structural steel (called St 52) the following specifications have been laid down: C = 0.20%; Si = 0.50%; Mn = 1.2%; Cu = 0.55%; P = 0.06%; S = 0.06% and P + S together = 0.1% to which may be added either 0.30 Mn, or 0.40 Cr, or 0.20 Mo.

Although restrictions on composition are an advance, recent research has shown that absolute safety cannot be assured through these restrictions alone: The properties of these steels may yet be so changed as a result of welding that they become unsuitable for structures.

As a general remark on the steel question, the *selection of weldable steels* should not be based solely on the ability of the steel to be welded under definite welding conditions. On the contrary, no likelihood of damage must appear under all the variations of welding conditions that must be encountered. For this reason mechanical tests of welds in Germany are always based on unfavorable welding conditions. There can be no compromise with absolute safety.

The chief causes of difficulties in welding the stronger steels at present are the hardening that occurs during cooling after arc welding and the associated shrinkage stresses. Since both influences are operative at the same time, relatively simple technological tests must be devised whereby both effects may be studied at once.

3. TESTS FOR SENSITIVITY TO WELDING

The most essential test for sensitivity to welding consists in depositing a single bead of weld metal down the middle of a long, thick plate.† This type of specimen, which is still being developed, is subjected to a plunger-bend test with the bead in tension. The test is continued beyond the first crack until the specimen is completely broken, Figs. 1 and 2. The angles of bend at the first crack and at complete rupture are measured as well as the elongation of the welded zone on short gage lengths over the fracture.

The type of specimen and the welding conditions represent circumstances that are practically attainable though unfavorable, in agreement with the general safety principle enunciated above. Depositing a single bead on a wide, thick plate is anything but a desirable condition. For plates $1^5/_8$ to 2 inches thick the diameter of the filler rod is usually 0.20 inch, since smaller electrodes, while representing still less favorable conditions, are never used on thick plates. By melting the rod into a semi-circular groove, 0.16 inch radius, constant conditions are assured for different tests. Thus, the amount of material deposited is held approximately constant.

The development of these technological tests requires

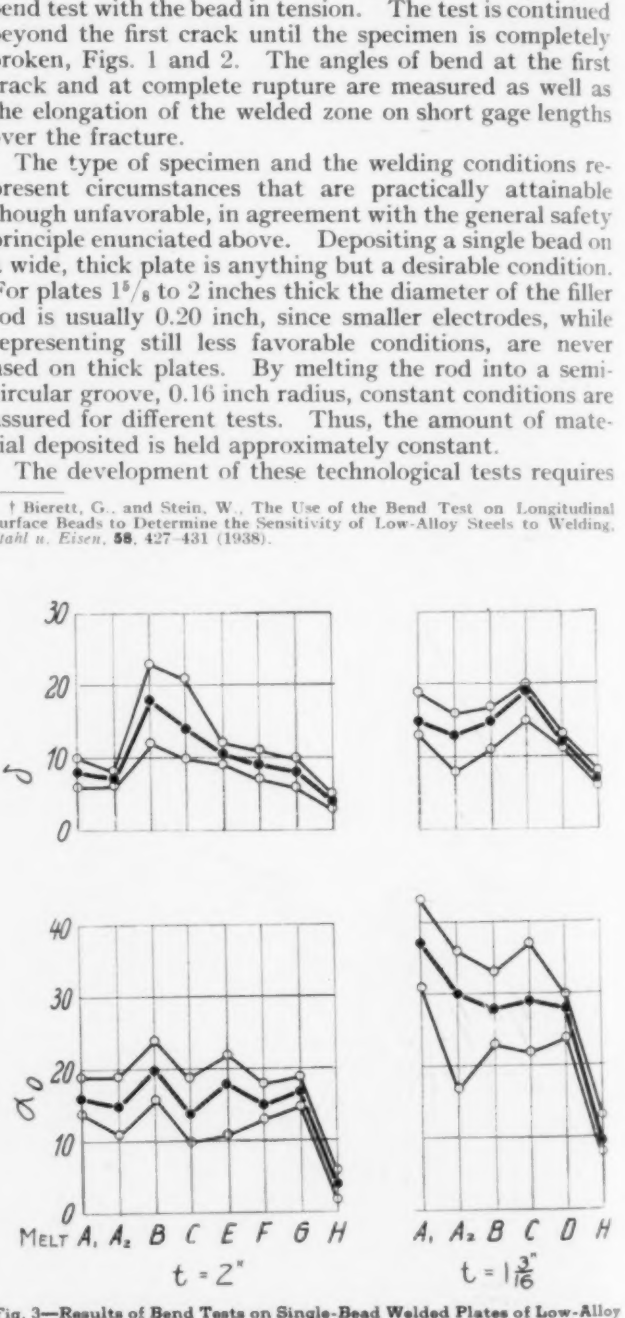


Fig. 3—Results of Bend Tests on Single-Bead Welded Plates of Low-Alloy Structural Steel St 52. Opening Between Rollers = 6t

δ = elongation over fracture, % in 0.39 inch
α0 = bend angle at first crack
●—● averages
○—○ maximum and minimum values

* Schaper, G., International Conference in Zurich on Welding in Bridge Construction, *Bautechnik*, **16** (26) 346–347 (1938).

† Bierett, G., and Stein, W., The Use of the Bend Test on Longitudinal Surface Beads to Determine the Sensitivity of Low-Alloy Steels to Welding, *Stahl u. Eisen*, **58**, 427–431 (1938).

still further investigation in a number of directions. First of all, experiments must be made on the effect of type of specimen—distance between supports, breadth, thickness, etc. Then, too, some thought must be given to the effect of welding conditions, and the type of filler metal.

So far, results suggest that the width of the specimen is particularly important. With the specimens 2 inches thick used to a great extent in Germany, unfavorable conditions are attained at a width of as little as 8 inches, so that this width has come to be recommended. The width of 8 inches is also used for specimens 1.18 and 1.57 inches thick. The distance between supports has much less influence on results than the width. The original opening between rollers was 6 times the thickness, which required too much power for the test. In order to permit the use of smaller machines, the opening has been changed to 12 times the thickness.

The results of tests that have been performed, which, however, are not so numerous as might be desired, have not suggested that welding conditions are critical. The quantity of deposited weld metal is determined to an approximate extent by the semi-circular groove. Using the customary amperage for the electrode being tested, we find that the welding velocity is determined quite closely, particularly since only one bead is deposited. This question will be more thoroughly studied in the future to determine what unfavorable effects will be created by variations in speed occasionally encountered in practice.

Systematic tests on the effect of different filler rods also remain to be made.

Tests that have been made on numerous steels of St 52 type have shown considerable scatter. Nevertheless, for particularly sensitive steels the test is important. The scatter in parallel tests, Fig. 3, is due mainly to lack of constancy of welding conditions. The maximum, average and minimum value for one steel of the group tested classifies that steel as particularly sensitive to welding. With further development this type of test will be of notable assistance in evaluating the sensitivity of a steel to welding. It will also develop into a simple means for determining most desirable welding condition for research and operating purposes.

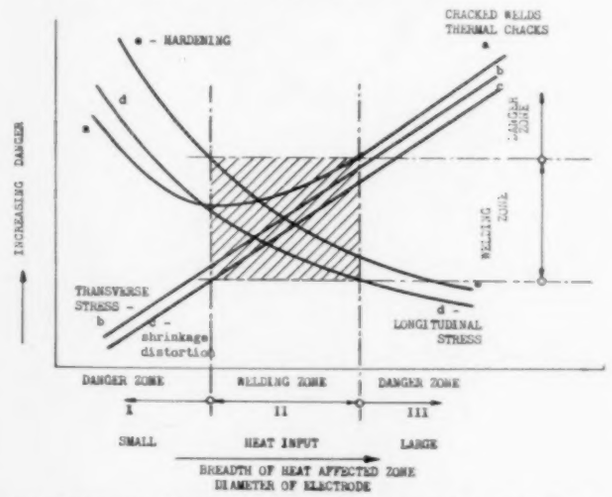


Fig. 4—Schematic Representation of Thermal and Material Sources of Danger in Welding

I = danger zone
II = welding zone
III = danger zone
Curve *a*—right-hand side = cracked welds (thermal cracks), left-hand side = shrinkage cracks
Curve *b*—transverse reaction stress
Curve *c*—shrinkage force, distortion
Curve *d*—longitudinal stress
Curve *e*—hardening

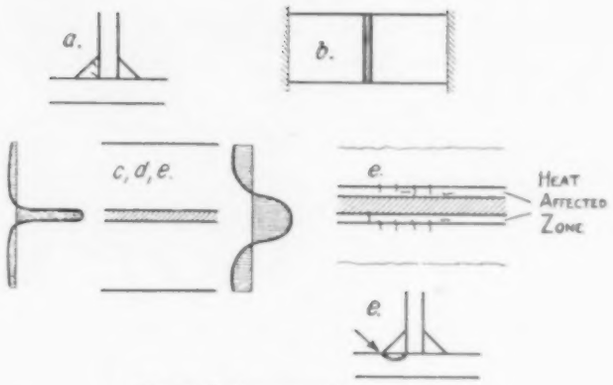


Fig. 5—Explanation of Terms in Fig. 4

a—cracked welds and shrinkage cracks in the direction of the weld
b—external and internal reaction stress
c—longitudinal shrinkage force, distortion, or overheating
d—maximum longitudinal stresses in the welded zone (weld or heat affected zone)
e—hardening, shrinkage and hardening cracks of any kind, especially transverse to the direction of the weld

In its present form the specimen is large and expensive. Attempts to use smaller, thinner specimens have already been made.‡ It appears that, if suitable changes in welding conditions (electrode diameter, etc.), the same effects are obtained with the small specimens as with the large.

The real *causes* of the unusually high *sensitivity* of some steels to welding are not yet clear. There is an obvious relationship between the capacity of a steel for deformation and the hardening in the heat-affected zone. However, the factors upon which hardening depends with constant welding conditions are not known definitely. Doubtless carbon content and some alloying elements are important, yet there are remarkable differences in capacity for deformation among steels having nearly the same chemical composition.

Of the steels represented in Fig. 3 and Table 1, which are steels at the upper limits of permissible alloy content, only steel *A*2 has unusually high-carbon content. Yet this steel is quite ductile in the bend test, Fig. 3. Compared with the other steels, steel *A*2 is by no means poor. Steel *H* is remarkably sensitive to welding. Another steel of almost the same composition from another mill was little better. Surprisingly enough, a third steel of the same composition from a third mill had a particularly high capacity for deformation in the welded condition. This steel, 1.57 inches thick, 8 inches wide, when bent between rollers having an opening of 12 times the thickness = 19 inches, withstood the following bend angles in 8 tests: 29, 31, 33, 35, 38, 42, 44 and 48 degrees, average = 38°. The local elongation amounted to 20 to 25% in 0.39 inch. A comparison of these results with the unfavorable results with steel *H* in Fig. 3 shows that, within limits, the composition of the steel is not the only vital factor for sensitivity to welding. Melting, rolling and cooling conditions and heat treatment of the rolled plate exert an important effect, which remains to be explained.

4. SHRINKAGE STRESSES AND OTHER SOURCES OF DANGER

The difficulties with the higher tensile steels have renewed interest in shrinkage stresses with which the difficulties are believed to be associated, the absence of difficulties with the lower-tensile steels having relegated the stress question to the background for a time. The

‡ Paper at the Third Kjellberg Conference by Dr. H. Groebler, see also *Esab's Tidning Svetsaren*, (9) 213–227, July 1938.

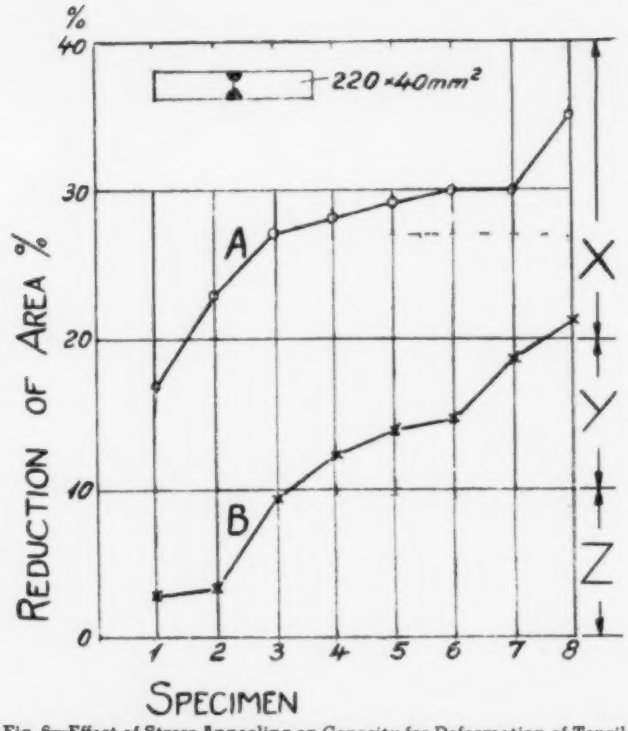


Fig. 6—Effect of Stress Annealing on Capacity for Deformation of Tensile Specimens of Low-Alloy Structural Steel St 52 with Longitudinal Welds Made with Covered Electrodes in 4 or 5 Layers on Both Sides

A = stress annealed
B = as-welded
X = ductile fracture
Y = transition
Z = brittle fracture

main relationships between shrinkage stresses and welding conditions have been investigated and are well known. The application of existing information to operating problems has not been completed.

To enable practical application of existing knowledge we must distinguish clearly between reaction stresses and residual stresses. In general, the reaction stresses perpendicular to the weld due to the cycle of local heating, upsetting, cooling and shrinking during welding, as well as the shrinkage stresses parallel to the weld, increase with increase in heat input. The residual stresses parallel to the weld, due to welding alone and not related to restraint together with the secondary stresses perpendicular to the weld and required by considerations of equilibrium increase with increase in cooling velocity, in other words with decrease in heat input and corresponding decrease in size of heated zone.

The relation between the different sources of danger and the thermal conditions is shown in Figs. 4 and 5, with particular reference to the welding of high-tensile steel. The illustrations are, and can only be, schematic in view of the divergence of prevailing opinion on several subjects. The course of the different lines appears to agree with present information, although some of the lines are not based on experiment.

The different sources of danger must be clearly distinguished. Not only must we keep in mind the externally visible damage, such as cracks and distortion, but we must be equally cognizant of equally dangerous effects, such as hardening and unusually high longitudinal shrinkage stresses. Among cracks we must distinguish between those caused by excessive unit stress usually associated with hardening and corresponding decrease in capacity for deformation, and, on the other hand, the so-called cracked weld, which is believed at present to be related to thermal checking or red shortness and to metallurgical, chemical and structural phenomena. Figure 5 explains the terms used in Fig. 4.

The right-hand part of line *a* and all of line *e* are related to the material Line *e* is characteristic of the steel and the right-hand part of line *a* is really a characteristic of the electrode although the material properties of the steel are inter-related with those of the electrode.

Lines *b* and *c* are thermal lines determined by the physical properties of the steel, such as thermal conductivity, specific heat, etc.

The left-hand part of line *a* and all of line *d* are mainly thermal characteristics, but material factors have some influence on their course.

Of course, the location of all these lines depends on the type of structure, dimensions, type of joint, etc.

In order to widen the welding region (II in Fig. 4) we must make line *e* as nearly horizontal as possible. Conforming with the necessities of steel production, limits are imposed naturally by the required mechanical properties. The lowering of the right-hand part of line *a* to widen the welding range by developing rods less sensitive to cracking in the weld metal is a problem of the electrode industry, in general. The electrode industry does not appear to be in the position to decrease the tendency to hardening, although with electrodes unusually resistant to cracking there is a possibility of using larger heat inputs without the danger of cracked welds.

With the lower-tensile steels there is little concern about restraint and shrinkage stresses. They are minimized in structural welding by employing relatively thin electrodes and multi-layer welding, which reduce the heat input per unit time to a minimum. If the same

Fig. 7—Strength and Ductility of Low-Alloy Structural Steel St 52 9 x 1.57 Inches Cross Section with Longitudinal Welds Made with Covered Electrodes

Upper photograph: normal hardness (200 to 250 Brinell) without shrinkage stresses
Tensile strength = 80,000 to 85,000 lb./in.²
Elongation = 24 to 27% in 4 inches
Reduction of area = 23 to 35%
Middle photograph: normal hardness (200 to 250 Brinell) with shrinkage stresses
Tensile strength = 81,000 to 87,000 lb./in.²
Elongation = 15 to 20% in 4 inches
Reduction of area = 14 to 21%
Lower photograph: excessive hardness (350 to 400 Brinell) with shrinkage stresses
Tensile strength = 58,000 to 67,000 lb./in.,
Elongation = 2 to 3% in 4 inches
Reduction of area = 3 to 4%

procedure is adopted for the higher tensile steels, hardening is favored by the high cooling speeds. At the same time, high shrinkage stresses are produced. Therefore, the above recommendations concerning the steel must be supplemented by suitable welding technique. The basic rule must be observed that the heat input per unit time must not be too small otherwise the cooling velocity is too great.

5. SHRINKAGE STRESSES IN WELDED GIRDERS

Besides the material factors associated with the shrinkage stress problem, there are factors related to restraint in large welded structures that have little connection with the steel, yet determine the magnitude of the reaction stresses. The field joints in the welded plate girders now used in Germany are welded, the butt type of joint being preferred for its high fatigue strength. In these joints plates of St 52 up to 2 inches thick may be welded together. A long investigation has been performed to determine the effect of constructional conditions, clamping arrangements, weld sequence and method of welding on the shrinkage stresses in the girder butt joints. The author has studied 22 butt joints made in the field on 8 highway bridges in Germany. The results of a similar investigation on four additional structures have been reported recently by Professor Graf.[1] All indications are that the considerations discussed above are the most important to be borne in mind to avoid major reaction stresses between flange and web and to maintain the stresses due to the tendency of the thick flange plates to bend at a minimum. The details of these researches will be published soon.

6. INCREASING THE QUALITY OF WELDED STRUCTURES

The results of fatigue tests and shrinkage investigations on welded structures emphasize the importance of the mechanical properties of the welded zone for the strength and security of the structure. Since the mechanical properties of the welded zone are related not only to the steel but to a pronounced extent also to the system of external loads and shrinkage stresses, improvement can be achieved only through simultaneous consideration of both material and stress factors. Thermal treatment during welding and heat treatment after welding may be relied upon for the desired improvement.

Preheating can be considered only as a supplementary means to increase security in the event that the heat of welding is not sufficiently intense to accomplish the desired effects on the structure of the steel or that the shrinkage stresses are particularly severe. In such circumstances preheating (200° C.) results in remarkable improvements. Unfortunately, preheating cannot be applied to large structures, except under unusual conditions, because the equipment for preheating is too difficult to handle in practice.

Above all it must be realized that, at least with the higher tensile steels, annealing is the safest procedure to adopt to ameliorate undesirable microstructure and to eliminate shrinkage stresses. Stress annealing at 600° to 650° C., which is the only type of annealing that is feasible in practice, eliminates the zones of maximum hardness at the same time that it relieves the shrinkage stresses, which are the causes for brittle fractures.

A good illustration of the increase of capacity for deformation—used as a measure of plastic reserve—due to stress annealing is provided by Fig. 6, in which is plotted the reduction of area of large tensile specimens, 9×1.57 inches cross section, having longitudinal welds. The weld metal is deposited in semi-circular grooves, 0.39 inch radius, in four or five layers on each side of the specimen. Covered electrodes 0.16 and 0.20 inch diameter were used. A comparison is made between 8 as-welded specimens and 8 welded and stress-annealed specimens.

Stress annealing considerably increased the capacity for deformation. The reduction of area of unannealed specimens 7 and 8 was higher than that of one of the stress annealed specimens. Hence, with correct welding conditions stress annealing may be unnecessary. However, on the whole, the beneficial effect of stress annealing is obvious. Stress annealing must not be relied upon as a cure-all for every welding difficulty but greatly ameliorates the effects of any chance irregularity that may occur during welding. The improvement due to stress annealing in Fig. 6 is believed to be associated mainly with the elimination of the high longitudinal shrinkage stresses in the welds which thus eliminated the possibility of brittle fracture. Nevertheless, stress annealing also decreased the maximum hardness.

Some particularly interesting results of this investigation are shown in Fig. 7, which shows three typical fractures. Alongside the fractures are the tensile strength, elongation % in 4 inches and the reduction of area.

The upper photograph is of a specimen with average hardness (about 250 Brinell) in the heat-affected zone, which has been relieved of shrinkage stresses by annealing at 600° C. The specimen behaved like a homogeneous bar of steel with a tensile strength of 86,000 lb./in^2. The fracture was ductile and the elongation was 25 to 35% in 4 inches with considerable reduction of area.

The bar in the middle has the same hardness but has not been stress relieved. The strength is the same as that of the stress annealed specimen, but there is much less elongation and little necking. The fracture is typical of oridinary welds in thick plates made with covered electrodes.

The lower specimen has been inexpertly welded. The hardening has been excessive and an absolutely brittle fracture occurred at a stress of only 58,200 lb./in.^{2}

Clearly these large differences emphasize the importance of a proper type of steel, suitable welding technique and heat treatment. The steel should be selected on the basis of a sensitivity test which, in the future, will reject steels unsuitable for structural welding. New specifications are awaited dealing with welding technique based on up-to-date information. The possibilities of heat treatment in structural welding should be investigated from the operating and economic viewpoints.

CONCLUSIONS

In view of the extensive use of arc welding in structural work in Germany and everywhere else and in view of the occasional difficulties that are encountered, increased attention has been directed to the materials problems in welding the higher tensile steels and to the problem of shrinkage stresses. Structural steels should be selected with strictest attention to their sensitivity to welding for the purpose of discarding all sensitive steels. At the same time the welding must be performed under such conditions that both material factors and shrinkage stress factors will be most favorably related. The most effective means of increasing the quality of a weld is stress annealing, the economic and practical aspects concerning the possibility of which are worthy of research.

[1] Graf, O., Investigations of Shrinkage Stresses in Welded Bridge Girders, *Stahlbau*, **11**, 97–101 (1938).

INTERNAL STRESSES IN CASTINGS*

By CHARLES W. BRIGGS†

INTRODUCTION

THE following article on internal stresses in castings is mostly a review of the published information on the subject. The purpose of publishing the material in this bulletin is to fulfill the desire of the Welding Research Committee to include additional information to the already existing reviews on Distortion and Shrinkage Stresses.[1‡] It is the Committee's opinion that the review lacked information on the stresses that are set up in different cast metals during the course of solidification and the subsequent cooling. The Committee has also become aware that some of the problems of welding are analogous to those encountered in the foundry, notably the problems of shrinkage and internal stresses. Hençe, the article is offered in the hope that from the available information on easting stresses, it will be possible to translate the data to apply to stresses in weld metal.

HISTORICAL REVIEW

There was practically nothing accomplished in a study of casting stresses prior to 1925. It is true that a dozen authors wrote on the subject, and in some cases at considerable length, but there were few tangible ideas expressed.

In 1856 R. Mallet[2] published a rather remarkable monograph on guns and included general remarks on the internal stresses in cast iron as produced in firing guns. This material was, however, not applicable to the subject at hand.

In a paper published in 1897, F. Schumann[3] presented an elementary discussion on internal stresses in cast iron. He asked that greater attention be given to the proper design of castings and stated that the unequal cooling rates existing between various sections were responsible for internal stresses. The author then proceeded to calculate the amount of deformation that would result in various simple shapes.

In 1920, C. Wiltshire[4] told of a method of removing internal stresses in iron castings but he did not consider the formation or magnitude of the stresses. In like manner, A. Fraser,[5] in the same year, discussed stresses in iron castings in a very general way, that added nothing in a quantitative way to the subject.

During 1922, E. Schüz[6] discussed shrinkage measurements on chill cast-iron rolls. He made detailed measurements of the mold cavity and of the cast-iron rolls obtained from such a cavity. He found that in the case of extensively chilled rolls, the contraction amounted to 2.7 per cent, whereas in rolls very lightly chilled, the contraction was only 1.0 per cent. In discussing the casting stresses, the author pointed out that the internal stresses in rolls resulted from the following two causes:

1. That various parts of the roll were at different temperatures at the same time.
2. That gray iron contracts at a different rate from that of white iron.

Commenting on these points, Schüz explained that the rim of the roll, due to the chilling effect, would solidify and begin normal contraction while the center of the roll was still in the molten condition. The roll would then consist of a chilled area at the rim followed by a mottled area, and the author stated that because of the difference in temperature between the zones, the rim would be under tension stress, while the second zone would be in compression. The compression stresses, of course, decrease toward the center or molten area of the roll.

When solidification had proceeded far enough to allow for a third, or a gray iron zone to be formed, then, due to the expansion of gray iron on solidification, the rim zone would be without much stress, the second zone would be in slight tension and the third zone would be under slight compression.

The different rates of contraction between the white and gray iron, and of course, the large temperature gradients present, were responsible for the important stresses located within the casting at room temperature. From this data, Schüz believed that the rim was under a compression stress which was reduced to zero as it extended through the mottled area, and that the gray iron zone was under a tension stress which increased in magnitude toward the center of the roll.

The author attempted to substantiate these deductions by some experiments on rings, which were later cut to show movements by the relieving of the stresses. In most of the cases he found the rings to be under compression stresses.

In the next year, R. Anderson[7] made a study of cracks in aluminum alloy castings and concluded that the formation of cracks in aluminum castings is the result of stresses set up by the contraction in metal volume on passing of the metal from the liquid to the solid state. However, the author did not attempt to prove this statement. He did point out, however, with a fair degree of substantiating material, that alloys having fairly good strength at high temperatures are less likely to crack than those which are very weak, but this property must be correlated with contraction results. This becomes apparent if it is considered that one metal may have substantially greater contraction, thus causing higher stresses to act on the metal. The variations that may exist in the linear contraction of an aluminum alloy were listed as follows:

Alloy	Linear Contraction, %
Aluminum	1.74
95% Al, 3% Cu, 2% Mn	1.52
92% Al, 8% Cu	1.40
88% Al, 12% Cu	1.20
87% Al, 13% Si	1.17

Mr. Anderson also pointed out that cracks caused by stresses at high temperature were intercrystalline. In the following discussion of the paper[7] Mr. Dix offered conclusive evidence to prove that contraction in the solid

* Contribution to Literature Division, Welding Research Committee.
† Technical Adviser, Steel Founders' Society of America, Cleveland, Ohio.
‡ The superior numerals refer to references as listed in the bibliography.

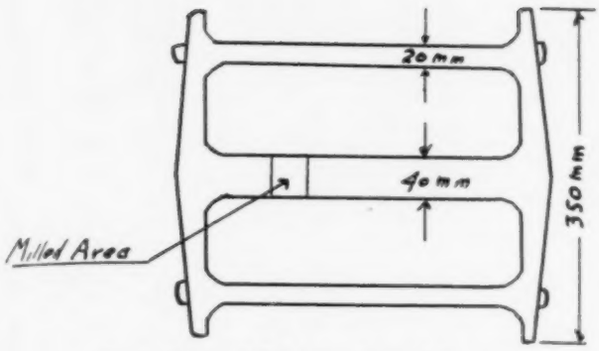


Fig. 1—Casting Made to Test Internal Stress

state, rather than the solidifying contraction, to be the most important stress former.

In 1924, F. Edwards[8] presented a paper on contraction stresses, their cause and remedy. Stress formation, however, was not discussed from the mechanic's standpoint.

The author gives two general causes of the formation of stresses: (1) sudden changes in the form of the casting, which produced changes in the direction of contraction; and (2) differences in the size of the respective sectional areas of parts of the casting, thus giving rise to different rates of cooling.

It may be seen that there was very little information presented prior to 1925 on the mechanics of the formulation of casting stresses. Interest, however, was being aroused, even though the publications consisted of narrative treatments of the subject devoted only to general descriptions of observed conditions.

STRESSES IN CAST IRON

During the last twelve years, most of the literature on the subject of casting stresses has been devoted to the relieving of this stress, and to such subjects as annealing temperatures, holding times and amounts of residual stress. Certain test castings, however, have been studied, and from these a general idea of the nature of the stresses involved can be obtained.

Several cast-iron castings of the design shown in Fig. 1. were produced by Bauer[9] and Sipp.[10] After removing the casting from the sand mold they proceeded to mill the large bar until it broke. A tensile test bar was prepared from the remainder of the bar. The broken portion of the cross-sectional area was then calculated and from this the stress present in the system was obtained by calculation from the formula

$$\rho = F \cdot \alpha_B$$

where

ρ = remaining or residual stress
F = cross section
α_B = tensile strength of the cast iron

By this manner it was found that the casting in the as-cast condition had an internal stress equivalent to 2984 kilograms (6580 lb.)

The cast iron castings were then stress relieved and the authors suggested that a stress relief temperature of 550° C. be used as the residual stress was the lowest at this temperature being 461 kilograms (1015 lb.).

In general, the procedure used by Bauer and Sipp was applied by Machin and Oldham[11] to cast iron, only in this case a wheel design (Fig. 2) was used. Two wheel castings and several test blocks were poured and the castings were allowed to cool to shop temperature before they were removed from the mold. One wheel was cut at places "*AA*" and "*BB*" The "*AA*" cut open 0.026 inches and the "*BB*" cut opened 0.015 inches. In the latter case the stress was partly released due to "*AA*" being cut first. The total area of the section of the spokes was 0.67 square inches and the fractured area was 0.28 square inch. After a tensile test was obtained, calculations showed that the internal stress in the spoke area was 12,020 pounds per square inch or an internal stress value that was 42 per cent of the ultimate tensile strength. These authors likewise investigated stress reduction, and they found that at 650° C. they were able to obtain 100 per cent stress removal.

Two other authors[12,13] discussed internal stresses and performed simple experiments, but they were not as complete as the two previously reported. They did not evaluate the amount of stress present. They also carried on stress-relieving experiments and recommended the adoption of 650° C. as a stress-relieving temperature.

In an article on the determination of stresses in large castings, R. Leonhardt[14] explained that the internal stresses were very complicated in that they may be peripheral stresses, stresses extending in a spiral direction along the outer layers of a solid cylinder, radial and axial stresses, or stresses at an angle to the axis of the specimen. These various stresses may be present singly or simultaneously and may vary in nature and magnitude at different points.

Mr. Leonhardt pointed out that it has been almost impossible to obtain more positive data concerning the nature, magnitude and behavior of internal stresses. He has, however, devised a test wherein a large cylindrical specimen is employed. Bands of varying widths and depths are machined so that layers of the material containing stresses are removed. The stresses remaining in the unmachined portions produce slight alterations in the shape, size and position of these parts of the test specimen. The dimensional and out of line differences recorded before and after machining permit drawing some conclusions as to the nature, direction and intensity of the stresses remaining in the unmachined parts of the test specimen, and the dissipated stresses in the part removed.

A considerable amount of equipment is necessary for a test of this type. A rigid frame to hold the test piece is necessary as is a scribing tool, a comparator with scale, measuring slides, adjustable telescopes and microscope, and a frame for carrying slip gages and caliper gages. The test specimen must also be well polished.

The equipment enables differences in diameter and length to be established to within 0.005 millimeters and permits the loads to be calculated to within a half kilogram per square millimeter.

Such equipment serves only for studying stresses in a

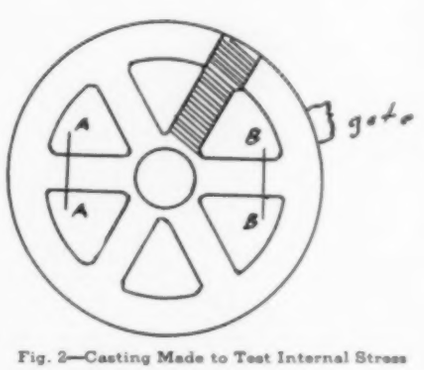


Fig. 2—Casting Made to Test Internal Stress

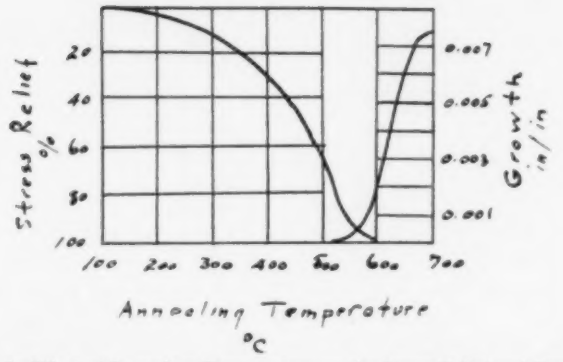


Fig. 3—Effect of Annealing Temperature on the Stress Relief and Growth of Cast Iron

very uniform section such as cylindrical type bodies like cast-iron or cast-steel rolls.

The author presents no information as to the magnitude of stresses that may be encountered in the castings he is studying.

In a recent paper, Benson and Allison[15] consider the relief of internal stress in cast-iron castings. They machined test pieces and deflected them. The specimens were then annealed and the residual stress was calculated from the amount of straightening which occurred. The maximum fiber stresses in the bars were calculated from the following approximate expression based on the elastic theory.

$$f = \frac{\delta E \epsilon}{0.17\, e^2}$$

where

f = maximum fiber stress
δ = deflection of bar measured at mid-length
e = effective length of bar between supports
ϵ = thickness of test bar
E = Young's modulus (12,500,000 lb. per sq. in.)

The method employed is not strictly accurate, as the stress calculations are based on the assumption that stress distribution in the bars is linear, both before and after annealing, which is not necessarily true.

Under the calculations, an initial stress of about 10,000 pounds per square inch was placed upon the bars. The effect of the annealing temperature on the stress relief of cast iron is shown in Fig. 3. This curve shows that the percentage stress relief increased progressively with temperature, and at 550° C. the initial stress has been relieved by 90 per cent. There is very little growth of cast iron at 550° C. Above this temperature, however, a very rapid increase in the rate of growth occurs; thus considering the stress relief and growth together, it appears that annealing at a temperature of 550° C. is the most suitable for cast iron.

INTERNAL STRESSES IN CAST STEEL

In 1927, H. Malzacher[16] discussed certain fundamentals concerning the formation of casting stresses. He pointed out that stresses originate from (1) mold resistance σ_s and (2) from variations of metal thickness, establishing in this way thermal gradients σ_w.

Thus, the total casting stresses $\sigma_g = \sigma_s + \sigma_w$.

In the normal cooling of steel castings σ_s is definitely greater than σ_w.

The author concluded that internal stresses occur primarily because of the existence of unequal amounts of contraction, which result in unequal amounts of permanent deformation of the individual parts of the casting.

At a little later date Korber and Schitzkowski[17] studied hindered contraction and by the use of insert bars in the mold were able to build up stresses of sufficient magnitude that they cracked cast-steel bars that were cooling from the solidifying temperature. They concluded from their study that at temperatures of about 1300° C. hindered contraction could cause steel castings to crack.

Continuing with this general line of development, Briggs and Gezelius[18,19] made a study in which it was their purpose to evaluate the amount of stress that would arise from hindered contraction. This information was collected on cast steel bars that cooled uniformly throughout. The contraction was hindered by means of springs, and the resulting stresses and the amount of contraction were correlated. Graphs were drawn (Fig. 4, 5, 6) showing the temperature-contraction relationship of a freely contracting bar and of bars hindered in their contraction. It will be noticed that the amount of stress present on the casting may be obtained at any temperature.

The data as illustrated in Fig. 6 probably represents more clearly the stresses encountered by the average commercial casting, as the total contraction recorded under this hindered contraction approximates the "patternmaker's shrinkage" of $^3/_{16}$ inch per foot (1.56 per cent). However, conditions as illustrated in Fig. 5, are not infrequent, especially in rangy cored construction. The high stress as represented by the high hindered contraction of Fig. 4 is probably seldom present and when it is, tearing of the casting to relieve this stress takes place.

There are one or two other points that become evident from a study of these contraction stress diagrams. Under the influence of the tension stresses, the amount

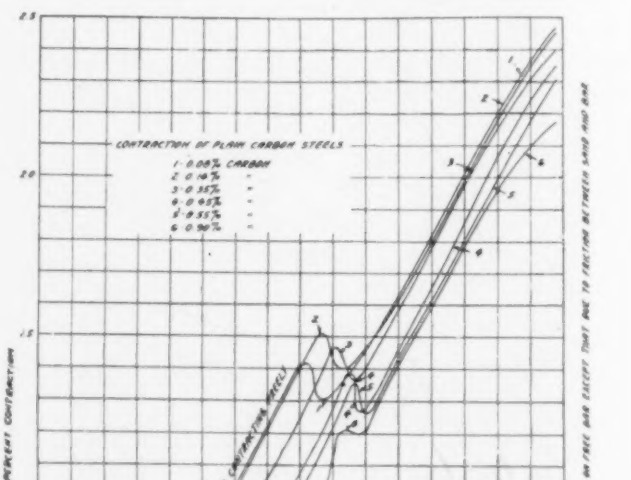


Fig. 4—Contraction of Plain Carbon Steels Free and Heavy "C" Spring

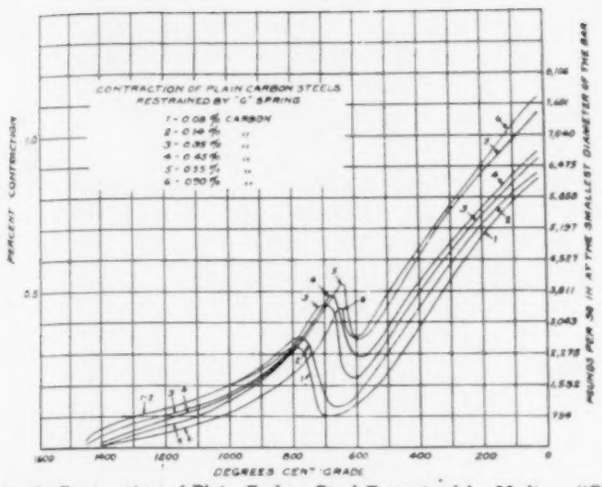

CONTRACTION OF PLAIN CARBON STEELS
RESTRAINED BY "C" SPRING
1 - 0.08 % CARBON
2 - 0.14 % "
3 - 0.35 % "
4 - 0.65 % "
5 - 0.55 % "
6 - 0.90 % "
PERCENT CONTRACTION
DEGREES CENTIGRADE

Fig. 5—Contraction of Plain Carbon Steel Restrained by Medium "C" Spring

of expansion resulting while the casting passes through the critical range is greater than that recorded on a freely contracting bar. Also, the amount of expansion that takes place as the steel passes through the critical range decreases as the carbon content increases. This point is important, for, in a casting of various size sections it is possible that one section may be expanding while another section is contracting due to temperature variations and gradients. This leads to the formation of important stresses which in turn increase, since under stress active systems the amount of expansion becomes greater.

An interesting point of practical importance, that throws some light on the mechanics of crack formation, is brought out by the data obtained on the hindered contraction bars. In the case of the light hindered contraction, where conditions are similar to usual casting conditions, it will be noted that at 1300° C. (2372° F.), the temperature range most susceptible to cracking, the 0.08 per cent carbon bar has contracted 0.18 per cent and has developed a stress of 425 lb. per sq. in., whereas the 0.90 per cent carbon steel has only contracted 0.002 per cent with a corresponding stress of about 50 lb. per sq. in.

This effect is even more noticeable in the case of bars under high hindered contraction. For example, the 0.14 per cent carbon steel at 1300° C. (2372° F.) has contracted 0.006 per cent, which results in a stress of 1200 pound per square inch. At the same temperature, the 0.35 per cent carbon steel has only contracted 0.003 per cent and thus has to resist only a stress of 600 pounds per square inch. However, the stress magnitude is not the only matter of consideration in this case, because it will be subsequently shown from Hall's work[20] that the lower carbon steel has the greater strength and ductility at the very high temperatures.

The work on stress-contraction-temperature relations was continued with various alloy cast steels wherein the carbon content was fixed at 0.35 per cent. In Fig. 7, one of the alloy steel charts is shown. The steel bars in this case are under low and high hindered contraction conditions. It may be seen that the stress conditions in alloy steels are very similar to those of the plain carbon steels.

It should of course be realized that a study of contraction stresses operative at the various high temperatures has no bearing on the ultimate strength of the steel, except that it shows that the slower contracting steels may have a lower yield strength at the higher temperatures, since the bar in contracting could only develop a stress equal to their yield strength. When the yield strength was exceeded the steel yielded, and upon further cooling, and thus further contraction of the steel, the stress is again built up to the new yield strength, due to the fact that the strength properties increase at progressively lower temperatures.

From a study of the data it seems probable, however, that the true yield strength of the steels is not recorded, since the stresses upon the bars at any one temperature vary considerably with the type of contraction. For example, the Manganese-Nickel steel of Fig. 7 at 1200° C., when under low hindered contraction, shows a stress of 472 pounds per square inch, whereas under high hindered contraction, a stress of 885 pounds per square inch is recorded. This would indicate that the rate at which the stress is applied has considerable to do with the amount of stress present at any one temperature under different types of loading. In the case of a low hindered contraction it seems possible that the yield strength is not reached even at the higher temperatures and that only the plastic flow of the metal is recorded.

CONTRACTION STRESSES

It would seem from the foregoing discussion that the primary source of internal stresses in castings results from contraction stresses. It is therefore important that the underlying principles involving contraction stresses should be stated. These may be listed as follows:

1. There are no contraction stresses (acting linearly) in a freely contracting cast bar.
2. In a stressed cast bar the resulting stresses are independent of the length of the bar.
3. Stress magnitudes vary as to the inverse rates of the cross section in which they appear.
4. Internal stresses in a casting are proportional to the modulus of elasticity, to the coefficient of contraction and to the differences of temperature involved.

The above points can be developed mathematically as illustrated in reference.[21]

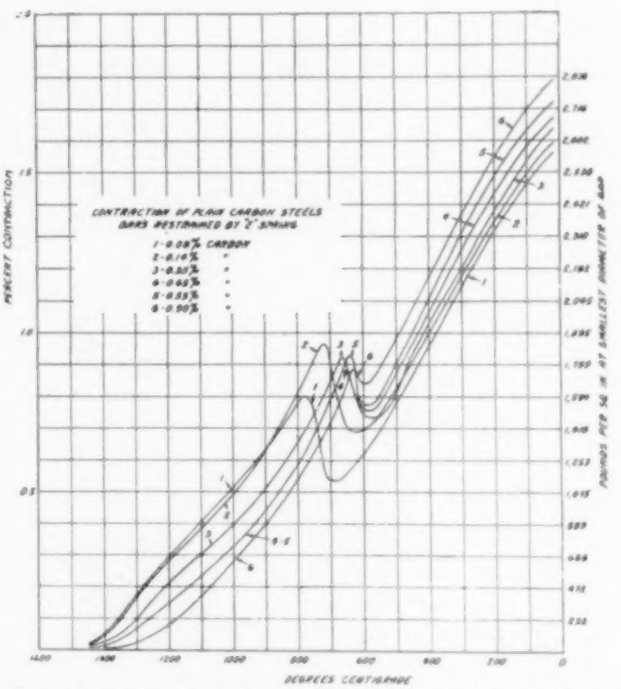


Fig. 6—Contraction of Plain Carbon Steel Restrained by Light "E" Spring

As long as tension and compression stresses acting along the casting produce only elastic deformation, the stresses will be temporary and though the relations may become somewhat involved they can be attacked clearly from the mathematical viewpoint. Complications arise, however, in that a stress system is usually rather complex, since other than linear tension and compression stresses such as circumferential, radial and shear stresses may be developed along the cast member. Also, to make it more complex, when permanent deformation exists then permanent stresses are left in the metal. To exceed the elastic limit of a metal is not a very difficult condition to fulfill, since metal at temperatures approaching melting, has exceptionally low elastic limits, and all deformation taking place is principally plastic. It should be pointed out that there is no clear-cut temperature at which deformations change from the plastic to the elastic state. In fact, experimentation has shown that stresses causing initial yielding at low temperatures decrease about linearly with increasing temperature, but become nearly independent of the temperature when the melting point is approached. Thus, metal crystals have a small, but finite elastic limit at temperatures approaching the melting point.

In the discussion of stresses in castings it should be remembered that besides stresses that are developed because of cross-sectional differences due to temperature variations in the metal, there are mold conditions that can cause similar stress formations. In this case, however, the stresses arise due to the prevention of normal contraction by the rigidity of sand molds and cores. A combination of stresses resulting from differential cooling of metal sections and from the action of the mold hindering conditions may be present simultaneously within the casting. The magnitude of this stress depends on the rate of the stress application, or to state it in another way, the stress depends on the rate of plastic deformation.

$$S = K \frac{dp}{dt}$$

Here S represents stress and p is the plastic deformation, or elongation, or if one prefers, the hindered contraction. The magnitude of p therefore depends on the amount of

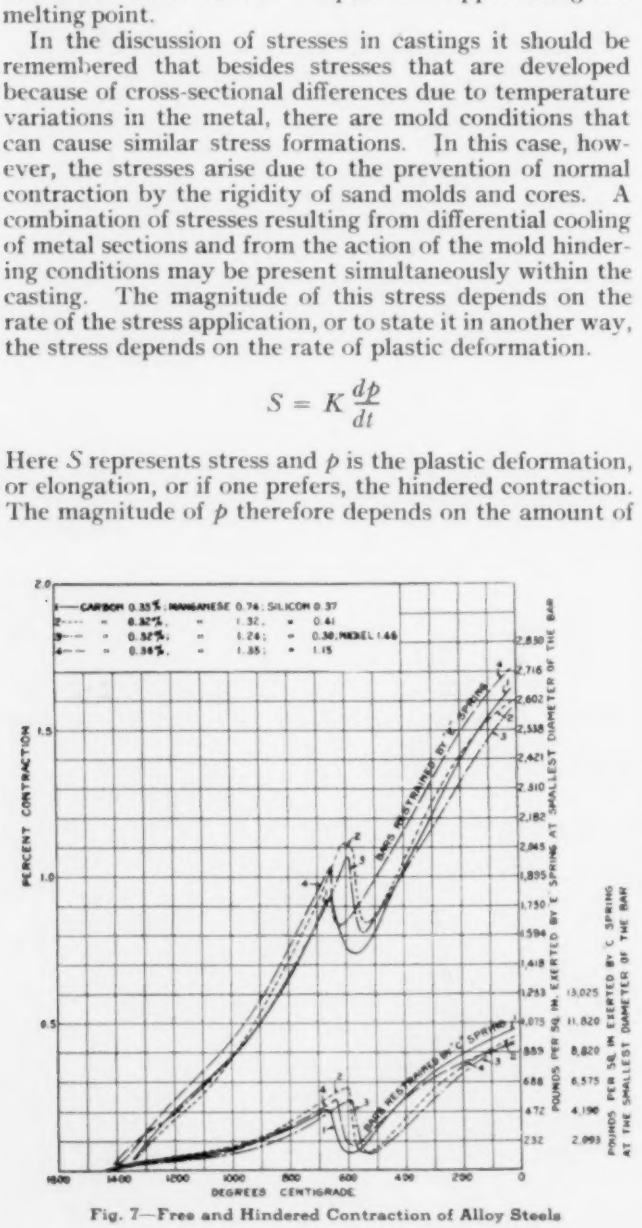

PERCENT CONTRACTION
DEGREES CENTIGRADE
POUNDS PER SQ. IN. EXERTED BY E SPRING AT SMALLEST DIAMETER OF THE BAR
POUNDS PER SQ. IN EXERTED BY C SPRING AT THE SMALLEST DIAMETER OF THE BAR
BARS RESTRAINED BY E SPRING
BARS RESTRAINED BY C SPRING

Fig. 7—Free and Hindered Contraction of Alloy Steels

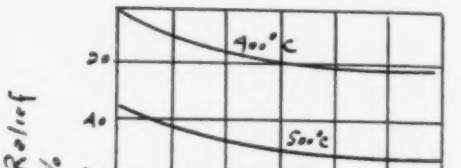


Fig. 8—Effect of Annealing Period on Stress Relief of Steel at Different Annealing Temperatures (Benson and Allison)

stress acting on the casting and the time on which it is effective. Plastic flow of a metal occurs only as long as there is a force acting on it and the rate at which it takes place depends on the magnitude of the force. Thus, when metal is cast under conditions that tend toward high resistance to contraction, the magnitude of the stresses acting on the casting is high, the rate at which they build up is fast, and the amount of plastic deformation that takes place is large. If, however, the resistance to contraction is not high then the other factors bearing on it will be correspondingly lower.

The above material sets forth some of the salient features of plastic deformation as arising from contraction stresses. It should be added that if at any time the cooling cycle of the stressed casting could be arrested at some high temperature, the magnitude of the stress would be progressively less with time, since plastic deformation of the metal would reduce the stress to zero. From this it can be seen that the cooling rate of the metal is important in considering the magnitude of the stress. In two hindered contracting castings, acting under like conditions, except for the cooling rate, the fastest cooled casting will build up the highest stresses.

STRESS CENTRALIZATION

All abrupt changes in section, sharp corners, casting irregularities and hot spots due to temperature gradients, are potential positions of stress concentration. When stresses arising from hindered contraction of a large casting concentrate at one point, it is easy to see that stresses may build rapidly. It is because of this condition of stress centralization that many foundrymen have looked skeptically at the statement made previously that contraction stresses arising in a casting are independent of the casting length since they are able to point to the fact that many long castings fail, while short castings do not. This failure in long castings is not the result of greater stresses developed because of their length, but it is due to the fact that these stresses may centralize in one place due to hot spots, sharp corners, etc., and the combined stresses centralized in a long casting will be much greater than the combined stresses centralized in a short casting.

There are two other points that should be discussed: (1) The magnitude of the stresses at high temperature where tearing takes place and (2) the magnitude of stresses at room temperature.

MAGNITUDE OF STRESSES AT HOT TEARING TEMPERATURES

Probably the most serious defects with which the casting manufacturer must occasionally contend are hot

tear cracks. With regard to steel, these tears form in the region of about 1300° C. (2372 Fahrenheit). If the properties of the steel at this temperature were known, then some approximation could be made of the magnitude of the stresses operating at this temperature to form hot tears.

Data on the strength and ductility of steel at temperatures near the melting point has been studied in a preliminary way by H. Hall.[20] He found that the ultimate stress necessary to cause failure in one inch bars averaged, for cast carbon steel, from 1700 pounds per square inch at 1300° C. to 2500 pounds per square inch at 1250° C.

A stress of 2000 pounds per square inch is quite high if the stress is to be presumably developed by hindered contraction. Briggs and Gezelius found that under the most drastic conditions of hindered contraction the amount of stress developed on a bar of 2 square inches was 700 pounds per square inch at 1300° C. or 1000 pounds per square inch at 1250° C. This would indicate that the stresses developed were not sufficient to cause failure, nevertheless hot tears do occasionally occur. The realization of this fact leaves two points for consideration; either the critical cracking temperature has been selected at too low a temperature, or else stress centralization is responsible for the apparent discrepancies, so that tearing does occur when most of the casting cools to the 1250–1300° C. range while hot spots in the casting are at a much higher temperature. It is in these hot spots of course, that the cracking occurs.

Thus, it is possible that small tension stresses of 500 to 1000 pounds per square inch as developed by contraction are responsible for hot tear formation.

Tearing in some cases takes place when the responsible stresses are even lower than that which appears to be necessary to cause failure by tension. It is often evident that failure has been due, from the very nature of conditions involved, to shear stresses. This would indicate that steel at these very high temperatures has very little resistance to shear stresses; in fact, much less resistance than it has to tension stresses. Thus, the tension properties of the steel at high temperatures may not show the magnitude of the stresses causing hot tear formation. It, therefore, appears that the entire problem of stress magnitude is undoubtedly tied up with (1) the rate of stress application, (2) the centralization of stresses, and (3) the nature of the acting stress.

MAGNITUDE OF STRESSES AT ROOM TEMPERATURE

A very definite statement of the magnitude of stresses that may be found in steel castings under hindered contraction has been presented by Briggs and Gezelius.[18] Consequently if a casting contracts only one per cent instead of the free contraction of 2.4 per cent, a stress of 7000 pounds per square inch may be present on the casting from hindered contraction.

The casting may also be under stresses the magnitude of which may be any figure and may mount as high as the ultimate tensile strength of the steel. In this case the residual stresses are caused by unequal cooling of the metal sections. One section may be expanding during the critical range, while another section is contracting. This may mean a difference of as much as 0.3 per cent movement. A resistance to a movement of 0.1 per cent would be equivalent to a stress equal to the elastic limits at room temperature conditions.* It thus may be seen that high residual stresses may be developed.

Two papers have recently been written on stress relief of steel castings.[11,15] In general they reported that stressed cast steel specimens could be stress relieved 90 to 95 per cent by annealing at 600° C. for six hours for a $^1/_2$ inch square test specimen (Fig. 8). They also reported that a relatively considerable measure of stress relief has occurred with no soaking period at all, that is, just heating up to the annealing temperature.

Briefly, the above information on internal stresses may be summarized as follows:

1. Contraction stresses, arising from hindered contraction of mold and metal section, are the primary internal stresses that are formed.
2. The magnitude of these stresses has been studied in a preliminary way at high temperatures as well as at room temperature conditions.
3. Temperature gradients in the metal sections are responsible for stresses of a large magnitude.
4. It is possible by annealing below the critical range to reduce the residual internal stress to a very low figure.

BIBLIOGRAPHY

1. Spraragen, W. and Claussen, G., "Shrinkage Stresses in Welding—A Review of the Literature to Jan. 1, 1937," The Engineering Foundation, Welding Research Committee, August 1, 1937.
2. Mallet, R., *Trans. Roy. Irish Academy*, **23**, Pt. 1, Science 141–436 (1856).
3. Schumann, F., *Trans. American Society Mechanical Engineers*, **18**, 394–414 (1897).
4. Wiltshire, C. J., *Trans. Am. Foundrymen's Assoc.*, **28**, 346–347 (1920).
5. Fraser, A., *Foundry Trade Journal*, **21**, 126–130 (1920).
6. Schüz, E., *Stahl u. Eisen*, **42**, 1773–1781 (1922).
7. Anderson, R. J., *Trans. A.I.M.E.*, **68**, 833–854 (1923).
8. Edwards, F. C., *Foundry Trade Journal*, **30**, 519–522 (1924).
9. Bauer, E., *Giess. Ztg.*, **23**, 61–73, 95–102, 121–128 (1926).
10. Bauer, O. and Sipp, K., *Giesserei*, **23**, 253–256 (1936).
11. Machin, W. and Oldham, M., *Foundry Trade Journal*, 343–348 (April 30, 1938).
12. Le Thomas, A., *Foundry Trade Journal*, **39**, 414–415 (1928).
13. Piwowarsky, E., *Hochwertige Grauguss*, Springer, Berlin, 167–178 (1929).
14. Leonhardt, R. W. P., *Machinery* (London), **44**, 705–707 (1934).
15. Benson, L. E. and Allison, H., *Foundry Trade Journal*, 527–528, 543–544 (June 23 and June 30, 1938).
16. Malzacher, H., *Stahl u. Eisen*, **47**, 2108–2112 (1927).
17. Körber, F. and Schitzkowski, G., *Stahl u. Eisen*, 129–135 (1928).
18. Briggs, C. and Gezelius, R., *Trans. Am. Foundrymen's Assoc.*, 385–448 (1934).
19. Briggs, C. and Gezelius, R., *Trans. Am. Foundrymen's Assoc.*, 1–32 (1936).
20. Hall, H., *Spec. Report No.* **18**, *Iron & Steel Inst.*, 65–94 (1936).
21. Briggs, C., *Trans. Am. Foundrymen's Assoc.*, 61–81 (1937).

* Based on an assumed elastic limit of 30,000 pounds per square inch and a modulus of elasticity of 30 million pounds.

THE FAILURE OF GIRDERS UNDER Repeated Stresses*

By PROF. FREDERICK CHARLES LEA, O.B.E., D.Sc. (ÈNG.), M. INST. C.E., and JOHN GWYNNE WHITMAN, M.ENG.

INTRODUCTION

IN TWO recent papers[1,2] the results of experiments on the effect of repeated stresses on structural elements have been given. In the second of these papers the results of preliminary experiments on structural elements in the form of rolled steel joists tested in a special machine, called the "girder-fatigue testing-machine," were given, together with a description of the machine.

This paper describes tests on the complete series of girders, consisting of the following:

1. Series A. Rolled mild-steel joists, 5 inches by 3 inches by 11 lb.
2. Series A1. Rolled mild steel joists, 5 inches by 3 inches by 11 lb., with two $^9/_{16}$-inch diameter rivet-holes in the tension-flange (as shown in Fig. 3).
3. Series B. Rolled mild-steel joists, 5 inches by 3 inches by 11 lb. with a butt-welded joint at mid-span.
   Series B1. Rolled mild-steel joists, 5 inches by $2^1/_2$ inches by 9 lb., with a butt-welded joint at mid-span.
4. Series D. Rolled mild-steel joists, 5 inches by 3 inches by 11 lb., with a fillet-welded joint at mid-span. Cover-plates were fillet welded on the flanges and on the web.
5. Series E. Rolled mild-steel joists, 5 inches by 3 inches by 11 lb., with a joint at mid-span. Cover plates were riveted on the flanges and on the web.
6. Series C. Built-up welded girders, 5 inches by 3 inches, consisting of web plates fillet welded to the flange plates. These were tested as welded.
7. Series C1. Similar girders to those of Series C, but tested after heat treatment at a temperature of 650° C.

These specimens were tested on a span of 7 feet 6 inches, and were so loaded that a length of 12 inches in the center was subjected to constant bending moment. All tests were run with zero minimum stress. A heavily-coated extruded electrode $^1/_8$ inch diameter was used at 120–130 amps. Typical test-results for all welded specimens were as follows:

Ultimate tensile strength	71,500 lb./in.^{2}
Yield-point	57,500 lb./in.^{2}
Elongation on 8 diameters	19.5 per cent
Reduction of area	52.0 per cent

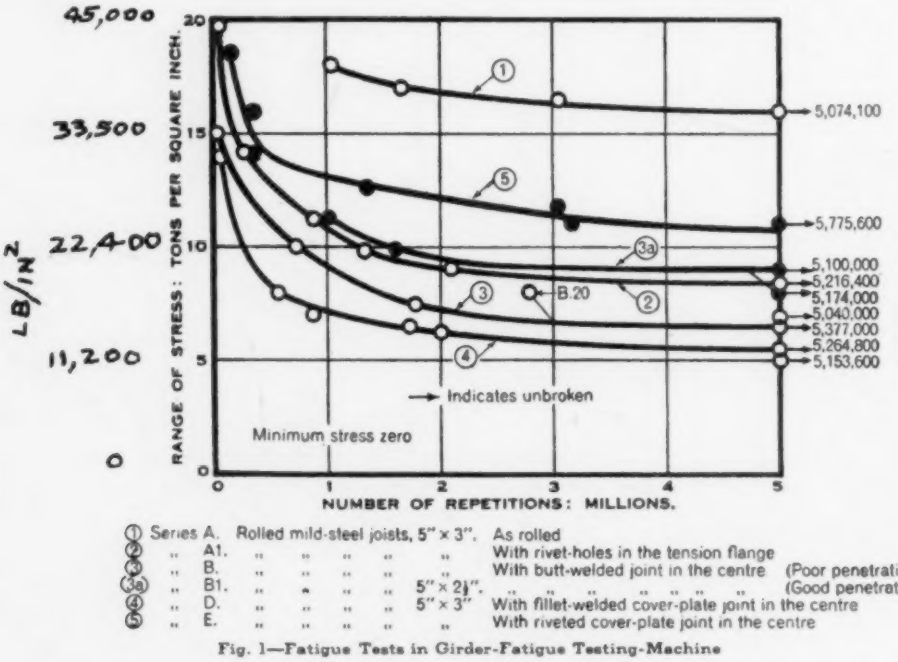
Specimens used for these results were not heat treated after welding.

TESTS OF ROLLED MILD-STEEL JOISTS: SERIES A

The mild steel joists were all rolled at the same time, and with them were supplied six short lengths from which the section modulus of the girders was carefully determined. The mean value of the section modulus was 5.58 inch^3 units, and that of the second moment of area was 14.40 inch^4 units.

Fatigue Tests

The results of the tests are shown in Table 1 and Fig. 1 (curve No. 1). The fractures were of the usual fatigue type. The girder A.8 cracked in the center of the tension flange.



① Series A. Rolled mild-steel joists, 5" × 3". As rolled
② " A1. " " " " " With rivet-holes in the tension flange
③ " B. " " " " " With butt-welded joint in the centre (Poor penetration)
③a " B1. " " " " 5" × 2½". " " " " " " " (Good penetration)
④ " D. " " " " 5" × 3" With fillet-welded cover-plate joint in the centre
⑤ " E. " " " " " With riveted cover-plate joint in the centre

Fig. 1—Fatigue Tests in Girder-Fatigue Testing-Machine

* Abstract of paper presented before Institution of Civil Engineers, Great Britain.

[1] Lea, F. C., "Repeated Stresses on Structural Elements," *Journal Inst. C.E.*, **4** (1936–37), p. 93, November 1936.

[2] Lea, F. C., and Whitman, J. G., "The Failure of Girders Under Repeated Stresses," *Ibid.*, **7** (1937–38), p. 119, November 1937.

Tensile Test

Two tensile specimens were turned from the flanges of girder A.8; the results of the tests are shown in Table 2.

Static Test

A static bending test was carried out on the plain girder A.4. The load-deflection diagram from the test is shown in Fig. 2; the girder failed by buckling in the compression flange. The maximum load carried is shown in Fig. 2, and the apparent crippling stress, calculated from the formula $f = \frac{M}{Z}$, in Table 8.

Table 1—Fatigue Tests on Mild-Steel Joists 5 Inches by 3 Inches by 11 Lb., as Rolled. Series A

Specimen	Range of Stress, Lb. per Square Inch	Number of Repetitions	Remarks
A.8	40,300	1,042,900	Small amount of creep, broken
A.7	35,800	5,074,100	No creep, unbroken
A.2	37,000	3,427,000	Very slight initial creep, broken
A.3	38,100	1,653,100	Small amount of initial creep; broken ¾ inch outside length of constant bending

The fatigue limit from zero minimum stress for more than 5×10^6 repetitions is 35,800 lb. per square inch.

Table 2—Tensile Tests on Two Specimens Cut from A.8 (Gage-Length 2 Inches)

Specimen	Diameter, Inches	Yield-point: Lb. per Square Inch	Ultimate Strength Lb. per Square Inch	Elongation Per Cent	Reduction of Area Per Cent
1	0.550	38,700	62,500	42.5	65
2	0.543	39,300	62,700	42	64

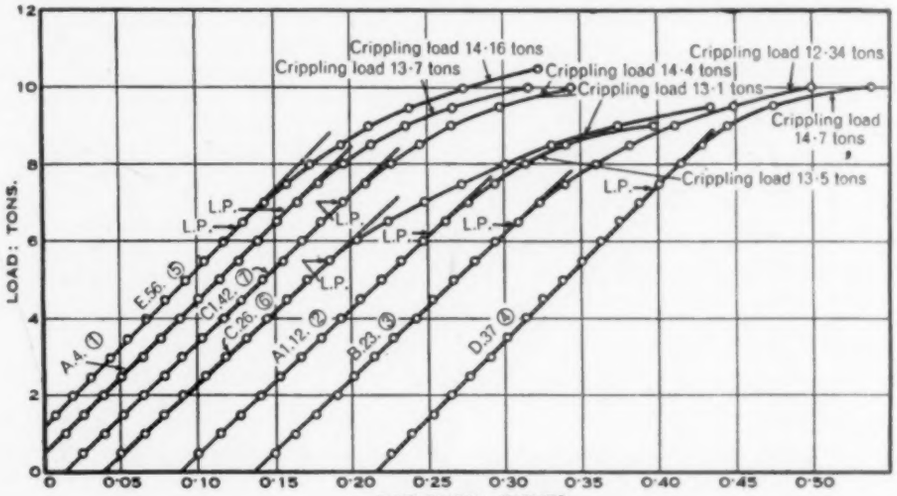
TESTS OF ROLLED MILD-STEEL JOISTS WITH A BUTT-WELDED JOINT IN THE CENTER: SERIES B AND B1

Series B

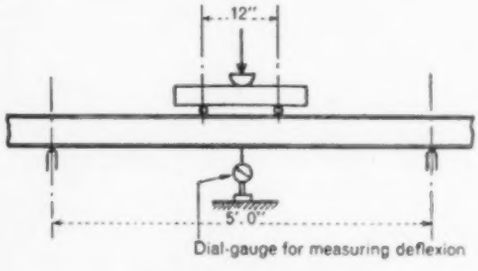
The specimens consisted of two half-girders, 5 inches by 3 inches by 11 lb., butt-welded together. Details of the welded joint are shown in Fig. 3.

Fatigue Tests

Results of the tests are shown in Table 3 and Fig. 1. The stresses quoted are maximum stresses calculated on the cross section of the girder; the actual section through the weld is not easy to determine. A typical fracture shows that there is a considerable lack of penetration, particularly at the root of the flange. There is little



Specimen Tested	Limit of proportionality: tons per square inch. On section of girder	Limit of proportionality: tons per square inch. On net section	Apparent crippling stress: tons per square inch. On section of girder	Apparent crippling stress: tons per square inch. On net section at failure
① A.4. Mild-steel joist. 5″ × 3″ × 11 lb. As rolled	14·6	—	29·5	—
② A1.12. As A.4, but with rivet-holes in the tension-flange	13·3	18·9	29·0	41·1
③ B.23. " " " " butt-welded joint at mid-span	13·7	—	26·6	—
④ D.37. " " " " fillet-welded cover-plate joint at mid-span	15·9	—	31·6	—
⑤ E.56. " " " " riveted cover-plate joint at mid-span	13·7	20·8	30·5	46·0
⑥ C.26. Built-up welded girder. 5″ × 3″. No heat-treatment	11·8	—	28·0	—
⑦ C1.42 " " " " " Heat-treated at 650° C.	14·9	—	29·8	—



LOADING ARRANGEMENT.

Fig. 2—Static Load-Deflection Tests

THE FAILURE OF GIRDERS UNDER Repeated Stresses*

By PROF. FREDERICK CHARLES LEA, O.B.E., D.Sc. (ENG.), M. INST. C.E., and JOHN GWYNNE WHITMAN, M.ENG.

INTRODUCTION

IN TWO recent papers[1,2] the results of experiments on the effect of repeated stresses on structural elements have been given. In the second of these papers the results of preliminary experiments on structural elements in the form of rolled steel joists tested in a special machine, called the "girder-fatigue testing-machine," were given, together with a description of the machine.

This paper describes tests on the complete series of girders, consisting of the following:

1. Series A. Rolled mild-steel joists, 5 inches by 3 inches by 11 lb.
2. Series A1. Rolled mild steel joists, 5 inches by 3 inches by 11 lb., with two $^{9}/_{16}$-inch diameter rivet-holes in the tension-flange (as shown in Fig. 3).
3. Series B. Rolled mild-steel joists, 5 inches by 3 inches by 11 lb. with a butt-welded joint at mid-span.
   Series B1. Rolled mild-steel joists, 5 inches by $2^{1}/_{2}$ inches by 9 lb., with a butt-welded joint at mid-span.
4. Series D. Rolled mild-steel joists, 5 inches by 3 inches by 11 lb., with a fillet-welded joint at mid-span. Cover-plates were fillet welded on the flanges and on the web.
5. Series E. Rolled mild-steel joists, 5 inches by 3 inches by 11 lb., with a joint at mid-span. Cover plates were riveted on the flanges and on the web.
6. Series C. Built-up welded girders, 5 inches by 3 inches, consisting of web plates fillet welded to the flange plates. These were tested as welded.
7. Series C1. Similar girders to those of Series C, but tested after heat treatment at a temperature of 650° C.

* Abstract of paper presented before Institution of Civil Engineers, Great Britain.

[1] Lea, F. C., "Repeated Stresses on Structural Elements," *Journal Inst. C.E.*, **4** (1936–37), p. 93, November 1936.

[2] Lea, F. C., and Whitman, J. G., "The Failure of Girders Under Repeated Stresses," *Ibid.*, **7** (1937–38), p. 119, November 1937.

These specimens were tested on a span of 7 feet 6 inches, and were so loaded that a length of 12 inches in the center was subjected to constant bending moment. All tests were run with zero minimum stress. A heavily-coated extruded electrode $^{1}/_{8}$ inch diameter was used at 120–130 amps. Typical test-results for all welded specimens were as follows:

Ultimate tensile strength	71,500 lb./in.^{2}
Yield-point	57,500 lb./in.^{2}
Elongation on 8 diameters	19.5 per cent
Reduction of area	52.0 per cent

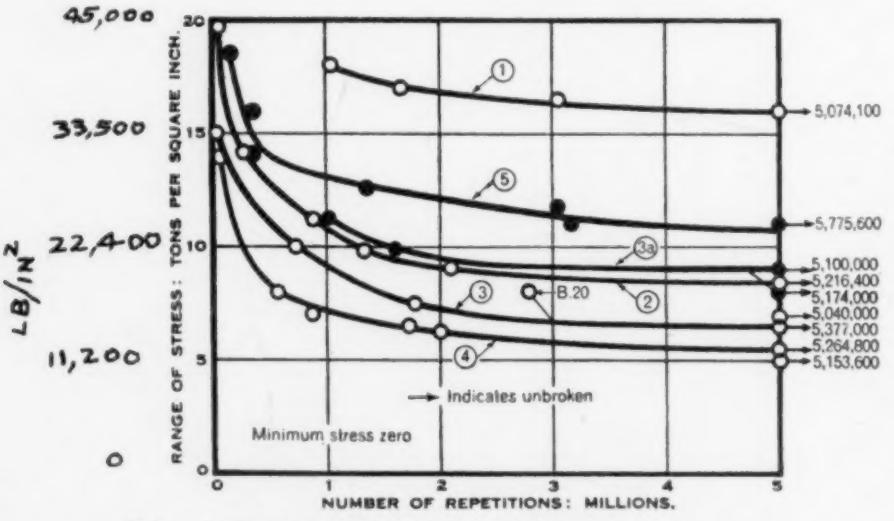
Specimens used for these results were not heat treated after welding.

TESTS OF ROLLED MILD-STEEL JOISTS: SERIES A

The mild steel joists were all rolled at the same time, and with them were supplied six short lengths from which the section modulus of the girders was carefully determined. The mean value of the section modulus was 5.58 inch^3 units, and that of the second moment of area was 14.40 inch^4 units.

Fatigue Tests

The results of the tests are shown in Table 1 and Fig. 1 (curve No. 1). The fractures were of the usual fatigue type. The girder A.8 cracked in the center of the tension flange.



① Series A. Rolled mild-steel joists, 5" × 3". As rolled
② " A1. " " " " " With rivet-holes in the tension flange
③ " B. " " " " " With butt-welded joint in the centre (Poor penetration)
③a " B1. " " " " 5" × 2½". " " " " " " " (Good penetration)
④ " D. " " " " 5" × 3" With fillet-welded cover-plate joint in the centre
⑤ " E. " " " " " With riveted cover-plate joint in the centre

Fig. 1—Fatigue Tests in Girder-Fatigue Testing-Machine

Tensile Test

Two tensile specimens were turned from the flanges of girder A.8; the results of the tests are shown in Table 2.

Static Test

A static bending test was carried out on the plain girder A.4. The load-deflection diagram from the test is shown in Fig. 2; the girder failed by buckling in the compression flange. The maximum load carried is shown in Fig. 2, and the apparent crippling stress, calculated from the formula $f = \frac{M}{Z}$, in Table 8.

Table 1—Fatigue Tests on Mild-Steel Joists 5 Inches by 3 Inches by 11 Lb., as Rolled. Series A

Specimen	Range of Stress, Lb. per Square Inch	Number of Repetitions	Remarks
A.8	40,300	1,042,900	Small amount of creep, broken
A.7	35,800	5,074,100	No creep, unbroken
A.2	37,000	3,427,000	Very slight initial creep, broken
A.3	38,100	1,653,100	Small amount of initial creep; broken ¾ inch outside length of constant bending

The fatigue limit from zero minimum stress for more than 5×10^6 repetitions is 35,800 lb. per square inch.

Table 2—Tensile Tests on Two Specimens Cut from A.8 (Gage-Length 2 Inches)

Specimen	Diameter, Inches	Yield-point: Lb. per Square Inch	Ultimate Strength Lb. per Square Inch	Elongation Per Cent	Reduction of Area Per Cent
1	0.550	38,700	62,500	42.5	65
2	0.543	39,300	62,700	42	64

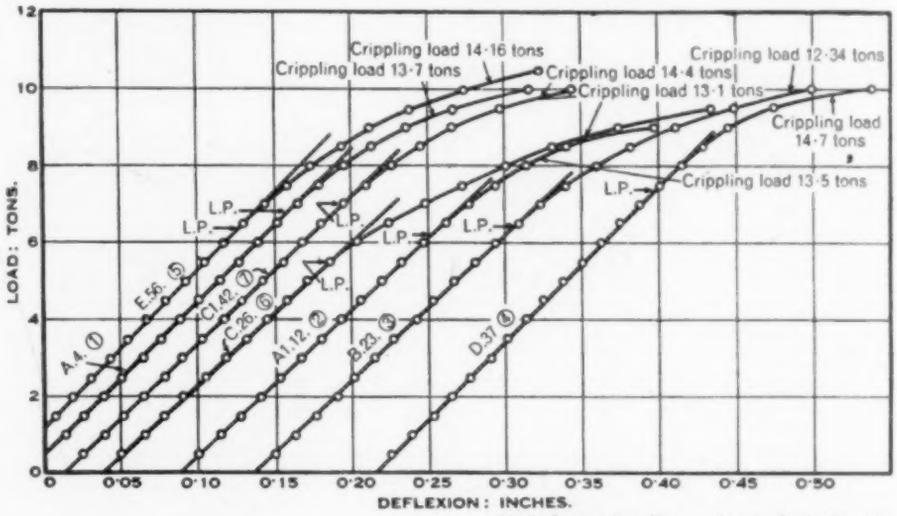
TESTS OF ROLLED MILD-STEEL JOISTS WITH A BUTT-WELDED JOINT IN THE CENTER: SERIES B AND B1

Series B

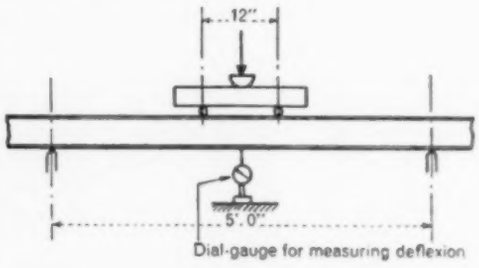
The specimens consisted of two half-girders, 5 inches by 3 inches by 11 lb., butt-welded together. Details of the welded joint are shown in Fig. 3.

Fatigue Tests

Results of the tests are shown in Table 3 and Fig. 1. The stresses quoted are maximum stresses calculated on the cross section of the girder; the actual section through the weld is not easy to determine. A typical fracture shows that there is a considerable lack of penetration, particularly at the root of the flange. There is little



Specimen Tested	Limit of proportionality: tons per square inch. On section of girder	Limit of proportionality: tons per square inch. On net section	Apparent crippling stress: tons per square inch. On section of girder	Apparent crippling stress: tons per square inch. On net section at failure
① A.4. Mild-steel joist. 5" × 3" × 11 lb. As rolled	14·6	—	29·5	—
② A1.12. As A.4, but with rivet-holes in the tension-flange	13·3	18·9	29·0	41·1
③ B.23. " " " " butt-welded joint at mid-span	13·7	—	26·6	—
④ D.37. " " " " fillet-welded cover-plate joint at mid-span	15·9	—	31·6	—
⑤ E.56. " " " " riveted cover-plate joint at mid-span	13·7	20·8	30·5	46·0
⑥ C.26. Built-up welded girder. 5" × 3". No heat-treatment	11·8	—	28·0	—
⑦ C1.42 " " " " " Heat-treated at 650° C.	14·9	—	29·8	—



LOADING ARRANGEMENT.

Fig. 2—Static Load-Deflection Tests

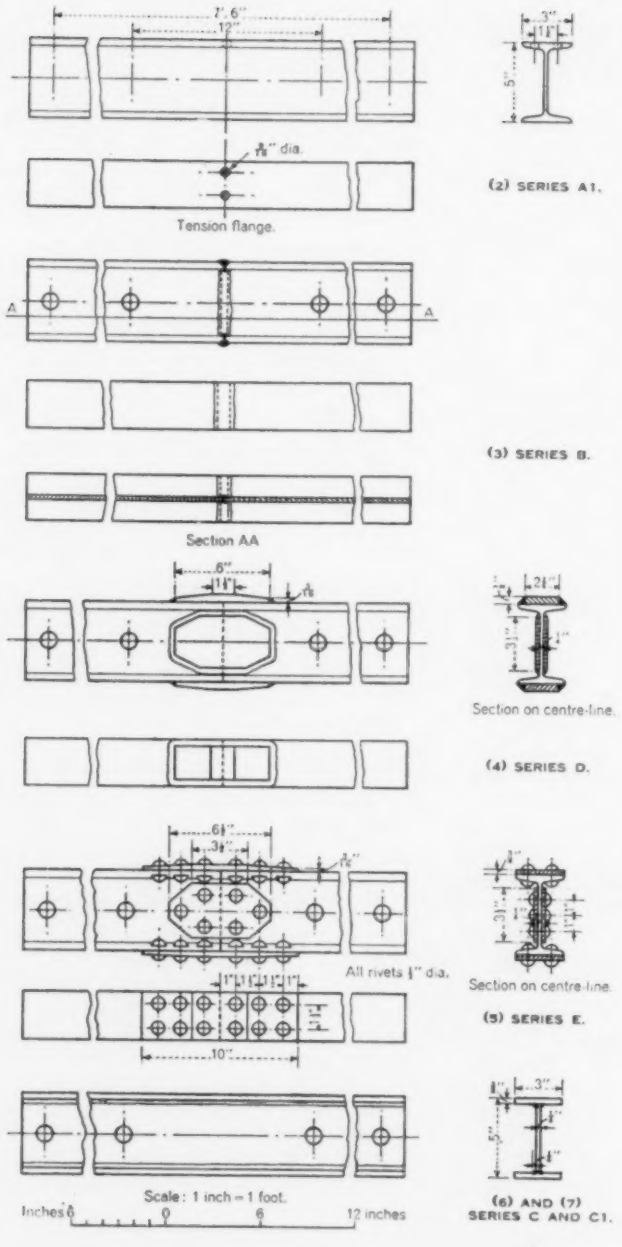


Fig. 3—Sections Tested in Girder-Fatigue Testing-Machine

doubt that the fractures have started at some point of the discontinuity due to this lack of penetration, and this probably accounts for the low result obtained of 14,600 lb. per square inch from zero minimum stress.

It will be noticed that the plotted result (Fig. 1) of the test of girder B.20 lies above the line drawn through the other plotted points. The crack in this girder commenced at the junction of the weld and flange at the top of the flange.

The results of the tests from this series are much lower than those obtained from tests of butt-welded plates, to which reference is made in the conclusions, the reason being the lack of penetration to which reference has already been made. The flange- and web-faces of joint B were chamfered by the workmen to a uniform depth and the two halves of the girder brought together so that there was a considerable area of contact. Weld metal did not penetrate into these areas.

Table 3—Fatigue Tests of Joists 5 Inches by 3 Inches by 11 Lb., with Butt-Welded Joint. Series B. Minimum Stress Zero

Specimen	Range of Stress: Lb. per Square Inch	Number of Repetitions	Remarks
B.18	33,600	24,900	Broken
B.17	22,400	707,000	Broken
B.20	18,200	2,780,400	Broken
B.19	16,800	1,775,800	Broken
B.24	14,600	5,377,000	Unbroken
B.22	15,700	5,040,000	Broken

The fatigue-limit from zero minimum stress for more than 5 × 10^6 repetitions is 14,600 lb. per square inch.

All specimens broke through the weld except B.20, which broke partly through the weld and partly at the junction of the weld and the plate.

Series B1

A further batch of 5-inch by 2½-inch by 9-lb. joists was butt welded, and definite instructions were given as to the way in which the weld was to be made. A gap was left between the vertical faces, and to allow for the varying thickness of the flange the chamfer near the center was made deeper than at the edges. Before testing, the top layer of weld metal was filed flush with the flange of the joists.

Fatigue Tests

The results of this series of tests are shown in Table 4 and Fig. 1. It will be seen that the fatigue-range has been increased from 14,600 to 20,200 lb. per square inch for 5 × 10^6 repetitions, and from 16,400 to 21,500 lb. per square inch for 2 × 10^6 repetitions. Fracture commenced in these tests at the junction of the weld and the joist near the edge of the flange.

Table 4—Fatigue Tests of Joists 5 Inches by 2-1- Inches by 9 Lb., with Butt-Welded Joint. Series B1. Minimum Stress Range

Specimen	Range of Stress, Lb. per Square Inch	Number of Repetitions	Remarks
B1.17	22,200	1,620,000	Broken at junction of weld and flange
B1.18	17,700	5,174,000	Unbroken
B1.19	202,00	5,100,000	Unbroken
B1.20	25,800	1,037,000	Broken at junction of weld and flange

The fatigue-limit from zero minimum stress for more than 5 × 10^4 repetitions is 20,200 lb. per square inch.

Static Test

A static bend-test was carried out on girder B.23. The results of the test are shown in Fig. 2. In spite of the comparatively large unwelded sectional area, there was no sign of a crack and the girder failed by buckling of the compression flange. The static test did not indicate the character of the weld. The maximum load carried is shown in Fig. 2, and the apparent crippling stress is shown in Table 8.

TESTS OF ROLLED MILD-STEEL JOISTS WITH A FILLET-WELDED COVER-PLATE JOINT IN THE CENTER: SERIES D

The specimens consisted of two half-girders joined together by cover-plates fillet welded on the flanges and on

the web, as shown in Fig. 3. The stresses quoted are maximum stresses taken on the cross section of the girder.

Fatigue Tests

The results of the tests are shown in Table 5 and Fig. 1

Table 5—Fatigue Tests of Joists 5 Inches by 3 Inches by 11 Lb., with Fillet-Welded Cover-Plate Joint. Series D. Minimum Stress Zero

Specimen	Range of Stress, Lb. per Square Inch	Number of Repetitions	Remarks
D.36	17,900	545,800	Broken
D.34	14,600	1,709,400	Broken
D.35	13,900	1,999,200	Broken*
D.33	11,200	5,153,600	Unbroken
D.32	12,300	5,264,800	Unbroken
D.40	15,700	860,700	Broken

The fatigue limit from zero minimum stress for more than 5 × 10^6 repetitions is 12,300 lb. per square inch.

* All specimens fractured at the junction of the tension flange and the transverse fillet weld except D.35, which broke through the center of the cover plate.

(curve No. 4). All the fractures except one occurred at the junction of the transverse fillet weld and flange at either end of the cover plate. In one case the fracture occurred through the center of the cover plate, starting in one of the longitudinal fillet welds at the junction of the two half-joists and working through the top and web cover plates. It is of interest to note that the results show that the discontinuity occurring at the transverse fillet weld on the flange is as serious as the discontinuity where the joist is cut.

Static Test

A static bend test was carried out on girder D.37; the girder failed by buckling in the compression flange, and there was some plastic flow at the junction of tension flange and cover plate. The load deflection curve and the maximum load carried are shown in Fig. 2 and the apparent crippling stress in Table 8.

TESTS ON WELDED GIRDERS: SERIES C

Tests were made on girders, Fig. 3, made by fillet welding the web plate to the flange plates.

No heat treatment was given after welding. The flange plates were tacked to the web plate with a series of tacks approximately 2 inches long and were finally welded with a fillet weld.

Determination of Section-Modulus

After each test the size of the specimen at fracture was ascertained by measurement with micrometer gages. In the case of unbroken specimens the minimum section was measured; $^1/_4$-inch 45-degree fillet welds were assumed, and the second moment of area and the section-modulus were calculated by dividing the section into geometric figures.

Fatigue Tests

Results of the tests are shown in Table 6 and in Fig. 4 (curves Nos. 6*a* and 6*b*). All the failures occurred in

Table 6—Fatigue Tests on Welded Joists. Series C. No Heat Treatment

Specimen	Second Moment of Area: Inch⁴ Units	Range of Stress: Lb. per Square Inch At Top Surface of Girder	At Junction of Fillet and Flange	Number of Repetitions	Remarks
C.29	14.16	32,500	28,200	695,300	Broken, slight creep
C.30	14.08	28,900	25,100	783,200	No creep, broken over one of the loading points
C.25	14.02	24,800	21,500	3,756,500	Broken, no creep, scale-inclusion at fracture
C.32	14.16	22,800	20,200	5,090,400	Unbroken, no creep
C.31	14.03	25,500	22,200	1,159,000	Broken, no creep, blowhole at fracture
C.28	13.95	26,900	22,800	1,195,900	Broken, no creep

The fatigue-limit, reckoned at the surface of the top flange, from zero minimum stress for more than 5 × 10^6 repetitions is 10.5 tons per square inch. The corresponding fatigue-limit at the top of the fillet-weld where the crack is believed to have commenced is 20,200 lb. per square inch.

the length subjected to constant bending moment. The fracture at an early stage of girder C.29 shows that the crack is more open underneath the tension flange than on top of the flange, suggesting that the crack started in the fillet weld and proceeded through the flange. There seems no doubt that the fracture in girder C.30 started at the fillet weld.

The fracture of girder C.25 showed a small scale inclusion in one of the fillet welds. This specimen broke at 3,756,000 repetitions. The plotted point is not inconsistent with the other plotted points, which suggests that this type of discontinuity is not more harmful than those less obvious discontinuities at which fracture of the remaining specimens commenced.

Specimen C.31 fractured at the junction of a tack weld and the final run, where some scale had been included, with a consequent blow-hole. The plotted point for girder C.31 (Fig. 4) is slightly below the general curve.

The limiting fatigue stress of 23,600 lb. per square inch calculated at the outer face of the girder corresponds to a stress of 20,200 lb. per square inch at the top of the fillet welds.

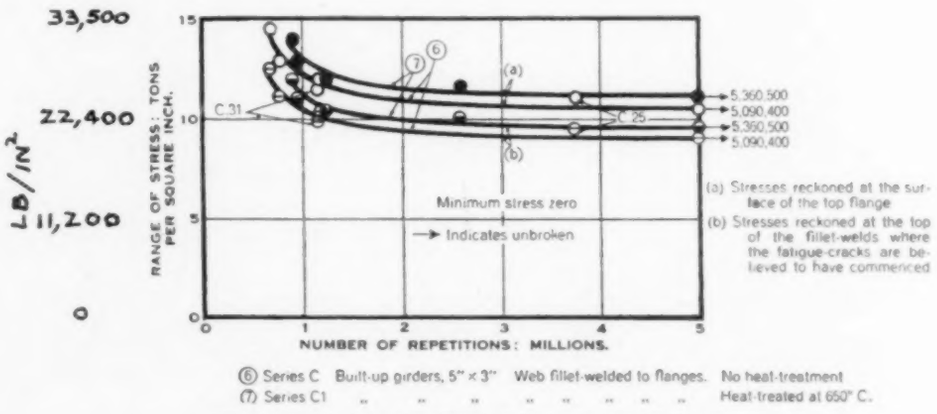


Fig. 4 Fatigue Tests in Girder-Fatigue Testing-Machine

Origin of Fracture

From the examination of the fractures it seemed clear that the crack did not start from the extreme fiber of the tension flange where there is maximum stress. The most probable point for the start of the fracture is somewhere in the junction of weld metal to plate.

FATIGUE TESTS ON WELDED GIRDERS AFTER HEAT TREATMENT AT A TEMPERATURE OF 650° C. (STRESS RELIEVED): SERIES C1

Details of Stress Relieving

The girders were welded similarly to series C (group 6). After welding, the joists were placed in reheating furnaces at a temperature of 460° C.; the temperature was raised to 600° C. in a period of 1 hour, and was then maintained at an average temperature of 650° C. for about 20 minutes. The joists were then removed and cooled in air.

Fatigue Tests

The results of the fatigue tests are shown in Table 7 and Fig. 4, (curves Nos. 7*a* and 7*b*). All failures except one occurred between the loading points in the length subjected to constant bending moment. There appears to be no essential difference between the fractures of these girders and those of series C. It may therefore be reasonably assumed that the fatigue crack originated in the same manner for both types of welded girders.

Static Tests of Welded Girders

Static bend tests were carried out on girder C1.42 (stress-relieved) and on girder C.26 (no heat treatment). Failure occurred in both cases by buckling of the compression flange. There was no evidence of failure of the fillet welds.

It is interesting to note that, although the stress relieving of the welded joists has raised the limit of proportionality on first loading from 26,500 to 33,400 lb. per square inch (that is, by 25 per cent), the fatigue limit has only been raised from 23,400 to 24,600 lb. per square inch (that is, by 5 per cent). Further, the apparent crippling stress under static loading has only been raised from 62,700 to 66,800 lb. per square inch.

Table 7—Fatigue Tests on Welded Joists. Series C1. Stress-Relieved at 650° C.

Specimen	Second Moment of Area Inch⁴ Units	Range of Stress: Lb. per Square Inch At Top Surface of Girder	At Junction of Fillet and Flange	Number of Repetitions	Remarks
C1.43	14.07	26,700	23,300	1,250,500	Broken, no creep
C1.48	14.01	24,600	21,500	5,260,500	Unbroken, no creep
C1.47	13.87	26,000	22,600	2,593,900	Broken, no creep
C1.45	13.95	28,700	24,900	958,300	Broken ⅝ inch outside the constant bending length, no creep
C1.44	14.05	31,400	27,400	917,700	Slight initial creep, broken

The fatigue limit, reckoned at the surface of the top flange, from zero minimum stress for more than 5×10^6 repetitions is 24,600 lb. per square inch. The corresponding fatigue limit at the top of the fillet-weld where the crack is believed to have commenced is 21,500 lb. per square inch.

SUMMARY OF TESTS

Table 8 summarizes the results obtained. Calling the fatigue range of the plain joist unity, the fatigue range factors, obtained by dividing the fatigue range for any joint from zero stress by the fatigue range of the plain joists, are shown in the Table. It will be seen that no joint gives a fatigue range factor of more than 0.67, and in this case the real fatigue range factor should be taken as 0.45. The fillet welded girders gave fatigue factors based on the stress at the fillet of 0.56 and 0.60 respectively. In a deeper girder the stress at the fillet is the all important stress. The butt-welded joints (series B) gave very poor results. By careful attention to the design of the butt weld, however, the fatigue factor, as shown by series B1, is as high as for the fillet welds of the welded girder. The results from these butt joints, series B and B1, suggest the desirability of the designer defining how the joint is to be made.

Although the results from series B are low, the fatigue-range factor is higher than the factor of series A1 when loads are compared or, in other words, the stress is reckoned on the cross section of the joist away from the rivet holes.

It is of interest to note from Fig. 1 that for, say, 200,000 repetitions, curves Nos. 2 and 5 give about the same fatigue range. It is suggested that the fatigue range of the riveted joint depends upon the tightness of the cover plates, but curve No. 2 (Fig. 1) gives the safe stress to which flanges with drilled holes can be subjected, and is the safe stress to assume for riveted joints. It is desirable also to point out that cracks developed in the fillet-welds transverse to the flanges of the joists of series D at stresses less than those in the fillet-welds of the welded girders. In the former case there is a more rapid discontinuity, which probably accounts for the lower result.

It will be clearly seen from Table 8 that, from the point of view of repeated stresses, static tests are of little value.

Table 8—Table Showing Fatigue Ranges and Static Strengths of Girders

Test Specimen	Maximum Stress Reckoned on	Fatigue Limit (Tension) for 5×10^6 Repetitions: Lb. per Square Inch	Fatigue Range Factor	Static Strength: Lb. per Square Inch Limit of Proportionality	Apparent Crippling Stress	Static-Strength Factor
1. Plain joist. Series A	Original section of joist	0 to 35,800	1	32,700	66,000	1
2. Drilled joist. Series A1	Original section of joist	0 to 13,500	0.38	29,800	65,000	0.98
	Net section of joist	0 to 19,100	0.53	42,400	92,000	..
3. Butt-welded joist. Series B	Original section of joist	0 to 14,600	0.41	30,700	59,500	0.90
Butt-welded joist. Series B1	Original section of joist	0 to 20,200	0.56	....	.....	..
4. Joist with fillet-welded cover-plate joint. Series D	Original section of joist	0 to 12,300	0.34	35,600	71,000	1.07
5. Joist with riveted cover-plate joint. Series E	Original section of joist	0 to 17,100	0.45	30,700	68,500	1.03
	Net section of joist	0 to 34,100	0.67	46,600	103,000	..
6. Welded girder, as welded. Series C	Maximum stress at top of flange	0 to 23,500	0.66	26,400	62,500	0.95
	Maximum stress at fillet	0 to 20,200	0.56	....	.....	..
7. Welded girder, stress-relieved. Series C1	Maximum stress at top of flange	0 to 24,600	0.69	33,400	67,000	1.01
	Maximum stress at fillet	0 to 21,500	0.60	....	.....	..

The load carried by the girder under either static or repeated stresses is proportional to the stress reckoned on the original section.

THE ENGINEERING FOUNDATION

Welding Research Committee

Sponsored by the American Welding Society « « » » and American Institute of Electrical Engineers

Supplement to the Journal of the American Welding Society, February 1939

WELDING COATED STEEL

A Review of the Literature to July 1, 1937

By W. SPRARAGEN* and G. E. CLAUSSEN†

This report is prepared under the auspices of

THE LITERATURE DIVISION OF THE ENGINEERING FOUNDATION
WELDING RESEARCH COMMITTEE

TABLE OF CONTENTS

	Page
Summary	33
Introduction	34
Welding Galvanized Iron	35
Spot Welding	35
Fusion Welding	35
Bronze Welding	35
General Observations	36
Welding Nickel Clad Steel	36
Fusion Welding	36
Spot Welding	37
Iron Pick-Up	37
Mechanical Properties	38
Flame Cutting	39
Welding Monel Clad Steel	39
Welding Inconel Clad Steel	39
Welding Stainless Clad Steel	39
Fusion Welding	39
Resistance Welding	40
Flame Cutting	40
Welding Tin Plate	40
Welding Terne Plate	40
Welding Chromium Plated Steel	40
Welding Cadmium Plated Steel	41
Welding Aluminum Clad Steel	41
Welding Copper Clad Steel	41
Welding Oxide-Coated Steel	41
Acknowledgments	42
Bibliography	42
Suggested Research Problems	43

Summary

WELDING GALVANIZED IRON

Spot Welding

Comparatively high currents (25 to 50% higher than for cold-rolled steel), high pressures and short times (2 to 8 cycles, 60 cycle a.c.) are necessary if the zinc is not to be burnt seriously.

Fusion Welding

There is no trouble in fusion welding galvanized iron pipe. Rapid welding with no attempt to gain full penetration was the rule adopted by one investigator to avoid porosity in welding galvanized iron. The extent of zinc burning depended on voltage and speed of welding. The burnt section was coated with an iron-zinc alloy. A 90% bituminous solution mixed with aluminum powder has been suggested for covering bare patches near arc welds.

Bronze Welding

Bronze welding is favored by many authorities for galvanized iron. The low temperature required for bronze welding does not vaporize zinc.

WELDING NICKEL CLAD STEEL

Fusion Welding

The nickel and steel are welded by means of the usual procedures, but the nickel weld must be contaminated with as little iron as possible. Consequently, the heat

* Secretary, Welding Research Committee.
† Research Assistant, Welding Research Committee.

input must be kept low, especially for the beads nearest the junction of nickel with steel, and the joints must be designed to offer the minimum opportunity for iron pick-up in the nickel weld. In butt welding, the clad side is usually welded last. The steel side is welded, the root chipped out and the nickel side welded. Metal and carbon arc, oxyacetylene and atomic hydrogen processes are equally applicable from the standpoint of feasibility of welding. For scarfed butt welds the nickel filler rod for oxyacetylene welding should be at least as thick as the sheet. For arc welding soft annealed flux covered nickel electrodes free from oxide and slightly thicker than the plate are best. In metal arc welding a short arc ($^1/_{16}$ to $^1/_8$ inch) is absolutely essential for the nickel weld, otherwise the action of the flux may be hindered. A slightly reducing flame is used for oxyacetylene welding.

Spot Welding

Spot welds in nickel plated steel offer no difficulty.

Iron Pick Up

The corrosion resistance of nickel generally decreases as the iron content increases. The pick-up is influenced by the extent to which the iron is melted during the welding of the nickel side.

The fillet weld with weld on nickel side, except for the exposed steel edge produces the least pick up. Single layer nickel welds with nickel on the outside pick-up the most iron.

Mechanical Properties

The strength of a butt weld in nickel clad steel is 90 to 100% of the strength of a butt weld in mild steel.

Flame Cutting

Nickel clad steel may be flame cut using the same procedure as for stainless clad steel.

WELDING MONEL CLAD STEEL

The same procedure is used for welding Monel clad steel as for nickel clad steel, except that a Monel filler rod is used.

WELDING INCONEL CLAD STEEL

One or two beads from an Inconel electrode are deposited on the Inconel side after the steel side has been welded with a steel electrode.

WELDING STAINLESS CLAD STEEL

Fusion Welding

In welding stainless clad parts there is less difficulty with gas absorption than with nickel clad, but the effect of iron pick-up is more dangerous. Several welding procedures have been recommended. In one method the plate is scarfed from the mild steel side. A bead of stainless is deposited at the bottom following which the gap is filled with mild steel electrodes. The stainless layer is welded from the reverse side. After the root has been chipped out, the 18–8 side is sometimes welded with 25 Cr–12 Ni electrodes. Stainless clad welds cannot be heat-treated.

Flame Cutting

The cut is made from the mild steel side, so that molten mild steel and slag will cleanly erode and sever the 18–8 cladding. For machine cutting the nozzle is one size larger than for mild steel and the cutting oxygen pressure is 50 to 100% higher. The nozzle is inclined 10° from the vertical so that the flame points back from the direction of cutting.

WELDING WITH VARIOUS COATINGS

Successful welds have been produced in tin plate, terne plate and chromium and cadmium plated steel.

WELDING COPPER CLAD STEEL

Iron pick-up in welding copper clad steel is lower than in welding nickel clad, so that alloy electrodes are not necessary for the first bead on the steel side. The tensile strength of welded copper clad was the same as base metal.

WELDING OXIDE COATED STEEL

The general belief is that rust and scale on scarves have no effect on fusion welding, unless the coating is thick and flaking. Unquestionably, thick oxide greatly hinders the spot welding of mild steel, but temper colors have no effect.

Welding Coated Steel

INTRODUCTION

STEEL is often coated to improve the corrosion resistance. Strictly speaking, there is an oxide coating on all steel exposed to the air. The coatings that are important for welding are the metallic coatings, such as zinc (galvanizing, sherardizing), nickel, 18–8 and tin. They may be electroplated, pressure welded with usually an intermediate layer of electrolytic iron or deoxidizer, sprayed or deposited on the surface with a welding torch. Some clad steels are made from bi-metal ingots. The manufacture of composite and clad steels is dealt with by T. S. Fitch in a comprehensive chapter entitled "Composite Steels" to be included in the 1939 Edition of *Metals Handbook* (American Society for Metals).

The coatings on steel always have a lower melting point than the steel itself. The extent to which welding procedure for coated steel differs from customary depends on the composition and thickness of the coating, as well as on the severity of service. The melting point, boiling point and reactivity of the coating with the welding atmosphere and materials have a direct influence on the precautions to be taken. Again, thick coatings demand more extensive changes in welding procedure than thin.

In the present review each coating is considered in a separate section, there being no important principles applicable to all coatings that are not too general to be valueless. The corrosion of welds in galvanized iron was dealt with in the review of literature on Corrosion Resistance of Welded Joints (AMERICAN WELDING

Society Journal, **16** (8) Suppl., 1–44 (1937)). Fumes from zinc, lead, lead paint and cadmium are poisonous. In general, the strength of spot welds in coated steel is 90 to 100% of base metal, according to Larsen.[1]

WELDING GALVANIZED IRON

Spot Welding

Comparatively high currents (25% higher than for cold rolled steel), high pressures and short times (4 to 8 cycles, 60 cycle a.c.) are necessary, according to Larsen[1] and Frost,[114] if the zinc is not to be burnt seriously. If the pressure is too low, the zinc vaporizes, and forms an arc, and a hole is burnt through the sheets. On the other hand, Owens[2] recommended half the pressure and twice the current for galvanized compared with black sheet iron. The die points should be kept rounded yet the zinc adhering to the tips should not be removed. The shear strength of single spots in galvanized iron (24 to 16 USS sheet gage) was always higher than single rivets (no details). Machine settings found by Thomason[3] to be successful in making spots that would tear a hole $^3/_{16}$ inch diameter in base metal are shown in Table 1. Gibb[4] observed in seam welding

Table 1—Machine Settings for Spot Welding Galvanized Iron. Thomason[3]

Specimen	Number of Cycles	Per Cent Capacity of Machine	Tip Pressure, Lb.
26 gage galvanized iron, 2 sheets	2	57	450
20 gage galvanized iron, 2 sheets	2	62	500
0.010 inch tinned sheet steel, 2 sheets	2	70	470
Ditto but 3 sheets, center sheet corrugated	2	88	470
20 gage galvanized iron to 0.050 inch Olympic bronze	4	67	500
26 gage galvanized iron to 0.010 inch tinned sheet steel	2	57	400
20 gage galvanized iron to 0.032 inch cadmium plated steel	2	31	450

100 kva. press type welder, 60 cycles a.c. 440 volts primary, throat 12 inches deep, copper-chromium alloy tips, $^1/_4$-inch tip cross section.

that zinc adhered to the rollers, but the welds did not rust. According to Hadley,[5] zinc burns away between the sheets but not between sheet and electrode. Some zinc and iron-zinc compounds were said by Goldmann[6] to have been visible in the microstructure of spot-welded galvanized iron, which he reported was more difficult to weld successfully than tin plate.

Fusion Welding

There is no trouble in fusion welding galvanized iron pipe, according to Greene[7] and Wikoff,[8] but the heat of oxyacetylene welding [96] removes zinc on the inside of the pipe to a distance on each side of oxyacetylene welds equal to the wall thickness. In the automatic arc welding of 12-gage galvanized iron tanks with a flux covered electrode having an external steel sheath Tracey[100] found that the galvanizing was retained in good condition on the inside of the tank adjacent to the weld. The sheets were $^1/_8$ inch apart before tacking, which drew them together. The welding speed was 75 ft./hr.

A good example of oxyacetylene welding galvanized iron tanks is given by *Welding Engineer*,[9] who mentioned no difficulties. An extreme instance of trouble with oxyacetylene welding galvanized iron is supplied by Arend.[10] The 16-gage galvanized steel was plated with iron or copper prior to welding in order to prevent loss of zinc. The iron deposit was amorphous and was more effective than copper. Borax solution thickened with salammoniac was used copiously, the salammoniac having the disadvantage sometimes of "chloridizing" the zinc. Water glass containing finely divided mineral wax, charcoal or sulphite pitch was also recommended. Fortunately Arend's black magic is not always necessary. Haas[96] used calcined borax or water glass as a flux to protect the zinc or else plated the galvanizing with iron or copper. Quantitative information on the efficacy of these treatments was not supplied. Since flux ordinarily increases the heat effect, it should also increase, rather than decrease, volatilization of zinc.

Rapid welding with no attempt to gain full penetration was the rule adopted by Warner[11] to avoid porosity in welding galvanized iron. Although in 1917 Kopferschmidt[12] stated that zinc contaminates oxyacetylene weld metal, Bibber[13] was unable to detect zinc in fillet welds made on galvanized iron with bare or coated electrodes. A bead of weld metal deposited by electrodes ($^3/_{16}$ inch diameter, 100 amps., 35 volts arc, coated; 160 amps., 22 volts arc, bare) on galvanized iron $^3/_8$ inch thick (2 to $2^2/_3$ oz. zinc per sq. ft.) melted the zinc on the reverse side, but not if the galvanized iron plate was $^1/_2$ inch thick. Although Bibber stated that the extent of zinc burning depended on voltage and speed of welding, he did not state the speed of welding in his tests. It was found that the burnt section was coated with an iron-zinc alloy. The absence of zinc in arc welds in galvanized steel was confirmed by Lillicrap[26] (no details). A 90% bituminous solution mixed with aluminum powder has been suggested[14] for covering bare patches near arc welds. For this purpose Liebetanz[91] has compounded a paint composed of zinc dust, $ZnCl_2$, $(NH_4)_2SO_4$, salammoniac and water. The paint is applied to the weld and its vicinity and permitted to dry. A soldering torch is then played upon it to produce a metallic finish. Rogers[102] found that the galvanizing on 8-inch galvanized wrought iron gas pipe had been burned off for a distance of 2 inches from oxyacetylene welds. The galvanizing was renewed by pouring molten zinc over the bare area as the pipe was rotated. The bare area was etched with HCl before regalvanizing.

In butt welding 14 gage (0.078 inch) Monel Metal sheets and at the same time welding them to galvanized iron angle bearers, Mechanical Topics[92] used a gap of $^3/_{16}$ inch (5 times the usual allowance) and Monel Metal electrodes $^3/_{16}$ inch diameter, 25 volts, 90 amps., short arc, half-moon weaving motion. The galvanizing was not removed before welding, and no finishing was required after welding.

Bronze Welding

Bronze welding is favored by many authorities[7,8,14,15] for galvanized iron. The low temperature required for bronze welding does not vaporize zinc to any extent on the inside of galvanized iron pipes. Forehand technique is recommended[96] with a little less spacing than for fusion welding. Flux on both sides and the same procedure as for mild steel are suggested by Granjon.[17]

Generally the diameter of the filler rod is $^3/_4$ the thickness of the sheet, Meslier[16] states. The flux used for bronze-welding cast iron is often suitable for preventing loss of zinc. The torch should deliver 27 to 45 cu. ft. acetylene per hr. per inch of thickness, Table 2, the lower consumptions applying to inexperienced operators. A neutral flame is held at an angle of 30° to the surface

and filler rod dipped in flux is not applied until the zinc has commenced to melt, not vaporize. Many operators find it easier to braze upward on a slight slope.

Table 2—Time and Material Consumption in Welding Tacked Galvanized Iron. Meslier[16]

Thickness, Inch	Torch Capacity Cu. Ft./Hr.	Diameter of Filler Rod, Inch	Time, Min./Ft.	Consumption of Acetylene Cu. Ft./Ft.	Consumption of Oxygen, Cu. Ft./Ft.	Filler Rod, Oz./Ft.	Flux, Oz./Ft.
0.08	3.6	0.08	2.1	0.12	0.14	0.45	0.10
0.16	7.2	0.12	2.8	0.30	0.36	0.85	0.15
0.24	10.8	0.16	4.0	0.76	0.90	2.5	0.20
0.39	18.0	0.24	5.5	1.4	1.6	6.5	0.25

It is said that no iron-zinc alloy is formed in bronze-welding galvanized iron pipe[97] and that the 90° V joint with 0.10–0.12 inch spacing should be backed up with a roll of asbestos paper.

A fumeless bronze rod is used by Sage[18] together with a slightly oxidizing flame, which is directed on the rod rather than on the zinc. As early as 1914 bronze-welded tensile specimens of galvanized iron fractured in base metal.[12]

Carbon arc welding with tin coated Everdur (97 Cu, 3 Si) rods is recommended by Ericson[20] and Sage.[18] The carbon is the negative pole and the arc is drawn over the flowing Everdur to prevent vaporization of zinc. The tin coated rod is not suitable for oxyacetylene welding. In another type of carbon arc welding[21] a bare phosphor bronze rod 2 to 4 times as thick as the space between the sheets is laid in the gap and fused with a thin carbon $^1/_8$ to $^1/_4$ inch diameter and low current (30–45 amps.). M. C. Smith[22] uses a copper electrode with thin mineral coating ($^1/_4$-inch electrode at 180 amps.).

Anders[23] and Wisbrock[90] make the unusual suggestion that a zinc rod with special flux be used for welding galvanizing iron. Lap joints ($^3/_8$-inch overlap) are employed. The sheet and zinc filler rod are coated with flux. The galvanizing is then melted with a small oxyacetylene torch and filler rod is applied along both edges. Powdered salammoniac applied to the rod assists in coating bare spots on the sheet. The shear strength of joints in sheets 0.026 inch thick, $2^3/_8$ inches wide with 0.32-inch lap was 3100 lb./in.^{2} using zinc filler rod and 2800 lb./in.^{2} using tin solder. Zinc joints required less time than soldered.

General Observations

It is often stated that galvanizing should be removed for a distance up to 1 inch[73] or so from the edges to be welded. Safety (fumes) as much as welding considerations influence the decision to remove zinc. The zinc may be removed with dilute hydrochloric acid,[24] which is an undesirable practice in itself. For thermit welding galvanizing is removed from that portion of the pipe projecting into the mold.[25] Lillicrap[26] believes it is best not to remove galvanizing. A zinc-lead-tin solder used by Ffield[27] to repair damaged galvanized coatings on Mn–V (0.25 C, 1.5 Mn, 0.11–0.18 V, 0.20 Si), Cr–Ni, and corrosion resisting steels caused cracks during subsequent heating occasioned by welding. Ffield believed the cracking was similar to that occasionally observed in soldering alloy steels. Experiments and literature on the penetration of solder into alloy steels are cited by Austin[28] and Goodrich.[29] Forge fires[30] in which babbitt metal had been melted were found to be unsuitable for forge welding presumably because there was intergranular penetration and cracking. Brazed or soldered joints should never be welded[19] because the solder tends to flow into the joint causing brittleness. The cracking of 18–8 weld metal in the vicinity of galvanized iron is discussed in the review of literature on Welding Chromium and Chromium-Nickel Corrosion and Heat Resisting Steels.

Boutté[32] believed two factors might account for the brittleness which he found (no details) in welded galvanized iron: (1) brittle iron-zinc alloy in the weld metal, (2) hydrogen from pickling after welding. In view of Bibber's[13] failure to detect zinc in arc weld metal on galvanized iron and in view of the extensive solid solubility of zinc in iron, the first factor requires more evidence than is now available for its acceptance as a cause. The second factor is not vital to welding. A. L. Wilson[33] reported that galvanizing after welding improved the strength. It is interesting to note that Viall[34] classed galvanized iron among unweldable materials in 1921.

Galvanized pipe can be cut and beveled[93] by the oxygen cutting process without serious injury to the galvanizing. An English writer[111] found that the consumption of oxygen in cutting galvanized plates $^1/_8$ to $^1/_4$ inch thick was 0.7 cu. ft./in.^{2}, which was also the average consumption for general demolition work on old ungalvanized plates.

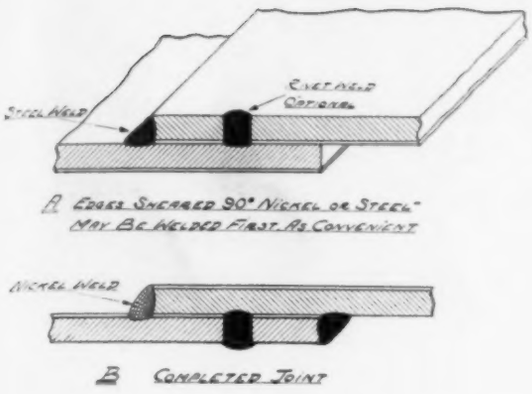
WELDING NICKEL CLAD STEEL

Fusion Welding

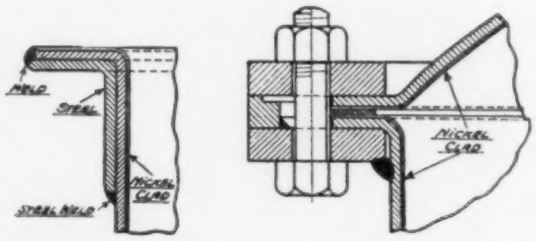
Nickel clad steel is mild steel upon one surface of which a layer of nickel has been pressure welded. The thickness of the nickel is generally 10 or 20% of the thickness of the plate, but thinner cladding also is used. The nickel and steel are welded by means of the usual procedures, but the nickel weld must be contaminated with as little iron as possible. Consequently, the heat input must be kept low, especially for the beads nearest the junction of nickel with steel, and the joints must be designed to offer the minimum opportunity for iron pick-up in the nickel weld.

In butt welding, the clad side is welded last, according to Huston,[35] Schoener and Flocke,[36] Müller,[31,37] Trautmann,[38] Hitchcock[39] and Miller.[40] Müller's reason for welding the clad side last was to avoid stresses, but the more usual reason is to restrict iron pick-up. For sheet 18 gage (0.050 inch) and thinner Miller[40] uses a flange weld with edges turned up $^1/_{16}$ inch. Thicker sheet and plate are scarfed, a 70° V or U weld being satisfactory. The steel side is welded, the root chipped out and the nickel side welded. If the nickel side is welded first, according to Huston and Watson,[103] it is necessary to provide machined, beveled joints with the lips in tight contact and in close alignment. Before the steel side is welded, the tacks on the steel side are chipped away. Nickel electrodes $^1/_8$ or $^5/_{32}$ inch diameter (reversed polarity, 90 to 140 amps.) are used with a short arc ($^1/_{16}$ to $^1/_8$ inch long) for plates $^5/_{16}$ to $^3/_8$ inch thick. A Monel electrode is satisfactory for making the steel to nickel weld in fillets, chipping being unnecessary.

An alloy electrode (no details) was recommended by Trautmann[38] for the first run on the steel side in U welds and in corner welds with the nickel outside. For plates up to 0.47 inch Müller[37] suggests a 90° V with scarf extending through the cladding. Thicker plates are U or 60° V and the scarf extends only through the steel side. Schwarz[41] and Lincoln Handbook[42] prefer to tack the steel side, weld the nickel side, then chip out the tacks and weld the steel side. For fillet welds the nickel is chipped out if the cladding is over 0.050 inch thick, according to Schoener and Flocke.[36] Joints sug-


STEEL WELD
EDGE BEVELED OPTIONAL
A EDGES BEVELED 90° NICKEL OR STEEL MAY BE WELDED FIRST AS CONVENIENT
NICKEL WELD
B COMPLETED JOINT

Metal arc fillet weld.



Reenforcing curb in light gage.

Flared cover plate

Outside corner welds.

Fig. 1—Types of Welds Involving Nickel Clad Steel. Huston.[25,103] (See Also *Welding Handbook*, 1938 Edn., pp. 636, 638)

gested by several writers, principally Huston,[25] are shown in Fig. 1.

With the exception that the heat should be kept low, the welding procedure is the same for nickel clad steel as for nickel and steel alone, as Huston[25] explains. Metal and carbon arc, oxyacetylene and atomic hydrogen processes are equally applicable from the standpoint of feasibility of welding. Flux-coated nickel rods $^3/_{32}$ or $^1/_8$ inch diameter are used in carbon arc welding with a pointed carbon rod $^3/_{16}$ inch diameter as negative pole. The carbon arc process is useful for sheet, the current being 50–80 amps. for sheet 0.062–0.125 inch thick with carbon $^1/_4$ inch diameter and rod $^3/_{32}$ inch diameter. In oxyacetylene welding, a single bead is advisable rather than multi-layers. A slightly reducing flame is essential to prevent sponginess on the nickel side.

Müller found that the nickel rod should be thicker (not less than 0.12–0.16 inch diameter) than usual to reduce iron pick-up.

For scarfed butt welds the nickel filler rod for oxyacetylene welding should be at least as thick as the sheet. For arc welding, soft annealed flux covered nickel electrodes free from oxide and slightly thicker than the plate are best. On the contrary, Trautmann[38] found that electrodes for the nickel side should be only 0.08 inch diameter. Rapid welding in a single pass prevents cracking of the nickel. The nickel weld is peened hot to refine the grain structure. Pneumatic hammers may be used to peen the nickel weld on unusually important parts, according to Huston and Watson,[103] but ordinarily peening is superfluous. Poste[43] believed peening removed the porosity from the nickel, which has a high capacity for absorbing hydrogen and other gases. In metal arc welding[25] a short arc ($^1/_{16}$ to $^1/_8$ inch) is absolutely essential for the nickel weld, otherwise the action of the flux may be hindered. A slightly reducing flame is used for oxyacetylene welding. A fluxed nickel rod $^3/_{32}$ to $^1/_8$ inch diameter is used in carbon arc welding with a carbon $^1/_4$ to $^5/_{16}$ inch diameter. The carbon arc process was recommended particularly for vertical welding. A flux coated nickel wire was suggested for atomic hydrogen welding.

The A A R Committee on Tank Cars[44] specified that calk welding on clad steel tanks be done with an electrode of the same composition as the cladding. The A. S. M. E. Boiler Code Committee[45] stated that radiographs of welds in nickel clad must be made after completion of the weld, but no allowance is permitted in design for the thickness of the nickel. If the sheet is nickel clad on both sides, according to Halfmann,[46] a nickel electrode is used for both sides. Mild steel clad on both sides is uncommon, Thompson[47] stated, but the unclad surface may be surfaced with nickel using nickel electrodes, if desired. According to Halfmann[46] there are no special precautions to be observed in soldering nickel clad steel.

Spot Welding

Spot welds in nickel plated steel offer no difficulty, Goldmann[6] found. Examination of the microstructure revealed hardened areas in the weld which may have been created by alloying of nickel with the steel. There were quantities of unalloyed nickel in the cooler portions of a seam weld in nickel plated sheets.

Iron Pick-Up

The corrosion resistance of nickel generally decreases as the iron content increases. It is inevitable that in depositing the single bead on the nickel side some iron is picked up by the nickel which must be deposited on the chipped root bead of the steel. The pick-up is influenced by the extent to which the iron is melted during the welding of the nickel side and is not, as Trautmann[38] suggests, influenced to a great extent by the velocity of diffusion of nickel in liquid or solid iron. A qualitative test suggested by Müller[37] for iron in the nickel weld consists of placing potassium ferricyanide paper on the surface of the weld, which has previously been moistened with dilute HCl. Blue spots on the paper signify segregations of iron.

The expected iron contamination of the nickel weld is shown in Table 3.

The fillet weld with the weld entirely on nickel, except for the exposed steel edge, produces the least pick-up. Single layer nickel welds with nickel on the outside pick up the most iron. Oxyacetylene welds may or may not pick up more iron than metal arc welds. Maier's

Table 3—Iron Content of the Nickel Weld in Nickel Clad Steel

Type of Weld	Iron Content, Per Cent	Reference
V Butt weld, arc	4 to 8	Huston,[35] and Schoener and Flocke[36], 1932
V Butt weld, oxyacetylene	Up to 15	
Lap weld, nickel inside (see Fig. 1)	7 to 15	
Corner weld, nickel outside multilayer nickel weld or J weld	Up to 15	
Corner weld, nickel outside, single nickel bead	45	
Fillet weld, nickel inside	3 to 7	
Butt weld	0.5 to 7	Trautmann,[38] 1936
Butt weld, 0.16-inch gap arc-plate thickness = 0.32 inch, nickel thickness = 0.032 inch	3	Maier,[48] 1936
Butt weld, 0.16-inch gap oxyacetylene	8	"
Butt weld, 0.32-inch gap arc	10	"
Butt weld, 0.32-inch gap oxyacetylene	13	"
Butt weld, 0.49-inch gap arc	6	"
Butt weld, 0.49-inch gap oxyacetylene	14	"
0.39-inch plate, 0.039-inch nickel thickness, backhand oxyacetylene butt weld	8 to 9	"
0.39-inch plate, 0.39-inch nickel thickness, forehand oxyacetylene butt weld	12 to 13	"
Ditto, torch held almost level with plate	8 at start; 14 at end	"
Ditto, torch held almost vertical	6 to 7	"
Ditto, iron layer chipped down 0.04 inch and strip of nickel 0.04 inch thick laid in gap	1 to 2	"
Ditto, but nickel bead deposited in chipped part before final bead was deposited	6 to 8	"
Ditto, arc welds, nickel electrodes, 0.16 inch diam., 140 amps.	11 to 15	"
Ditto, arc welds, nickel electrodes, 0.126 inch diam.	6 to 10	"
0.59-inch plate, 0.10 inch nickel clad, nickel side welded first, oxyacetylene butt weld	0.6 to 0.9	"
Ditto, arc	2 to 2.7	"
Ditto, iron side welded first, two layer oxyacetylene nickel weld	Nil	"
Ditto, arc	1.6 to 1.9	"
Ditto, nickel strip inserted, single layer oxyacetylene, rod had 0.5 Fe	0.55 to 0.6	"
Ditto, nickel strip inserted, single layer arc	1.5 to 1.8	"
0.75-inch plate, 0.02-inch nickel clad, oxyacetylene butt weld	3	"
Ditto, arc weld	11 to 18	"
Ditto, iron chipped and nickel sheet inserted	0.7 to 1	"
Ditto, arc weld	2.9 to 3.3	"
Ditto, nickel side welded with Monel rod	1.7 to 2.1	"
Butt weld, oxyacetylene	0.2 to 0.5	Rädeker,[49] 1937
Butt weld, arc	2	"

welds were made either with nickel electrodes $^1/_8$ inch diameter, 100 amps. or with a torch using 26.5 cu. ft. oxygen per hr. His nickel rod contained 0.5 Fe, 1.5 Mn; the weld metal contained 1.2–1.4 Mn. Chipping out the iron weld about $^1/_{16}$ inch and inserting a nickel strip before depositing the nickel bead reduced the pick-up, particularly with the thin cladding on thick plate. Multilayer welds on the nickel side were also effective, although Maier does not mention the cracking difficulty encountered by other investigators in depositing multiple beads on the nickel side. Rädeker[105] believes that the iron content of gas welds is less than that of arc welds, but arc welds have a little less tendency to intercrystalline cracking in contact with alkali melts than gas welds. A test which artificially produces grain boundary cracks in nickel clad is needed.

Mechanical Properties

In Maier's[48] tests described in the preceding section the tensile strength of the welds in 0.32 inch 10% nickel clad was about 60,000 lb./in.^{2}. Ductility was highest in the welds made with torch level with plate or with inserted strip. The welds in the 0.59-inch plate with 0.10-inch nickel cladding had a tensile strength of only 31,000 to 45,000 lb./in.^{2}, the thick nickel layer presumably having an adverse effect. In the last series with thin cladding the oxyacetylene welds made with and without inserted nickel strip had tensile strengths of 46,000–52,000 and 32,000–39,000 lb./in.^{2}, respectively. The corresponding strengths of the arc welds were 55,000–57,000 and 49,000–52,500 lb./in^2. Although the inserted nickel strip improved the ductility, the arc and oxyacetylene welds were much less ductile than the welds made with Monel rod, which failed in base metal at tensile strengths of 58,500–60,000 lb./in^2.

Metallic arc butt welds in 10% nickel clad steel $^{11}/_{16}$ inch thick stress relieved 45 minutes at 620–650° C. had the tensile properties shown in Table 4 in Huston and Watson's[103] tests. These results, as well as the properties of the oxyacetylene weld, agree closely with the properties of unwelded nickel clad steel.

Table 4—Mechanical Properties of Butt Welds in Nickel Clad Steel. Huston and Watson[103]

Specimen	Yield Strength, Lb./In.^{2}	Tensile Strength, Lb./In.^{2}	Elongation Per Cent in 2 Inches
Metal arc butt weld, $^{11}/_{16}$-inch plate. Full section specimen	40,300	59,800	22.2
Ditto but reduced section specimen	40,700	65,300	25.0
Ditto but special reduced section specimen*	38,200	64,000	22.5
Oxyacetylene butt weld, $^1/_4$ inch 10% clad plate	39,300	64,500†	20-nickel side 30-steel side

* The steel side was machined to provide about the same thickness of steel weld metal as of nickel weld metal. Free-bend tests of the arc welds resulted in 31% elongation with the steel in tension and 37% elongation in 1 inch with the nickel in tension. In neither test were cracks observed. The elongation in the nickel weld metal, which was $^3/_4$ inch wide, was 25% in $^1/_2$ inch.

† Fracture occurred $1^1/_2$ inches away from the weld.

Butt welds in nickel clad mild steel $^3/_8$ inch thick presumably 10% clad, were tested by Hitchcock,[39] Table 5. After the steel side had been welded, the root was chipped with a $^1/_4$ inch round nose gage. A single bead from coated nickel electrodes, $^1/_8$ inch diameter was deposited on the nickel side.

Table 5—**Tensile Tests of Arc Welded Nickel Clad Steel. Hitchcock**[39]

Specimen	Yield Strength, Lb./In.²	Tensile Strength, Lb./In.²	Elongation, Per Cent in 2 Inches	Location of Failure
Reduced section	37,400	64,500	35	In base metal 2 inches from weld
Full section	39,800	64,300	34	In base metal 2 inches from weld

Without supplying details of his tests, Rädeker[49] stated that butt welds, U or X, in nickel clad steel have a tensile strength of 54,500 to 56,000 lb./in.²; the bend angle varying between 134° and 146°. The elongation of the U welds was increased from 10–12% to 23–25% by annealing. The X welds were less ductile: 5–7% unannealed, 12–13% annealed. The steel was arc welded. It was immaterial whether the nickel was arc or oxyacetylene welded. In all specimens the scarfing did not extend through the cladding. Chipping the nickel away from the gap after the steel side had been welded had little effect.

The strength of a butt weld in nickel clad steel, according to Schoener and Flocke,[36] is the same as the strength of a butt weld in mild steel. Trautmann[38] regarded the strength of a nickel clad weld as 90% of the strength of mild steel welds. Poste[43] pointed out that stress annealing had no effect on nickel clad welds.

Flame Cutting

Huston and Watson[103] cut nickel-clad steel plate (5 to 20% cladding) 3/4 inch thick or heavier at about the same speed as unclad steel plate. The cut is made from the torch side with the tip inclined 10° away from the direction of travel. The same oxygen pressure as for steel is used with a tip one size larger. The cut surface is rough on plates thinner than 3/4 inch and the oxygen pressure for plates 3/8 inch thick and under should be 25% less than for mild steel. Nickel clad steel may be flame cut using the same procedure as for stainless clad steel, according to Inskeep[50] (see section on Welding Stainless Clad Steel).

WELDING MONEL CLAD STEEL

The same procedure is used for welding Monel clad steel as for nickel clad steel, according to Müller,[37] Trautmann,[38] and Halfmann,[46] except that a Monel filler rod is used. Clad rolls described by Steel[51] consisted of Monel plates welded to the steel with Monel electrodes containing 3% Si. A micrographic etching method that may be applied to welded Monel clad sheet is mentioned in Nickel Bulletin.[52] Cold concd. HNO_3 is used for the Monel, followed by 2% nital for the steel.

WELDING INCONEL CLAD STEEL

According to Trautmann[38] and the International Nickel Company,[53] the same procedure is used as for nickel clad. One or two beads from an Inconel electrode are deposited on the Inconel side after the steel side has been welded with a steel electrode. The flux covered Inconel electrode contains no iron, simply 80% Ni, 20% Cr. The tensile[53] properties of a reduced section specimen of a butt weld in 10% Inconel clad plate 5/16 inch thick were: yield strength = 39,600 lb./in.², tensile strength = 67,800 lb./in.², elongation = 21% in 2 inches, which were practically the same as the properties of unwelded Inconel clad steel.

WELDING STAINLESS CLAD STEEL

Fusion Welding

Stainless clad steel is mild steel on one surface of which a layer of 18–8 constituting 10 or 20% of the total thickness has been pressure welded. In welding stainless clad parts there is less difficulty with gas absorption than with nickel clad, as Poste[43] observed, but the effect of iron pick-up is more dangerous.

Several welding procedures have been recommended. In Moritz's[54] method the plate is scarfed from the mild steel side. A bead of stainless is deposited at the bottom following which the gap is filled with mild steel electrodes. The stainless layer is welded from the reverse side. A similar procedure is used by Miller[40] who remarks that the heat dissipation in stainless clad is quicker than in 18–8. One bead is deposited on the 18–8 side before a number of thin layers are used on the steel side to avoid overheating. The 18–8 side is finished last.

The principle of turned-down edges is advocated by P. A. E. Armstrong (private communication, December 1938) for butt welds in all thicknesses of 18–8 clad. In welding thin material the edges are turned down and welded from the steel side. A very light weld is applied in the groove on the clad side. The principle is particularly useful for avoiding dilution in butt welding thicker plate, Fig. 2. A U-shaped scarf is formed, the mild steel being cut back at least 3/16 inch depending on the thickness of the cladding in order to form an overhanging lip of cladding. If a scarf planer is employed, a second tool post should be fitted with a roller to turn down the overhanging cladding. Otherwise the lip may be hammered down. The scarfed plates are strongly clamped together, preferably by a band around the vessel or by bolts to the erecting plate. A backing-up chill is desirable. An 18–8 electrode is used to weld the upstanding lips in the bottom of the U from the mild steel side. The clad side then is welded, following which the plate is once again turned over. A light mild steel electrode which will not penetrate deeply

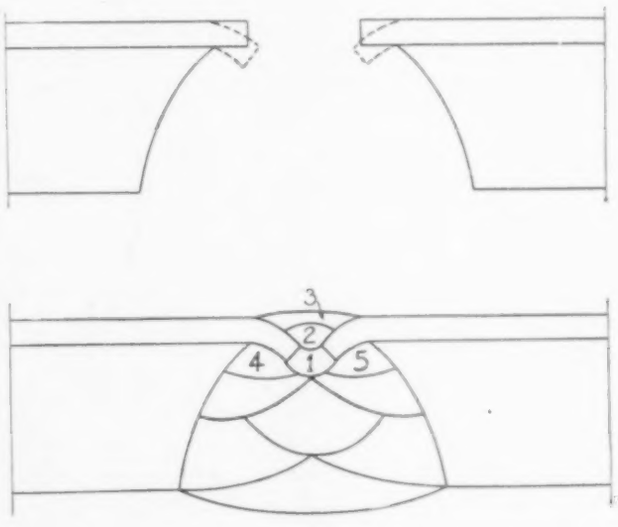


Fig. 2—Method of Butt Welding Stainless Clad Plates Suggested by P. A. E. Armstrong

Upper sketch shows U scarf machined in low-carbon steel with overhanging lips of cladding turned down
Lower sketch shows order of depositing weld
Bead 1. Stainless electrode, deposited from mild steel side
Beads 2 and 3. Stainless electrode, deposited from clad side
Beads 4 and 5. Light mild steel electrode, deposited from mild steel side. Most of the region around beads 4 and 5 will be fused together during welding
The remaining beads are deposited with a mild steel electrode in the customary way

into the 18–8 weld and cladding, is used to fill the bottom of the U. The first mild steel bead is deposited between the 18–8 lip and one side of the bottom of the U. The second bead is deposited in a similar manner on the other side of the 18–8 lip. Successive beads later are deposited in customary fashion to fill the U. In this way with reasonable care there is no dilution of the 18–8 facing.

Several authorities[55,56] employ the same tactics with stainless clad as with nickel clad. For example, Schuler[57] recommends a 90° V butt weld for plate over 12 gage, and U weld above 1/2 inch. The mild steel side is welded first with a 3/16-inch electrode. After the root has been chipped out, the 18–8 side is welded with 25 Cr–12 Ni electrodes. Sheets 12 gage or lighter need chill bars and may be welded from either side. Ingersoll[58] believes either steel or 18–8 side may be beveled (over 10 gage) to 90° V, but the stainless electrode is used last. If 25–12 electrodes are used for the 18–8 side to counteract iron pick up, according to Johnston,[59] the diameter should be larger than for a like thickness of 18–8 because 18–8 clad has higher heat conductivity than 18–8. In this way overheating and puddling become unnecessary to secure adequate penetration. Johnson[60] recommends the 25–12 electrode exclusively for thin sheet where a single bead is all that is required. The 25–12 weld should be sand blasted and cleaned with a solution containing 15% HNO_3, 3% HF, according to Keelor.[61] Peening either the 18–8 or 25–12 weld metal is unnecessary.[42,62]

On the other hand, Jennings[62] and Schwarz[41] prefer to weld the 18–8 side first with thin electrodes and low current. Schwarz illustrates several parts of arc welded 18–8, Cb alloyed 20% clad mild steel. An arc welded stainless clad pipe 5 ft. diameter is described by Davis.[63] In fabricating steel soap kettles lined with 16 gage stainless clad sheet, Keelor[64] adopted lap and flange welds and used 1/8 inch mild steel shielded arc electrodes wherever the welds were not exposed to corrosion. Coated 18–8 electrodes 3/32 inch diameter were used elsewhere. Vertical welds were made with a short arc. Seams were welded at the rate of 40 to 60 ft. per hr. Tichodeev[65] used 18–8 (0.12 C) electrodes 0.08–0.10 inch diameter, 50 amps. for welding 20% stainless clad 0.06 inch thick. Lap and lock seam joints welded on the 18–8 side had tensile strengths of 60,000 and 63,500 lb./in.^{2}, respectively. Butt welds failed at 72,000 lb./in.^{2}.

Stainless clad welds cannot be heat treated, Hodge[66] states, but Block[67] believed the peak residual stresses in welded stainless clad vessels may be relieved by a hydrostatic test (no details).

Mild steel clad with 18 Cr iron was difficult to weld without excessive iron pick up by the cladding in Leverick's[68] tests.

Resistance Welding

Examples of resistance (spot) welded dairy equipment made of stainless clad are cited by Karsten.[69] Schröder[70] mentions the seam welding of 18–8 sheets on steel to form stainless clad.

Flame Cutting

The cut is made from the mild steel side, according to Inskeep,[50] so that molten mild steel and slag will cleanly erode and sever the 18–8 cladding. For machine cutting the nozzle is one size larger than for mild steel and the cutting oxygen pressure is 50 to 100% higher. The nozzle is inclined 10° from the vertical so that the flame points back from the direction of cutting. The nozzle angle is the same for manual cutting but the cutting oxygen pressure is 5 to 10% higher than for machine cutting. Machines cut 18–8 clad 6/32 and 3/8 inch thick at the rate of 12 and 9 inches per minute, respectively, which is a bit slower than for mild steel. Hand cutting is slower than machine cutting. No filing or grinding is necessary on bevel cuts. As a result of cutting, the 18–8 edge lost a little carbon, did not pick up any iron and did not peel from the mild steel. Peeling has never been observed in welding stainless clad for that matter. There was a slight change in the microstructure of the 18–8 near the oxide film (0.0005 inch thick) at the cut edge. Although no tests were reported, it was believed that the cut edge would not be subject to intergranular corrosion if the 18–8 cladding contained one of the usual alloying elements.

WELDING TIN PLATE

Tin plate consists of sheet steel that has acquired a coating of tin by electrolysis or through having been dipped in a bath of the molten metal. Before being fusion welded, the sheet may be heated and the tin brushed off.[24] Although tin has a high electric resistance, it also has a high boiling point. Consequently, there is no vaporization during spot welding and welds are easily[1] made. Machine settings for spot welding tin plate so that the joint fails by pulling a hole at least 3/16 inch diameter in base metal are listed in Table 1. Tin may make spot welds in tin plate brittle, according to Goldmann,[6] who stated that he found globules of a tin-iron compound (no details) in a spot weld. It is unlikely that Goldmann's observation of free carbon near the surface of a spot welded tin plate was accurate. In 1914 Fuchs[71] found that spot welded tin plate was, in reality, a soldered joint and had low strength. A micrograph exhibited by Atkins[72] was believed to reveal brittle tin-iron alloy in resistance welded tinned iron bars. There is no difference between the welding of hot-dip and electrolytic tin plate, according to Colin G. Fink (private communication, September 1938).

WELDING TERNE PLATE

The principles that apply to welding tin plate apply as well to terne plate. Spot[1] welding (see Table 1) and fusion welding[24] are the same for both types of plate. No intermediate alloy is formed between the lead and iron in oxyacetylene welding.[95] The lead melts and, being immiscible in iron, is removed from the seam.

For safety the lead should be removed for a distance of 3/8 to 1 inch from the weld, as Horn[73] points out. Whether the lead sinks to the bottom in oxyacetylene welding[74] is dubious. Werner's[75] observation that intergranular oxide in lead deposited on steel by means of an oxidizing flame leads to intergranular corrosion in service may have some application in the welding of lead coated steel. To clad steel with lead,[76] the steel is either tinned or NH_4Cl is used as flux. Spot and seam welds in lead-coated iron examined by Goldmann[6] contained minute globules of lead.

WELDING CHROMIUM PLATED STEEL

About the same procedure as for cold-rolled steel is adopted in spot welding chromium plated steel, according to Larsen.[1] A flat electrode[114] is essential to retain a flawless surface.

WELDING CADMIUM PLATED STEEL

Machine settings for spot welding cadmium plated steel are given in Table 1. Thomason[77] found that cadmium adheres to copper tips. In Ignitron spot welding brass to cadmium plated steel, Dawson[78] found that the cadmium between the sheets is instantly vaporized and a strong spot weld is produced. The spot welding of silver contacts to cadmium plated steel is all but impossible.

WELDING ALUMINUM CLAD STEEL

Aluminum plated steel is easily welded by electric or gas processes according to Colin G. Fink (private communication, September 1938). The ductility of the material is not destroyed in either operation, although gas welding darkens the aluminum adjacent to the weld. Aside from calorized steel, which is dealt with in the review of literature on the Effect of Aluminum on the Welding of Steel, no information on aluminum clad steel is available except a vague statement by Johag[79] about using welding to patch an aluminum-lined mild steel tank.

WELDING COPPER CLAD STEEL

Iron pick up in welding copper clad is lower than in welding nickel clad, Trautmann[38] found, so that alloy electrodes are not necessary for the first bead on the steel side. Oxyacetylene or atomic hydrogen welding and 70° V butt welds are preferred for copper clad. The carbon arc process is best for vertical welds on the clad side. The carbon is the negative pole and is 0.24 inch diameter. The filler rod is 0.08 inch diameter, is held pointing downward at 30° to the vertical, and is not applied until the edges have reached the melting point. The carbon points upward at 60° to the vertical. Rädeker[49,80,105] found that the Lessel sleeve electrode is useful for welding the copper side, and a rod containing 90 Cu, 10 Zn is useful in oxyacetylene welding the copper side. Welders accustomed to steel should not be entrusted with the welding of the copper side, because the copper is easily burnt. The best test for the corrosion behavior of welded copper-clad and of nickel- and stainless-clad as well is deep etching in HNO_3 or HCl (no details).

The iron content of the copper weld is 0.03 to 0.08%, the latter figure applying to preheated joints. If the copper layer contains 0.88 Cu it is certain that overheating has occurred. The low melting point of copper rather than low diffusion coefficient of iron in copper unquestionably accounts for the low iron pick-up in the copper weld.

Contrary to Trautmann, Maier[48] preferred metal arc to oxyacetylene welding for the copper side, because a smaller scarf opening (0.24–0.32 inch maximum) was required for arc welding without destroying the copper cladding. Welds in plates $^3/_8$ inch thick were brittle at the starting end. Annealing and hot peening were beneficial. The tensile strength of welded copper clad was the same as base metal. Mayer[104] removed the copper for a distance of 0.08 inch on either side of the joint, welded the steel side (70° V butt, arc or gas), and gas welded the copper side with a slightly reducing flame. Peening the copper raised the corrosion resistance. Rädeker[49] found no difference in strength between welded nickel clad and welded copper clad.

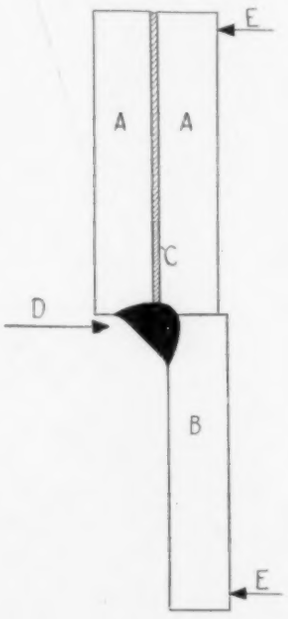


Fig. 3—Specimen to Test the Effect of Red Lead Paint on Overhead Fillet Welds in Structural Steel 0.55 Inch Thick. Schmidt.[101]

A—Old bridge steel
B—Mild structural steel
C—Layer of red lead paint
D—Test load
E—Supports

WELDING OXIDE-COATED STEEL

The general belief is that rust and scale on scarves have no effect on fusion welding, unless the coating is thick and flaking. Critchett[81] pointed out that a carburizing flame in oxyacetylene welding reduces any oxide scale to metallic iron. Klosse[82] carried out a comprehensive investigation of the subject but he was hampered by inexpert welders and drew no conclusions. In Clarke's[83] tests, flame cut surfaces were as satisfactory as ground surfaces for arc welding. Depending on the generator, penetration was or was not deeper on rusty plain carbon steel welded with coated electrodes, Tetzlaff[84] found. Rust had little effect on the fillet welds tested by Rogers,[85] who also found that fresh red lead paint was beneficial to welding. Unfortunately, he did not state whether covered electrodes were used nor whether the $^1/_4$-inch fillet welds were made in a single pass. For some reason dry oil or paint on the surface caused porosity, although the difference in strength between best and worst specimens was less than 10% in all Roger's tests. Owens[2] found that paint had no effect on welding (2 layer fillet welds, $^3/_{16}$-inch electrodes) in the flat position but caused a little porosity overhead, because oil dripped into the weld. Riveting destroyed as much paint as fillet welding. Iron oxide paint was preferable to red lead paint.

Arc welding repairs on a steel bridge were not affected by red lead paint in Schmidt's[101] experience. Hammer tests were made on overhead fillet welds in two plates of mild steel (0.06 C, 0.40 Mn, 0.04 Si, 0.03 Cu, 0.06 S, 0.06 P) 0.55 inch thick at right angles. The paint layer on both plates was 0.04–0.06 inch thick. With covered electrodes the welds fractured after 54 blows with a 17 lb. hammer. The fracture was ductile and free from porosity and slag. If the paint had been removed with a welding torch the specimen could not be fractured. Overhead fillet welds of the type shown

in Fig. 3, when tested in bending, showed fine-grained fracture with excellent penetration and without inclusions, although during welding the paint had bubbled and vaporized.

In Groebler's[94] tests a good covered electrode (no details) was deposited as single beads on three types of surfaces: (1) ground, (2) rusty, (3) mill scale, of two steels: (*a*) 0.19 C, 0.30 Si, 1.11 Mn, 0.44 Cu, 0.35 Cr, (*b*) 0.21 C, 0.40 Si, 1.07 Mn, 0.45 Cu, each 0.39 inch thick. The deposits on steel (*a*) were less smooth than on (*b*), but there was no appreciable difference due to type of surface. The electrode was able to melt through the rust, which decreased the viscosity of the slag on account of increased iron oxide content, but increased the viscosity of the metal, whose oxide content was increased.

Only the removal of mill scale was effective in preventing bright blow-holes in automatic 90° V butt welds in $^1/_4$-inch plate made by Johnson[113] with $^3/_{16}$-inch bare mild steel electrodes. It was believed that porosity was caused by a reaction between carbon and oxygen in the solidifying weld metal.

Unquestionably, thick oxide greatly hinders the spot welding of mild steel, as Jevons and Wheeler[86] state, but temper colors have no effect. Burton[87] believed that a thin coating of rust was good in spot and resistance butt welding for the dubious reason that rust localized the heat. C. A. Adams pointed out that rust may be expelled in welding a bar to a sheet but may be entrapped in a weld between two sheets. Thum[99] found numerous gas cavities in spot welds in rusty structural steel. In seam welding mild steel 0.04 inch thick, 0.39-inch overlap, 0.08 sec. weld, 0.04 sec. interval, 0.08-inch seam width, 700-lb. pressure, 3.3 ft. per min., Sidorenko[88] found, Table 6, that thin or thick coatings

Table 6—Seam Welding Mild Steel. Sidorenko[88]

No.	Specimen	Location of Fracture	Strength, Lb. per Inch of Seam
1	Pickled 30 min., 80° C. in 50% HCl	Heat affected zone	279
2	Ground	" " "	275
3	Oil coated	" " "	254
4	Rust coated	Weld	173
5	Thin slag (cinder) coating	"	157
6	Thick slag and rust coating	"	136

of slag and rust produced welds that were quite unsatisfactory in appearance and strength. X-ray examination revealed no more porosity in specimen 4 than in specimens 1 to 3. The tensile specimen was $1^3/_8$ inches wide along the reduced section of overlap. Although their conclusions were tentative, Tafel and Heil[89] found that the strength of resistance butt welds in a steel containing 0.066–0.10 C, 0.47–0.59 Mn was improved if a slag (48.70 Fe, 21.50 SiO_2, 5.07 Al_2O_3, 4.60 CaO, 2.41 MgO, 1.07 Mn, 0.047 P) was applied to the surfaces to be welded.

Rust, oil, grease and paint retard flame cutting, as Atkins and Walker[106] pointed out, and create ragged cuts, according to Schimpke and Horn.[107] Larger preheating flames were used by Mitchell[108] and Leigh[109] in flame cutting rusty or scaled steel, whereas Shibata[110] used 10% higher cutting oxygen pressure. An English writer[111] found that in ship breaking, paint and rust decreased the cutting speed 20% and increased the oxygen consumption 33% compared with clean surfaces. Although Witt[112] found that rust on the lower side of the plate may cause slag to adhere, Crowe and Deming (see *Welding Handbook*, 1938 Edn., 301) state that cutting slag does not adhere so tightly to rusty surfaces as to clean steel. The rustier side of a plate to be cut should be the lower side.

ACKNOWLEDGMENTS

Contributions to the review have been received from:

P. A. E. Armstrong, Latrobe Electric Steel Company.
Dr. Colin G. Fink, Columbia University.
T. S. Fitch, Jessop Steel Company.

BIBLIOGRAPHY

1. Larsen, E. I., American Welding Society Journal, **15** (12) 9–16 (1936).
2. Owens, J. G., American Welding Society Journal, **6** (4) 59–73 (1927).
3. Thomason, H., *Canadian Machy.*, **48** (5) 50–51 (1937).
4. Gibb, W. H., *Welding Engr.*, **11** (3) 27–29 (1926).
5. Hadley, C. A., paper to Instn. Welding Engrs., page 43, Jan. 30, 1930.
6. Goldmann, F., *Elektroschwg.*, **2**, 41–47, 81–86 (1931).
7. Greene, T. W., American Welding Society Journal, **12** (7) 14–15 (1933).
8. Wikoff, A. G., American Welding Society Journal, **9** (1) 23–27 (1930).
9. *Welding Engr.*, **12** (11) 39–40 (1927).
10. Arend, A. G., *Sheet Metal Inds.*, **9**, 169 (1935).
11. Warner, W. L., *Trans. A. S. M. E.*, **51**, Part 2, IS-51–7, 69–73 (1929).
12. Kopferschmidt, *Welding Engr.*, **2** (3) 25 (1917).
13. Bibber, L. C., American Welding Society Journal, **12** (12) 4–9 (1933).
14. *Mech. World & Engg. Record*, **87**, 226 (1930).
15. Meslier, R., *Rev. Soud. Autog.*, **27** (252) 7–12 (1935).
16. Meslier, R., *Rev. Soud. Autog.*, **30** (290) 392–394, May 1938.
17. Granjon, R., *Notions Pratiques de Soudure Autogène*, Paris, 62–63 (1932).
18. Sage, V. L., *Ind. & Weld.*, **6** (12) 20–21, Dec. 1935.
19. Johnson, J. B., American Welding Society Journal, **15** (9) 2–11 (1936).
20. Ericson, L. W., *Ind. & Weld.*, **6** (2) 24–25, Feb. 1935.
21. *Industry & Welding*, **4** (4) 26, April 1934.
22. Smith, M. C., *Amer. Machinist*, **79**, 614–616 (1935).
23. Anders, W., *Autog. Metallb.*, **30**, 33–36 (1937).
24. *Allg. Schlosser- u. Maschinenbauerztg.*, **37**, 643–645 (1936).
25. *Iron Trade Rev.*, **68**, 1393 (1921).
26. Lillicrap, C. S., *Trans. Instn. Naval Arch.*, **75**, 45–58 (1933).
27. Ffield, P., *Advance Paper No. 7, Soc. Naval Arch. & Marine Engrs.*, (1937).
28. Austin, G. W., *Inst. Metals Jnl.*, **58**, 173–185 (1936).
29. Goodrich, W. E., *Iron & Steel Inst. Jnl.*, **132**, 43–66 (1935).
30. Lee, B. A., *Machinery* (N. Y.), **35**, 207–208 (1928).
31. Müller, R., *Korrosion, Suppl. to Chem. Apparatur*, **7**, 37–39 (1932).
32. Boutté, A., *Tenth Intnl. Cong. Acetylene, Zurich, Mitteil.*, 336–338 (1930); *Welding Journal* (London), **28**, 16–17 (1931).
33. Wilson, A. L., *Weld. Engr.*, **15** (2) 47–52 (1930).
34. Viall, E., "Gas Torch and Thermit Welding," McGraw-Hill Book Co., N. Y., **186**, 213–220 (1921).
35. Huston, F. P., *Weld. Engr.*, **17** (9) 24–29 (1932); Huston, F. P., and Watson, T. T., *Ind. & Weld.*, **6** (12) 50–55, Dec. 1935; *Proc. Master Boiler Makers' Assn.*, 95–101 (1936).
36. Schoener, J. G., and Flocke, F. G., American Welding Society Journal, **11** (11) 18–22 (1932); *Weld. Engr.*, **21** (11) 30–34 (1936); *Welding Handbook*, 1938 Edn., 634–639.
37. Müller, R., *Elektroschwg.*, **3**, 229–230 (1932); *T. Z. prakt. Metallb.*, **43**, 52–57 (1933); *Chem. Apparatur*, **24**, 19–22, 49–52 (1937).
38. Trautmann, B., *Maschbau.*, **15**, 295–299 (1936); *Werkstatt u. Betrieb*, **69**, 253–256 (1936); *Z. VDI*, **80**, 785–787 (1936); *Chem. Fabrik*, **10**, 463–468 (1937).
39. Hitchcock, J. O., *Industrial Chemist*, **13**, 433–436 (1937).
40. Miller, P. H., "Stainless Steels," Oxford Univ. Press, London, 95 pp. (1937).
41. Schwarz, A., *Ind. & Weld.*, **10** (11) 30–32, 39, Nov. 1937.
42. Procedure Handbook for Arc Welding, Fourth Edn., Lincoln Electric Co., Cleveland (1936).
43. Poste, E. P., *Ind. & Engg. Chem.*, **27**, 128–134 (1935).
44. Goodwin, G. S., *Rwy. Age*, **102**, 1040D122–1040D124 (1937).
45. *Mech. Engg.*, **59**, 123 (1937).
46. Halfmann, K., *Z. VDI*, **78**, 1421–1422 (1934).
47. Thompson, W., *Welder*, **9**, 380–381 (1937).
48. Maier, *Twelfth Intnl. Cong. Acetylene, Proc.*, 1424–1429 (1936).
49. Rädeker, W., *Zeit. Metallkunde*, **29**, 1–8 (1937).
50. Inskeep, H. V., American Welding Society Journal, **16** (8) 2–5 (1937); *Mech. World & Engg. Record*, **102**, 79–81 (1937).
51. *Steel*, **99**, 45–46, Nov. 9, 1936.
52. *Nickel Bulletin*, **9**, 256–257 (1936).
53. Intnl. Nickel Co. Advt., *Ind. & Weld.*, **10** (8) 22 (1937); Flocke, F. G., and Schoener, J. G., *Welding Handbook*, 1938 Edn., 633–634.
54. Moritz, M., Iron & Steel Inst. Welding Symposium, **II**, 445–452 (1935).
55. Hougardy, H., *Elektroschwg.*, **9** (1) 5–9, Jan. 1938.
56. *Electric Welding*, **3**, New Series, (16) 110–111, April 1934.
57. Schuler, E. V., *Welding*, **5**, 193–194 (1934).
58. Ingersoll, S. L., *Weld. Engr.*, **17** (10) 27–28 (1932).
59. Johnston, A. P., *Weld. Engr.*, **20** (8) 19–22 (1935).
60. Johnson, W. C., Book of Stainless Steels, Edited by E. E. Thum, Cleveland, Second Edn., 220–223 (1935).
61. Keelor, W. B., **18** (12) 15–17 (1933).
62. Jennings, C. H., "How to Weld 29 Metals," Westinghouse E. & M. Co., 73–75 (1937).
63. Davis, A. F., American Welding Society Journal, **13** (9) 2 (1934).
64. Keelor, W. B., *Ind. & Weld.*, **10** (2) 68–69 (1937); *Weld. Engr.*, **22** (1) 21–22 (1937); *Sheet Metal Worker*, **28** (2) 27–28 (1937); *Chem. & Met. Engg.*, **44**, 139 (1937).

65. Tichodeev, G. M., *Katchestvennaia Stal*, (7) 45–50 (1936); abstract in *Z. VDI*, **81**, 579 (1937).
66. Hodge, J. C., *Refiner*, **12**, 189–197 (1933).
67. Block, E., *Elektroschwg.*, **9** (3) 45–49, Mar. 1938.
68. Leverick, F., *Inst. Weld. Engrs. Proc.*, **7**, 17–35 (1933); *Welding Jnl.* (London), **31**, 237–243 (1934).
69. Karsten, A., *Mülkwirtschafliche Zbl.*, **66**, 104–107 (1937).
70. Schröder, E., *Elektroschwg.*, **1**, 110–113 (1930).
71. Fuchs, O., *Werkstattstechnik*, **8**, 323–327 (1914).
72. Atkins, E. A., *Weld. Engr.*, **7** (7) 32–38 (1922).
73. Horn, H. A., *Schmelzschw.*, **4**, 114–116 (1925).
74. *Welding Journal* (London), **33**, 214 (1926).
75. Werner, M., *Maschbau.*, **13**, 522 (1934); *Westdeutsche Techn. Bl.*, No. 2, 5 (1933).
76. *Soudure et Oxy Coupage*, **12** (71) 275, Nov.-Dec. 1935.
77. Thomason, H., *Engg. Jnl. Canada*, **20** (1) 8–14, Jan. 1937.
78. Dawson, J. W., Iron & Steel Inst. Weld. Symp., II, 55–66 (1935).
79. Johag, W., *Autog. Metallb.*, **24**, 355–356 (1931).
80. Samesreuther, R., *Verfahrenstechnik, beiheft Z. VDI*, (2) 68–71 (1937).
81. Critchett, J. H., AMERICAN WELDING SOCIETY JOURNAL, **14** (5) 2–6 (1935).
82. Klosse, E., *Dr. Ing. Dissn. T. H. Breslau* (1933); abstracts in *Stahl u. Eisen*, **53**, 1374 (1933); **54**, 348 (1934).
83. Clarke, E., *Welding Industry*, **1**, 315–320 (1933).
84. Tetzlaff, L., *Schiffbau*, **39** (1) 1–10, Jan. 1938.
85. Rogers, G. F., AMERICAN WELDING SOCIETY JOURNAL, **9** (1) 38–40 (1930).
86. Jevons, J. D., and Wheeler, M. A., *Weld. Ind.*, **4**, 356–360, 396–400 (1936).
87. Burton, F., AMERICAN WELDING SOCIETY JOURNAL, **9** (9) 78–87 (1930); *Steel*, **87**, 72–73, Oct. 9, 1930.
88. Sidorenko, A., "Effect of Surface Coatings on Seam Welds in Low Carbon Steel," Academy of Sciences, Institute of Electric Welding, Kiev, Russia, 111–128 (1937).
89. Tafel, and Heil, W., *Stahl u. Eisen*, **47**, 1010–1011 (1927).
90. Wisbrock, K. H., *Autog. Metallb.*, **31** (13) 211–214, July 1, 1938.
91. Liebetanz, R., *Werkstattstechnik u. Werksl.*, **30**, 337 (1936).
92. *Industry & Welding*, **11** (8) 34, 38, Aug. 1938.
93. *Welding*, **1**, 375–376 (1930).
94. Groebler, H., *Esabs tidning Svetsaren*, (9) 213–227, July 1938; *Tagungsbericht 3 Kjellberg Tagung*, 115–128(1938).
95. *Acetylene & Weld. Jnl.*, **14**, 118 (1917).
96. Design of Welded Piping, Linde Air Products Co., N. Y., 69–71 (1936).
97. *AGA Meddelande*, **44**, 8, Aug. 1935.
98. Haas, B., *Z. Giesserei Praxis*, **44**, 77–78 (1923).
99. Thum, E. E., *Chem. & Met. Engg.*, **19**, 301–308 (1918).
100. Tracey, B. C., *General Electric Rev.*, **27**, 772–773 (1924).
101. Schmidt, H., *Bautechnik*, **14**, 413–415 (1936).
102. Rogers, W. H., *Proc. Pacific Coast Gas Assn.*, **14**, 886–887 (1922–23).
103. Huston, F. P., and Watson, T. T., Bulletin T-4, International Nickel Co., 16 pp., Sept. 1938.
104. Mayer, L., *Berg.-u. Hüttenmänn. Jahrbuch*, **84**, 82–87 (1936).
105. Rädeker, W., *Stahl u. Eisen*, **58** (43) 1153–1160 (1938).
106. Atkins, E. A., and Walker, A. G., "Electric Arc and Oxy-Acetylene Welding," Third Edn., Pitman, London, 142–151 (1936).
107. Schimpke, P., and Horn, H. A., "Praktisches Handbuch der gesamten Schweisstechnik," Vol. 1, Third Edn., Springer, Berlin, 255 (1938).
108. Mitchell, A. F., *Proc. Engrs. Soc. West. Penn.*, **31**, 711–718 (1915).
109. Leigh, A. C., *Proc. Intnl. Acet. Assn.*, **38**, 146–150 (1937).
110. Shibata, H., and Otsuka, S., *Proc. Twelfth Intnl. Cong. Acet.*, 1386–1391 (1936).
111. *Acetylene & Weld. Jnl.*, **21**, 240 (1924).
112. Witt, E. A., *Weld. Ind.*, **4**, 313–319 (1936).
113. Johnson, J. S., AMERICAN WELDING SOCIETY JOURNAL, **14** (7) 10–14 (1935).
114. Frost, L. H., *Weld. Engr.*, **21** (6) 44–46 (1936).

SUGGESTED RESEARCH PROBLEMS

1. Shrinkage stresses in unannealed fusion welded nickel-clad and stainless-clad steel. The distribution of shrinkage stresses in butt-welded plates made with and without restraint should be investigated by X-ray or subdivision methods.

2. Fatigue strength of fusion welded nickel-clad and stainless-clad steel.

3. Creep behavior of fusion welded nickel-clad and stainless-clad steel.

4. A study of the welding of higher-tensile alloy steels clad with nickel or stainless steel. Cracking and other difficulties should be studied as well as the mechanical properties of welds.

5. Mechanical properties and microstructure of carbon-arc welds in nickel-clad steel.

6. Mechanical properties of fusion welded copper-clad and Monel-clad steel.

7. Intergranular penetration of zinc-lead-tin solder in areas of alloy steel affected by welding heat resulted in cracks. The conditions necessary for such cracking should be determined. Doubtless many steels will be insensitive and some solders will not exhibit intergranular penetration under any circumstances.

8. There is practically no information on the spot welding of coated steels, although it is common practice. Galvanized iron, terne plate and tin plate should be tested. (*a*) The stages in the production of spot welds in these coated materials may be revealed by examining the weld and its microstructure after passage of welding current for intervals varying from short (insufficient to cause welding) to long. In this way the effect of the melting point of the coating, its volatility and oxidizability on spot welding could be evaluated. Some coatings may be squeezed from the weld, others may volatilize or oxidize. (*b*) The strength and ductility of spot welds in these coated sheets. (*c*) The corrosion resistance of the spot welds. (*d*) The composition of the metal in the fused spot as a function of thickness of coating and power input in welding.

9. The fusion welding of light gage stainless clad (lighter than 0.090 inch) is common practice but methods must be developed for reducing the iron contamination on the stainless side. Iron contamination seriously reduces the corrosion resistance of welds in light gage stainless clad.

COOPERATION INVITED

The Welding Research Committee cordially invites every one identified with welding to carefully study its annual report published as the December 1938 Welding Research Supplement and submit constructive suggestions and criticisms.

Annual contributions from interested individuals and corporations range from $100 to $3000. The committee requests additional cash contributions to its program. Checks should be made payable to WELDING RESEARCH COMMITTEE, 29 West 39th Street, New York.

THE EFFECT OF SULPHUR ON THE Welding of Steel*

A Review of the Literature to July 1, 1937

By W. SPRARAGEN† and G. E. CLAUSSEN‡

* *This report is prepared under the auspices of*

THE LITERATURE DIVISION OF THE ENGINEERING FOUNDATION—WELDING RESEARCH COMMITTEE

TABLE OF CONTENTS

	Page
Summary	44
Introduction	44
General Observations	45
Oxyacetylene Welding	46
Forge Welding	48
Arc Welding	48
Atomic Hydrogen Welding	48
Resistance Welding	48
Flame Cutting	48
Acknowledgments	49
Bibliography	49
Suggested Research Problems	49

Summary

The sulphur content of steel is usually limited to a maximum of 0.05% in open-hearth and electric steel and 0.08% in Bessemer steel, but may be as high as 0.30% in free-machining steels. Unless sulphur is added intentionally, sulphur above about 0.05 or 0.08% may be an indication of poorly refined steel.

Experimental evidence is not sufficient to justify the conclusion that sulphur below 0.05% adversely affects the quality of the weld made by arc, gas or resistance welding. If there are sulphur segregations even below 0.05% they are evidence of the poor quality of the steel and of the probable presence of elements or conditions, such as oxides or slag inclusions, which may be prejudicial to satisfactory welding.

The Effect of Sulfur on the Welding of Steel

INTRODUCTION

The sulphur content of steel is usually limited to a maximum of 0.05% in open-hearth and electric steel and 0.08% in Bessemer steel, but may be as high as 0.30% in free-machining steels. Unless sulphur is added intentionally, sulphur above about 0.05% or 0.08% may be an indication of poorly refined steel. Although sulphur boils at 445° C., it remains combined as sulphide in solid and liquid steels. In the basic open-hearth process sulphur is removed from the bath by lime. High temperature and high CaO concentration in the slag favor removal of sulphur from the metal. Acid slags (high SiO_2 or FeO) do not remove sulphur.

Unless there are other elements to combine with sulphur, iron sulphide will be formed. Steel containing appreciable quantities of iron sulphide (0.02 S combined as FeS) is brittle at forging temperatures, that is, red short. If the steel contains over 0.2% Mn the sulphide is an iron-manganese sulfide, which does not create red shortness.

Nevertheless, in the presence of FeO in the steel, the manganese may be oxidized and made unavailable for MnS. Besides, depending on the content of Mn and S, the inclusions in the solidifying metal may be largely FeS which is converted to MnS on reheating. Sulphur segregates to the center and top of the ingot, and is practically insoluble in solid steel.

In rimming steel ingots the sulphur content of the rim zone may be only one-half that of the center, the increase in sulphur content occurring mainly in the zone of secondary blow-holes.

Apart from its effect as a solid inclusion in steel, sulphur has no effect on the hardening properties of steel. Above 0.15%, sulphur decreases the strength and ductility; if the sulphur is segregated, much smaller amounts may produce low ductility or impact value.

The present review of literature is an attempt to assess the effect of sulphur on the welding of steel from the standpoints of both the weld puddle and the mechanical properties of the weld. So far as possible each welding process is dealt with separately.

† Secretary, Welding Research Committee.
‡ Research Assistant, Welding Research Committee.

GENERAL OBSERVATIONS

As Dorey[1] points out, it is not so much the amount of sulphur in the steel that is important for welding, as the form in which the sulphur appears. The sulphur in weld metal is uniformly distributed as unusually small sulphide particles. For example, Lewis[2] compared sulphur prints of a rimmed plate and a rimmed electrode with a print of the resulting V butt weld. Both plate and electrode contained a high-sulphur core, as shown by the prints, but there was no evidence of sulphur in the print of the weld because the rapid cooling of the weld yields a uniform distribution of fine sulphides. The weld contained more sulphur than base metal (no details). The same conclusion was reached by Leroy[3] (0.035 S in weld, 0.033 S in plate) and by Schuster,[5] who found that a weld containing 0.07 S failed to darken the paper (3% or 10% H_2SO_4). However, brown markings were observed with a weld containing 0.19 S.

In Hoyt's[4] tests bare electrode deposits with 0.025 S exhibited no reaction in Baumann and cadmium-copper prints because the sulphur was finely subdivided. The only weld which he found to give a positive reaction in printing contained "a combination of FeO and FeS." According to Hoyt, the cause of the hot shortness of bare electrode deposits is FeO, although sulphur may intensify the effect. His conclusion rested on bend tests of welds at 1050° C., in which the bare electrode deposits failed badly. Hoyt believed the failures were not caused by sulphur because sulphur causes hot shortness only at 1325° C. Other authorities place the hot shortness ranges of high sulphur steels at 800 to 1000° C. and above 1200° C. Other references to hot shortness are given in later sections.

Despite the fact that the sulphur in fusion welds is not segregated, welding may cause a change in form of the sulphur in steel. The Lincoln Handbook[6] states that segregated MnS (manganese sulphide) creates gas pockets at the fusion line (no details). As pointed out by E. W. P. Smith (private communication, November 1938), high-sulphur (and high-phosphorus) segregations in base metal are the most frequent source of inferior welds. Although the plate may contain only 0.04 S as shown by analysis, segregated areas may contain up to 0.15 S, and cause porosity and small cracks in adjacent weld metal. The difficulty can be reduced by melting as little of the segregated area as possible into the weld. Chapman[59] refers in a similar way to the segregations of sulphur just beneath the rim in rimming steel. When the zone of high sulphur content is fused during welding small gas bubbles may be formed, which appear as cracks or lack of fusion at the boundaries of the weld deposit, and are among the major causes for rejection of a welded pressure vessel.

In chromium-molybdenum aircraft steels it is well established that the sulphur content of the steel may appear as an embrittling network of grain boundary sulphide (no details) in the heat-affected zone close to the weld, as shown by Bollenrath and Cornelius.[7] Their crack sensitivity tests (see review of literature on Welding Chromium Steels, AMERICAN WELDING SOCIETY JOURNAL, **17** (7) Suppl. (1938), Table 39) failed to show more than an irregular trend toward higher crack sensitivity as the sulphur content was raised from 0.01 to 0.04%. In crack sensitivity tests on gas and arc welding 0.08 inch sheet containing 0.25–0.26 C, 0.5 Mn, 1 Cr, 0.2–0.3 Mo, Cornelius[8] found that steels with 0.034 P, 0.033 S had about the same crack sensitivity as steel with 0.011 P, 0.048 S. Both were worse than a steel containing 0.23 C, 0.02 P, 0.005 S.

These investigators cast one-half of a 550-lb. melt of aircraft steel from a high frequency furnace into a mold without addition of sulphur. The ingot from the other half contained 0.076 S as a result of intentional sulphur additions. Both ingots were rolled to sheet 0.04 inch thick and subjected to crack sensitivity tests, Table 1. The results show that an exceptionally high sulphur content is harmful, even in a steel that has been carefully refined in other respects. Nevertheless, the effect of the high sulphur content was not so great as Bollenrath and Cornelius had expected. They concluded that it was best to limit the sulphur content of Cr-Mo aircraft steel to 0.02%. Bardenheuer and Bottenberg[9] also found that sulphur was only one of several important variables contributing to crack sensitivity, of which the most important was the refining treatment. Bollenrath and Cornelius were aware that in other countries than Germany up to 0.04–0.05% S was permissible in aircraft steel, and felt that some other factor than sulphur was at fault.

On the other hand, Müller[10] found that plain-carbon aircraft steel was resistant to cracks if the steel contained 0.20 C and not over 0.040 S + P, but with 0.30 C the sum of the sulphur and phosphorus contents had to be below 0.020%. The sulphur content rather than the phosphorus was the troublesome factor. Yet Müller[11] was not satisfied that his results could not be explained by other factors than sulphur. For one thing, he found that the S + P relationship did not hold for alloy steels. Furthermore, he was unable to determine the composition of the inclusions in his steels, and it was the inclusions he believed were responsible for cracks. If sulphide was assumed to be the cause of weld cracks, Müller believed that the iron-iron sulphide eutectic, which he stated melted at 985° C. (more likely 1050° C.), was the active agent.

The strongest evidence for the effect of small amounts of sulphur in increasing the sensitivity of plain carbon and Cr-Mo aircraft sheet 0.04 inch thick to welding cracks has been advanced by Werner,[12] Fig. 1. A part of the results in Fig. 1 was obtained from a large number of electric-furnace Cr-Mo steels containing 1 Cr, 0.25 Mo, 0.3 Si, 0.5 Mn, a part from unalloyed hot rolled and Izett steels and a part from special basic open-hearth steels (0.19 to 0.49 C, 0.85–1.3 Mn, 0.18–0.30 Si, 0.024–

Table 1—The Effect of Sulphur Content on the Sensitivity to Cracking. Bollenrath and Cornelius[7]

	Chemical Composition, %							Cracks—% of Length of Weld		
	C	Si	Mn	P	S	Cr	Mo	Rigid Assembly Test[1]	Fillet Bend Test[2]	Torch Moved Over Sheet from Center to Edge[3]
Sheet *A* without sulphur addition	0.24	0.25	0.60	0.019	0.006	0.99	0.19	0; 0; 0; 0;	0; 0;	0; 0; 0; 0; 0; 0;
Sheet *B* with sulphur addition	0.24	0.25	0.61	0.019	0.076	0.97	0.19	18; 1; 12; 7; 6; 6; [8]	16; 16;	0; 28; 0; 35; 0; 40; [17]

[1] Length of weld = 1.8 inches.
[2] Length of weld = 7.1 inches.
[3] Length of weld = approximately 2.4 inches.
Figures in brackets are averages.

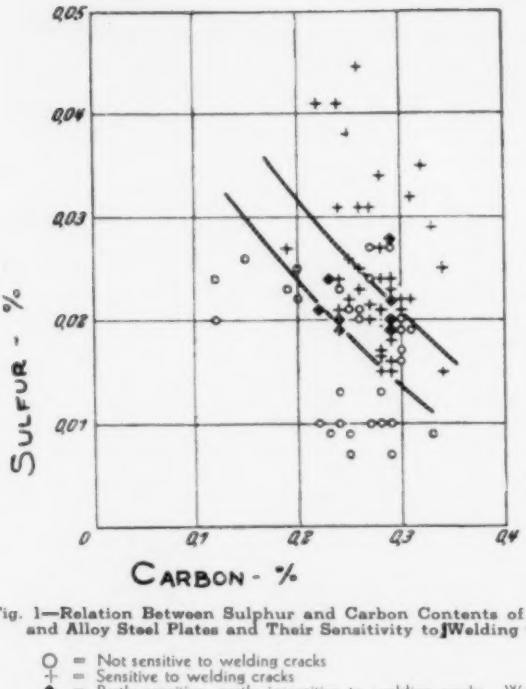


Fig. 1—Relation Between Sulphur and Carbon Contents of Unalloyed and Alloy Steel Plates and Their Sensitivity to Welding Cracks

○ = Not sensitive to welding cracks
+ = Sensitive to welding cracks
◆ = Partly sensitive, partly insensitive to welding cracks. Werner[13]

0.039 S, with or without 0.63–1.02 Cr and 0.37–0.46 Mo) that had been subjected to deoxidation by manganese (no details). The lower line in Fig. 1 applies to the ordinary steels, the upper line to the steels that had been deoxidized by the special method. Clearly the sulphur content was an important factor even in the special steels.

On the other hand, Scherer's[13] tests, Table 2 (thickness of sheet not stated) showed that only if the sulphur

Table 2—Effect of Sulphur Content on Crack Sensitivity of Cr-Mo Steel Sheets. Scherer[13]

Composition of Steel, %							Cross-Shaped Fillet Weld Test. Length of Weld = 8 Inches. Length of Cracks in Inches		Focke-Wulf Clamp Weld Test. Crack Sensitivity
C	Si	Mn	P	S	Cr	Mo	Welded	Welded & Bent	in %
0.25	0.22	0.66	0.012	0.008	0.95	0.18	0	0	0
0.25	0.32	0.52	0.012	0.008	0.98	0.33	0	0	0
0.25	0.24	0.65	0.013	0.021	0.96	0.18	0	0.08	1.4
0.25	0.29	0.52	0.011	0.048	0.98	0.33	2.8	4	46*

* Cracks extended through the entire thickness of the sheet.

content exceeded the usual limits did it affect the crack sensitivity. The melt was made in a 13-ton arc furnace. After part of the melt had been poured, iron sulphide was added to the remainder.

It should be emphasized that the difficulty with sulphur in aircraft steels is confined to Germany, where opinions and results of different authorities are by no means in agreement. In this country, J. B. Johnson[14] stated that up to 0.05 S has no effect on welding.

The arc welding of high-sulphur free-machining steels $^3/_8$ to $^7/_8$ inch thick to itself or to low-sulphur steel is not feasible, according to Rolfe.[15] Good welds could not be obtained in the steels listed in Table 3 with gas-shielded electrodes. There was an evolution of SO_2 in the plastic weld metal at a few points along the bead that gave rise to deep holes. Rolfe placed the sulphur limit for arc welding at 0.08%. There was no difficulty in oxyacetylene

Table 3—Steels Unsuitable for Arc Welding. Rolfe[15]

C	0.22	0.17	0.17	0.13	0.22	0.20	0.21
Mn	0.90	0.97	1.02	0.94	1.06	0.96	0.79
Si	0.008	0.009	0.009	0.023	Trace	0.016	0.008
P	0.057	0.051	0.054	0.052	0.065	0.052	0.061
S	0.27	0.260	0.265	0.230	0.260	0.265	0.245

welding the high-sulphur steel. Routine tests have been made by Rolfe (private communication, September 1938) in which a few inches of arc weld metal is deposited on every bar of free-machining steel. The high-sulphur crater effect was detected in only two steels containing 0.30 and 0.24% S, respectively, the manganese content being 0.89% in each. On the other hand, Pagel[16] could not weld high-sulphur free-machining steels by either arc or oxyacetylene process. He had no difficulty in spot welding these steels. Provided suitable electrodes are used, Dorey,[1] Reeve and Swinden[17] agree that there is no necessity for specifying sulphur (or phosphorus) below 0.05% max. in boiler steel (0.15–0.25 C, 0.4–0.5 Mn).

Spatter from rods in fusion welding has been attributed (without evidence) by Atkins[18] and Booer[19] to either the expansion of easily fusible sulphides on melting in the solid steel or to the oxidation of MnS, which is supposed to occur with explosive violence.

OXYACETYLENE WELDING

In the preceding section it was mentioned that Rolfe and Pagel had opposite experiences in welding high-sulphur steels. Pomp's[20] tests suggest that segregation may have been a factor. In oxyacetylene welding strip into pipe $^3/_4$ inch diameter, $^1/_{16}$ inch wall, he found that a steel containing 0.07 C, 0.068 S, 0.074 P that was not segregated produced a satisfactory weld, whereas a tube with 0.08 C, 0.065 P, 0.024 S did not, because the impurities were badly segregated, and there were blow-holes and poor fusion. The puddle bubbled in the latter steel if an oxidizing flame was used. In 1921 Diegel[21] found that good oxyacetylene welds could be made in plate *C*, Table 4, with a Swedish iron rod, but welds in the other steels were not satisfactory. The sulphide inclusions formed a chain or line of weakness at the fusion line.

Table 4—Steels 0.63 Inch Thick Used by Diegel[21]

Steel	C	Mn	Si	P	S	Ni	Notes
A	0.12	0.45	0.01	0.049	0.061	0.06	Low-grade welds
B	0.15	0.42	0.27	0.015	0.045	0.04	Low-grade welds
C	0.12	0.32	0.01	0.017	0.018	0.25	High-grade welds

Segregation of sulphur in the welding rod may be troublesome. Kalisch[22] tested two rods (C ≦ 0.1, Mn ≦ 0.5, P + S = 0.6) of which one was segregated, the sulphur being at the center. Drops melted from the segregated rod were full of blow-holes; drops from the other rod were not. The upper part of welds made with the segregated rod also contained blow-holes. A German blowpipe manufacturer[23] stated that filler rods for mild steel should not have over 0.035 S. If the rod contained no silicon, the manganese content should be 12 times S; with 0.6 Si, Mn = 8 times S. Kemper[24] found that deposits made from rods containing 0.065 S were red short, whereas with 0.03–0.04 S the deposits were not. On account of its high sulphur content, Tris and Kapet-

Table 5—Effect of Hydrogen Sulphide in the Acetylene on Oxyacetylene Welded Mild Steel. Holzhäuser[27]

Volume % H_2S in Acetylene	Composition of Welded Metal, % C	Mn	P	S	Cu	Yield Strength, Lb./In.²	Tensile Strength, Lb./In.²	Rockwell Hardness, B Scale	Bend Angle, Degrees
0.00	0.04	0.28	0.019	0.028	0.23	37,600	42,400	63.5	142
0.05	0.03	0.27	0.017	0.024	0.25	37,400	41,700	59.9	140
0.1	0.04	0.29	0.018	0.026	0.25	36,500	40,300	61.3	141
0.25	0.05	0.29	0.024	0.036	0.22	37,400	38,300	64.7	124
0.5	0.04	0.27	0.017	0.060	0.22	38,400	37,900	60.0	105
1.0	0.05	0.27	0.017	0.078	0.22	....	34,700	59.2	50
2.0	0.06	0.25	0.017	0.156	0.22	....	26,400	57.1	16

ensky[25] in 1919 found that a rod with 0.05 C, 0.43 Mn, 0.082 S was unsuitable for oxyacetylene welding. Diegel[26] believed that rods with over 0.03 S should not be used, but the manganese content of his rods was low. He found that a deposit containing 0.11 C, 0.031 Mn, 0.024 S had low ductility and attributed the effect to the greater influence of sulphur on cast structures than on rolled materials. Even with 0.3 Mn, a deposit with 0.04 S had bad ductility. It is probable that other factors in addition to sulphur should have been considered by Diegel.

A number of foreign investigators has studied the effect of sulphur as an impurity in the acetylene. Holzhäuser[27] found that the pick up of sulphur from acetylene was proportional to the volume percentage of H_2S in the acetylene, Table 5. Dry generator acetylene and tank oxygen (99.2% purity) were used. The ratio of oxygen to acetylene by volume was 1.06 to 1 in all tests. Butt welds, 60° V, 11 inches long were made manually by an expert welder in dead mild steel 0.24 inch thick with a rod containing 0.07 C, 0.34 Mn, 0.0 Si, 0.020 P, 0.022 S, 0.20 Cu. Four welds were made with each gas mixture. The mechanical properties were determined on flush machined specimens (German standard). The hardness was measured in the center of the cross section of the weld.

Up to 0.1 volume % H_2S had no effect on welding. As the H_2S content increased from 0.25 to 0.5 vol. % the bubbling increased. The flame was blue at 1.0 vol. % H_2S, the weld foamed and the slag was voluminous but was not abnormally viscous. In this connection, Kautny[28] observed that oxyacetylene weld metal was made more fluid by sulphur, and Coulson-Smith[29] remarked that the deposit from rods containing over 0.05 S was likely to boil. With 2.0 vol. % H_2S, Holzhäuser found that the slag became viscous and the foaming was violent. There was a pronounced odor, whether of H_2S or SO_2 was not stated.

There was nothing unusual about the microstructure of the deposits until 1.0 vol. % H_2S was added to the acetylene. The grains were coarse and quantities of slag were observed in the fusion zone. This condition was accentuated at 2.0 vol. % H_2S; sulphide inclusions were observed in the weld. The strength and ductility were not affected by up to 0.1 vol. % H_2S. With higher sulphur contents the strength and ductility steadily decreased.

The effect of phosphine added to acetylene containing hydrogen sulphide is discussed in the review of literature on the Effect of Phosphorus on the Welding of Steel. The linear relationship between the percentage of sulphur and phosphorus in the acetylene (the weight percentages of S and P in the gas were practically identical in each test) and the percentages in the weld metal, is not in agreement with Herty's data on the sulphur content of open-hearth gas neutral to scrap of a given sulphur content. Herty showed that the percentage of sulphur absorbed from the gas by the molten bath increases more rapidly than linear with the sulphur content of the gas. However, Herty's metal absorbed much less sulphur than Holzhäuser's weld metal from a gas of given weight percentage sulphur (about 0.5 wt. % S). Bischof in 1933 reported a linear relationship between sulphur content of open-hearth gas and sulphur content of bath, and believed that the sulphur was picked up through SO_2 rather than H_2S in the open hearth.

According to a German laboratory,[30] sulphur in acetylene may or may not be absorbed by the weld puddle depending on the sulphur, hydrogen and oxygen contents of the gas. As a general rule, Table 6, with a ratio of oxygen to acetylene of 1.1 or 1.2 to 1, the weld metal has slightly lower sulphur content than the mild steel welding rod, if commercially pure acetylene is used. As explained in the section on Oxyacetylene Welding in the review of literature on the Effect of Phosphorus on the Welding of Steel, Rimarski[31] found no pick up of sulphur from an acetylene containing 0.01 gm. sulphur per 100 liters. The rod and base metal contained 0.028–0.03 S, the V butt welds in plates 0.08 to 0.63 inch thick contained 0.024–0.027 S. Baumann prints revealed no difference between welds made with pure and impure gas. Rimarski thought pick up of sulphur from the flame was less rapid than pick up of phosphorus.

Table 6—Recovery of Sulphur in Oxyacetylene Welding Mild Steel. Reichsanstalt[30]

	Commercially Pure Acetylene		Acetylene with 0.05 Vol. % Hydrogen Sulphide	
Sulphur in rod, %	0.037	0.039	0.039	0.039
Sulphur in deposit, %	0.032	0.034	0.045	0.046

In contrast with Rimarski's results, a German welding laboratory[32] showed that nearly all the sulphur in the acetylene is absorbed by the weld metal (mild steel). As high as 0.2 S was observed in some welds. Welds made with acetylene containing 0.04 wt. % S were definitely red short, whereas welds made with purified acetylene could be bent 180° at a red heat without cracking. Pothmann[33] stated that 0.01% H_2S and 0.01% organic sulphur compounds, such as COS, are the maximum contents permissible in acetylene, if red short weld metal is to be avoided. On the other hand, a French Acetylene Committee[34] were of the belief that the limit could be set as high as 0.15 vol. % H_2S. An unusual explanation for high sulphur content in generator acetylene during cold weather was evolved by Loch.[35] The high sulphur

content was picked up by the weld metal and caused cracks.

FORGE WELDING

Sulphur below 0.05% is not harmful in forge welding iron and steel, according to Speller.[36] Sometimes higher sulphur contents have created no difficulties. Obrapolski[37] studied a forge welded chain which contained 0.112 S, 0.134 P and had segregations of oxide and carbon (up to 0.41 C). The chain failed in service. Sulphur was considered by Howe[38] to be fundamentally bad for forge welding, but Schäfer[39] stated that, within the usual limits, sulphur has no effect.

In water gas welding, Fry[40] found sulphur a disadvantage on account of red shortness. Diegel's[41] tests on water gas welded steel containing 0.1 C, 0.4 Mn, 0.01 Si showed that 0.08–0.11 S was sufficient to spoil the welding of a steel containing 0.10–0.13 P. The phosphorus alone had little effect (see review of literature on the Effect of Phosphorus on the Welding of Steel for a discussion of Bessemer steel).

High-sulphur segregated areas provide nuclei for cracks in water gas welding. A boiler plate containing 0.021 S, 0.025 P could not be successfully water gas welded by Pomp,[42] who found 0.042 P, 0.051 S in a segregation. Regè[43] observed that, in water gas welding a rimmed steel the outer layers of which contained 0.255 C, 0.056 S and the inner layers 0.143 C, 0.028 S, the segregated impurities (sulphides) collected along the junction line of the scarves. He also pointed out that if the flames strike directly on the sheared edge of a rimmed steel with sulphur segregations, the segregated zone may crack owing to red shortness. Only rolled edges should be exposed because the segregated zone never extends to the rolled edge. Cracking during water gas welding a plate containing 0.033 P, 0.061 As, 0.085 S was attributed by Bauer[44] to the high sulphur content.

ARC WELDING

Apparently arc welders have been so careful to restrict the sulphur content of their rods and base metal that there is little information of importance on the effect of sulphur. The observations of Rolfe[15] and Pagel[16] on the difficulty of arc welding free-machining steel, and of Chapman[59] and Smith on Sulphur segregation have been discussed in the section on General Observations.

It was thought that an examination of published analyses of bare electrode deposits and welds might show whether there is a significant loss of sulphur in depositing bare electrodes. Unfortunately, the majority of investigators did not take sufficient pains to make their results worthy of close consideration. Either the composition of base metal was not given, or it was uncertain whether the electrodes tested were those that were subjected to chemical analysis. In any case, the results show that there is no average loss in sulphur greater than 0.005% in depositing bare electrodes containing up to 0.04% S, unless, as Losana[45] points out, the welding conditions are unusually oxidizing. Hobart[46] reported a loss of 40% in depositing a bare, cold rolled electrode containing 0.11 C, 0.72 Mn, 0.097 P, 0.123 S, 0.011 Si. The deposit contained 0.05 C, 0.11 Mn, 0.086 P, 0.072 S, 0.011 Si. Fuchs[47] believed that cracks in bare electrode welds sometimes could be attributed to high sulphur content (low-grade rods).

With covered electrodes, according to Holmberg,[55] sulphur is often lost in welding (no details). Baumgärtel[48] cites a covered electrode whose coating contained 52% CaO and 0.19% S (no further details) the deposit from which contained 0.06–0.09 C, 0.01 Si, 0.40 Mn, 0.015–0.026 P, 0.104–0.132 S. In the absence of information on the thickness of the coating and on the remaining constituents, it is difficult to explain Baumgärtel's result. Sulphur entering the coating from rods pickled in H_2SO_4 and insufficiently washed is picked up by the weld metal, as Krivenko[58] showed. More sulphur was picked up by weld metal from a coating containing 5% $CaSO_4$ than from a coating containing 50 or 100% $CaSO_4$. Krivenko concluded that coatings should not contain over 0.25% $CaSO_4$. Applying iron sulphide as dust on heavy covered electrodes (0.10 C, 0.32 Mn) or as paste with waterglass on cored and bare electrodes, Stieler[49] obtained deposits with 0.15 and 0.40 S, respectively. Both deposits tended to crack in fillet welding, the difficulty increasing with sulphur content. Similar experiments were made by Linetsky,[60] who used coatings containing Na_2SO_4. Cracks appeared in the mild steel weld metal when the sulphur content of the deposit exceeded 0.030–0.035% and the ratio of Mn to S exceeded 5. According to E. W. P. Smith (private communication, November 1938), root beads and fillet weld metal in heavy plate tend to crack if the sulphur content of the weld metal exceeds 0.035%. In 1919 Heaton[50] found that satisfactory carbon arc welds (Benardos process) could not be made in steel containing 0.10 C, 0.40 Mn, 0.073 P, 0.086 S, although a steel with 0.15 C, 0.64 Mn, 0.068 P, 0.025 S gave good results. High sulphur content caused the weld metal to boil, and to solidify as a porous deposit.

ATOMIC HYDROGEN WELDING

Hydrogen is not an efficient agent for removing sulphur from steel. Consequently Weinman's[51] report that the deposit from a mild steel rod containing 0.075–0.150 S contained 0.086 S in atomic hydrogen welding is not surprising (complete details are given in the review of literature on the Effect of Phosphorus on the Welding of Steel).

RESISTANCE WELDING

Although Pagel[16] stated that high-sulphur free-machining steels give no trouble in spot welding, and Burton[44] was certain that very good spot and resistance butt welds could be made in plain carbon steel containing about 0.18 C and up to 0.05 or 0.06 S, Hughes[52] believed that above 0.025 S creates difficulty (no details). Heaton[50] blamed the sulphur content for his inability to resistance butt weld an open-hearth steel with 0.19 C, 0.05 Si, 0.44 Mn, 0.097 S, 0.075 P. Yet good tensile strength was developed by resistance butt welds in a steel containing 0.066 C, 0.00 Si, 0.48 Mn, 0.067 P, 0.070 S, 0.04 Cu, 0.064 As, according to Tafel and Heil.[53] The welds had the same strength as welds in similar steel having a lower sulphur content, but the ductility was not determined. Marchand[57] observed that resistance welds in mild steel containing over 0.05 S boiled during welding and contained blow-holes.

FLAME CUTTING

Wiss[54] found that an iron-sulphur alloy with 35% S could be oxyacetylene flame cut without difficulty. Sparks and SO_2 fumes were plentiful. The flame cutting qualities of plain carbon steel, at any rate, Wiss concluded, were not affected by sulphur.

ACKNOWLEDGMENTS

Contributions to this review have been received from:
R. H. Aborn, United States Steel Corporation
R. T. Rolfe, W. H. Allen Sons & Company, Ltd., Bedford, England
E. W. P. Smith, Lincoln Electric Company
W. A. Spindler, University of Michigan

BIBLIOGRAPHY

1. Dorey, S. F., *Jnl. Instn. Civil Engrs.*, No. **6**, 621–666, April 1937.
2. Lewis, W. E., *N. E. Coast Instn. Engrs. & Shipbuilders. Trans.*, **52**, D41–D42 (1935–36).
3. Leroy, A., *Rev. Soud. Autog.*, **29**, 226–234 (1937).
4. Hoyt, S. L., *Trans. Amer. Soc. Metals*, **23**, 61–84 (1935).
5. Schuster, L. W., *Weld. Jnl.* (London), **29**, 294–298 (1932).
6. *Procedure Handbook of Arc Welding*, Lincoln Electric Co., Cleveland, Fourth Edn. (1936).
7. Bollenrath, F. and Cornelius, H., *Arch. Eisenhüttenwes.*, **10**, 563–576 (1937).
8. Cornelius, H., *Luftfahrtfschg.*, **15** (3) 133–140, March 1938.
9. Bardenheuer, P., and Bottenberg, W., *Arch. Eisenhüttenwes.*, **11** (8) 375–383, Feb. 1938.
10. Müller, J., *VDI Sonderheft Schweisstechnik*, **2**, 14–15 (1936).
11. Müller, J., *Luftfahrtfschg.*, **11**, 93–103 (1934).
12. Bollenrath, F., and Cornelius, H., *Stahl u. Eisen*, **56**, 565–571 (1936). Discussion by O. Werner.
13. Scherer, R., *Arch. Eisenhüttenwes.*, **11**, 446–447, March 1938.
14. Johnson, J. B., *Airplane Welding*, Goodheart-Wilcox Co. Inc., Chicago (1929).
15. Rolfe, R. T., *Iron & Steel Ind.*, **9**, 443–444 (1936); see also "Steels for the User," Chapman & Hall Ltd., London, p. 87 (1937), which illustrates the craters in welds on high-sulphur steel.
16. Pagel, W., *Stahl u. Eisen*, **56**, 861–863 (1936).
17. Swinden, T., *Advance Paper, N. E. Coast Instn. Engrs. & Shipbuilders* (1938).
18. Atkins, E. A., *Weld. Engr.* **7** (7) 32–38 (1922); **10** (1) 23–25, 39–41 (1925).
19. Booer, J. R., *Chem. Trade Jnl.*, **74**, 189–191 (1924).
20. Pomp, A., *Stahl u. Eisen als Werkstoff, Werkstofftagung*, Berlin, **2**, 57–63 (1928).
21. Diegel, C., *Z. VDI*, **65**, 626–629 (1921).
22. Kalisch, E., *Autog. Metallb.*, **23**, 384–387 (1930).
23. *Autog. Metallb.*, **22**, 243–255, 265–270 (1929).
24. Kemper, H., *Schmelzschwg.*, **11**, 156–158 (1932).
25. Tris, P. C., and Kapetensky, M., *Proc. Steel Treating Research Soc.*, **2**, 18–24, 50 (1919).
26. Diegel, C., *Forscharb. Geb. Ingwes.*, **246**, 44 (1922).
27. Holzhäuser, P. K., *Autog. Metallb.*, **23**, 266–275, 286–296 (1930).
28. Kautny, T., *Autog. Metallb.*, **17**, 139–141, 150–154 (1924).
29. Coulson-Smith, C., *Trans. Faraday Soc.*, **20**, 163–167 (1924).
30. *Chem.-Tech. Reichsanstalt Jahresbericht*, **7**, 83–86 (1928).
31. Rimarski, W., Kantner, C., and Streb, E., *Forscharb. Geb. Ingwes.*, **317**, 44 (1929).
32. *Maschinenbau*, **4**, 165–169 (1925).
33. Pothmann, P., *Autog. Metallb.*, **20**, 50–57, 67–73 (1927).
34. *Bull. Officiel Comm. Perm. Intnl. Acetylene*, No. **2**, 37 (1929).
35. Loch, F., *Autog. Metallb.*, **22**, 6–7 (1929).
36. Speller, F. N., *Trans. A. S. M. E.*, **44**, 399–407 (1922).
37. Obrapolski, J., *Technika Cieplna*, **15**, 127–133 (1937).
38. Howe, H. M., *Metallurgy of Steel*, **1**, 41 (1890).
39. Schäfer, R., *Die Konstruktionsstähle und ihre Wärmebehandlung*, Springer, Berlin, 125–129 (1923).
40. Fry, A., *Kruppsche Monatsh.*, **12**, 201–214 (1931).
41. Diegel, C., *Verein Beförderung Gewerbfleisses*, **79**, 233–250 (1918).
42. Pomp, A., *Autog. Metallb.*, **21**, 64–66 (1928).
43. Regè, A., *Metallurgia Italiana*, **29**, 457–472 (1937).
44. Bauer, O., *Mitt. Verein Grosskesselbes.*, **32**, 89–100 (1931).
45. Losana, L., *Metallurgia Italiana*, **26**, 391–403 (1934).
46. Hobart, H. M., *Weld. Engr.*, **3** (9) 40–55 (1919).
47. Fuchs, J., *Z. VDI*, **72**, 1151–1155 (1928).
48. Baumgärtel, K., *Forscharb. Geb. Ingwes.*, **336**, 33 pp. (1930).
49. Stieler, C., *Stahl u. Eisen*, **58** (13) 346–350, March 31, 1938.
50. Heaton, T. T., *Proc. Instn. Mech. Engrs.*, 49–71 (1919).
51. Weinman, R. A., American Welding Society Journal, **10** (10) 12–18 (1931).
52. Hughes, G. A., *Welding*, **3**, 84–87 (1932).
53. Tafel, W., and Heil, W., *Stahl u. Eisen*, **47**, 1010–1011 (1927).
54. Wiss, E., *Autog. Metallb.*, **22**, 46–53, 58–66 (1929).
55. Holmberg, J. C., *Welding*, **4**, 59–62 (1933).
56. Burton, F., American Welding Society Journal, **9** (9) 78–87 (1930).
57. Marchand, H., *Rev. Gén. Sciences pure et appliquées*, **30**, 433–442 (1919).
58. Krivenko, I. N., *Avtog. Delo*, **8** (10) 15–17 (1937).
59. Chapman, E., *Jnl. Amer. Soc. Naval Engrs.*, **48**, 476–483 (1936).
60. Linetsky, I. A., *Vestnik Metallopromish.*, **18** (2) 109–124; (3) 135–149 (1938).

SUGGESTED RESEARCH PROBLEMS

1. Poor ductility in welds with rather high sulphur content is often blamed on the sulphur. Welds in carefully refined steels to which sulphur has been added intentionally in amounts up to 0.15% should be prepared and tested for ductility. Similar tests should be made on weld metal of high quality except for sulphur content. The welds should be examined for porosity to determine whether sulphur-containing gases are the cause of any poor properties that may be found.

2. The solubility of sulphur compounds and gases in the various steels as a function of temperature should bear profitable study to explain the conditions under which gas pockets form. Such a study would have to account not alone for the total sulphur contents involved, but also its condition of segregation.

3. Further studies of rimmed steel in terms of the effect of sulphur segregations are needed.

4. Intergranular sulphide has been observed unexpectedly in welds in steels containing relatively high sulphur. The conditions should be determined under which sulphur in plain-carbon and alloy steels may occur as intergranular films of sulphide in weld metal or heat-affected zone.

5. Is it true that the effect of sulphur is to make the welding arc unstable, because $FeSO_4$ is not a stabilizing substance?

STATIC AND FATIGUE STRENGTH of Welded Reinforcing Bars*

By DR. ING. BÜHLER

IN EUROPE the longest concrete reinforcing bars appear to have been those used in the Dreirosen Bridge at Basel. They were 130 feet long. The usual length of the bars in German highway bridges is 98 feet. Longer bars are obtained by welding or by the use of sleeves. To avoid joints in the bars the concrete structure has sometimes been designed with special appendages under compression to secure anchorage. This solution involves added weight and is not necessarily satisfactory.

Field welding is a little troublesome in that trained operators are essential. So long as the shear load can be taken by bent bars, welding or sleeves present no complications. If, on the contrary, the bent bars are designed to be fully utilized in shear and there is no room for additional bars, the reversed shear stress, as in long-span bridges, may be large. However, fatigue stresses are not usually considered in reinforced concrete design. Nevertheless, in railway bridge design the fatigue limit of reinforcing bars and joints is often involved so that the same design procedure is necessary as for steel structures.

Static and fatigue tests were made on a low-alloy steel containing 0.25–0.30 C, 0.6–0.8 Mn, <0.35 Si, 0.4–0.5 Cr, <0.035 P, <0.035 S. The fatigue tests were made in an Amsler pulsator at 300 cpm. The results of static ten-

* Abstract of Rundstahl für Eisenbetonbauten, published in *Beton u. Eisen*, **37** (16) 258–263, Aug. 20, 1938. The tests were made at the Rollschen Eisenwerken, Gerlafingen, Switzerland under the direction of Dr. Dübi and Dr. Frankhauser.

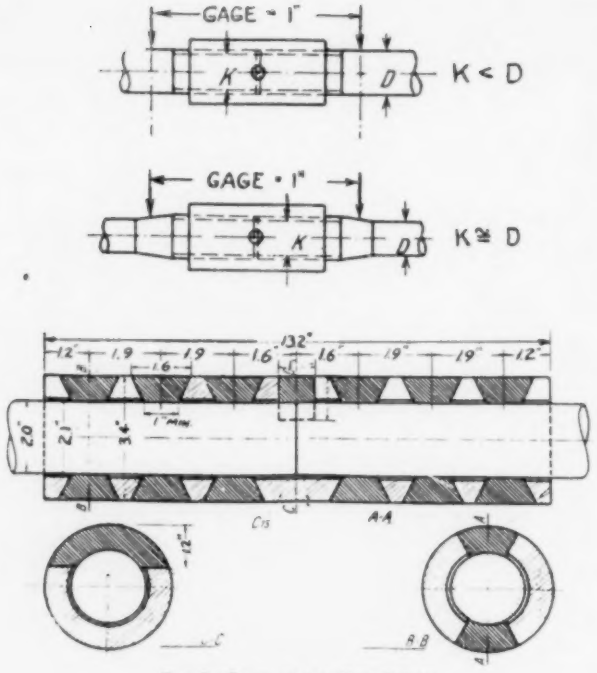


Fig. 1—Some of the Test Joints

Upper—Threaded joints
Lower—Plug welded sleeve joints

sion tests for as-rolled, unwelded bars 1.0–2.0 inches diameter were:

Proportional Limit = 51,400 lb./in.²
Yield Strength = 53,000 lb./in.²
Tensile Strength = 81,700 lb./in.²
Elongation = 25.4% in 10 diameters.

The fatigue strengths of unwelded bars for 5 million cycles were: Pulsating tension (P/2 to P) with mill scale = 55,400 lb./in.² Pulsating tension (0 to P) with mill scale = 41,200 lb./in.² Pulsating tension (0 to P) fine machined surface = 44,000 lb./in.² Rotating bend, unnotched, fine machined surface = 39,800 lb./in.² Rotating bend, notched, fine machined surface = 29,800 lb./in.² The pulsator tests were made on bars 1 to $1^1/_2$ inches diameter, with or without a parallel milled section. The rotating bend specimens had a minimum diameter of 0.42 inch. The fracture in specimens with mill scale started in the mill scale.

The results on joined bars, Fig. 1, are shown in Table 1. Covered electrodes were used for the arc-welded joints. The stress-strain diagrams for a diagonal butt-welded joint and a butt weld with reinforcement, Fig. 2, showed that ample yield value was developed. The screwed joints exhibited initially rather large permanent deformations, in contrast with the welded joints.

The well known effect of mill scale in reducing the fatigue limit of unwelded steel was confirmed. The pulsating tension fatigue strength (0 to max.) of the joints at 1 million cycles was 22,700 lb./in.², decreasing to 20,000 lb./in.² at 5 million cycles. Hence, the joint has a greater effect than mill scale in decreasing the pulsating tension fatigue strength. It should be noted that fatigue strength of the welded joints was determined by the method of successive increments of load, which usually yields a fictitiously high fatigue value. Therefore, the fatigue results must be regarded as approximate. Preliminary tests indicate that the fatigue strength of butt-welded joints can be raised 10% by grinding off the reinforcement. Resistance welded joints were not tested in fatigue, but it seems likely that they will have a high fatigue strength corresponding to the high fatigue strength of butt-welded rails.

Table 1—Static and Fatigue Tests of Joined Reinforcing Bars

Joint	Yield Strength, Lb./In.²	Tensile Strength, Lb./In.²	Elongation, %	Reduction of Area, %	Pulsating Tension Fatigue Strength (5 Million Cycles) Lb./In.²
Resistance butt weld in bars 1.6–2.0 inches diameter, average of 12 specimens	50,100 to 54,000 52,000	82,800 to 85,800 84,500‡	8.0 to 19.8 17†	3 to 57	.....
Arc-welded sleeve joint, average of 12 specimens	51,700 to 53,500 53,200	82,900 to 86,200 85,200**	8.6 to 15.0 12*	55 to 59	.....
Butt weld with covered electrodes, average of 2 specimens, 2 inches diameter	52,100	83,400[1]	19†	46	20,000 (0 to P) (specimens 8 and 9, Fig. 2; fracture occurred at start of weld)
Screwed sleeve joints with upset ends,[3] 5 specimens, 1.8–2.0 inches diameter	48,600 to 53,200 (based on nominal bar diameter)	83,500 to 87,600[1] (based on nominal bar diameter)	11.6 to 14.4	51 to 53	
Screwed sleeve joints with plain ends,[3] 5 specimens, 1.5–1.7 inches diameter average	39,600 to 42,500[2]	68,200 to 71,900[2]	5.2 to 5.8	15 to 19	20,000 (0 to P); 22,700 (P/2 to P)—(averages of 5 specimens, all of which fractured in the threads)
Screwed sleeve joints with plain ends,[4] 1 specimen, 1.6 inches diameter	44,000[2]	73,000[2]	.....	.....	

* Measured over the sleeve.
† In 2.4–3.2 inches.
‡ All but 3 specimens (one with 8% elong.) fractured outside the weld.
** Seven specimens fractured outside the sleeve.
[1] Fracture outside joint.
[2] Calculated on cross section of unthreaded bar; fracture in threads.
[3] 60° V thread with rounded peaks and roots.
[4] 30° Whitworth thread.

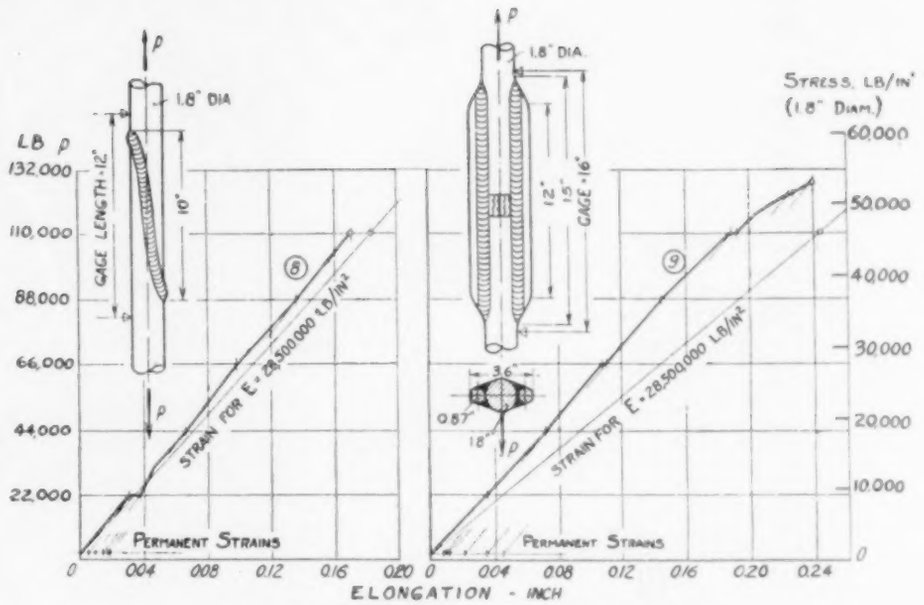


Fig. 2—Stress-Strain Diagrams of Welded Joints 8 and 9

A = elongations calculated for Young's Modulus = 28,500,000 lb./in.², and diameter = 1.78 inches. Stress is also based on a diameter of 1.78 inches

All in all, the results show that welded joints in reinforcing bar are entirely satisfactory where fatigue is a minor factor. If fatigue is important, joints of any sort should be used with caution, long unwelded bars being preferable to several short lengths screwed or welded together.

DEFLECTION TESTS ON THE Welded Steel Frame of Lagunita Court*

By **RICHARD A. HATTRUP**
and **HOWARD A. RUSSELL**

INTRODUCTION

IT IS recognized by those in the building industry that there has been a need for a type of steel construction satisfactory for intermediate buildings of one or two stories. At Stanford University a multiple unit dormitory has been constructed of light welded steel frames. This new type of construction, which was developed by the Soulé Steel Company, may well fill the need for the intermediate type building.

The building elements of the dormitory are studs and joists spaced as they usually are in wood construction. The studs and joists, however, are shallow steel trusses. Novel features of the building are that the steel used in the members was, for the most part, only one-sixteenth or one-eighth inch thick and all joints were made by arc welding.

By using this type of construction instead of the common wood framing several advantages are gained. A rigid type of construction is obtained which is, if properly designed, highly efficient in resisting earthquake shocks. The frame cannot be attacked by termites and it is more fire-resistant, thus reducing (fire) insurance rates. The annoying problem of shrinkage, ever present in wood construction is eliminated. Because of the open web construction of the joists, a convenient location is given for placing conduits and pipes. Perhaps one of the greatest advantages is the speed of erection which can be obtained—the frames of the four buildings at Stanford were erected in only thirty days. There is some mention of a disadvantage in that sound is transmitted easily throughout the framework, but the seriousness of this is questioned, as it is a difficulty which can be easily overcome.

The new features used in this type of construction originate several unusual design problems. The different building elements are connected with welded joints, forming a system of rigidly connected beams and columns. The restraining action of the rigid frame causes end

* Résumé of a thesis, supervised by Prof. A. S. Niles, submitted to the Department of Civil Engineering of Stanford University in partial fulfillment of the requirements for the Degree of Engineer, June 1935. Contribution to Fundamental Research Division of Welding Research Committee.

Fig. 1—Detail of Ames Dial Connection

moments which in turn affect the loads in and deflections of the units. The same situation is not so pronounced in other forms of construction because ordinarily the joints are not made as rigid as in this case where welding was used. Also, the welding of all the joints of the trusses causes secondary stresses in the individual members. Whereas the secondary stresses in trusses of relatively unimportant structures are usually neglected, the possibility of eccentric connections and the size of the members as compared with the truss depth makes the secondary stress problem an important one.

In June 1934, during the construction of the building, the authors of this thesis made several load-deflection tests upon a bent of the building. Both horizontal and vertical loads were used in order to obtain a complete set of data for the behavior of the frame. The data should make possible a complete study of the action of the structure. This thesis is not intended to be a complete study of the various design problems, but rather a progress report covering only the problem of determining the magnitude of the rigid frame action, which seems to be the one requiring the earliest solution.

To solve the problem of the magnitude of the rigid frame action the Hardy Cross method of moment distribution seemed most logical. The simplicity of the bents of Lagunita Court indicated that if this method could be applied to a bent consisting of trusses in place of the usual beams, the analysis would be relatively simple. However, it was necessary to determine the elastic characteristics of the trussed joists and studs before the Hardy Cross method could be used.

FIELD TESTS ON LAGUNITA COURT

The deflections of the steel frame were measured with Ames dials which were supported on an entirely independent wood frame. A second platform independent of both the steel frame and that supporting the Ames dials, served as a scaffolding for the observers to stand on. By having the two auxiliary wood frames it was possible for the observers to move about without affecting any of the dial readings. The details of the assembly of the dials and their connection to the frame are shown in Figs. 1 and 2.

For the vertical loads, platforms were hung directly from the joists. Figure 3 shows a detail of the manner in which the cable loads were brought into the joists. For horizontal loads, the cable was carried over a pulley and attached to the building by means of an eye bolt which was threaded into a nut welded into the steel frame. This particular detail was used so that the eye bolt could be left in position for making another horizontal load test after the building was completed. Figure 4 shows the derrick and assembly for applying horizontal loads.

Loading tests of both the second floors' joists and the ceiling joists were made with the diagonal bracing in and out of the bent. Figure 5 shows the diagonal bracing. The tests were made by applying the loads at the third points of the room span joists—first on one side of the corridor, then on the other side, and finally on both sides at the same time. Because a scaffolding for supporting the Ames dials was built only on one side of the corridor it was necessary to load each side separately in order to obtain deflections on both the side which was loaded and on the opposite side. The same tests that were made for the second floor span were repeated for the ceiling joists (Fig. 6). Horizontal load tests were made with and without and diagonals and also after the building was completely finished. The horizontal loads were applied at the second floor and at the ceiling of the second floor.

The field tests were made under rather difficult circumstances as the construction of the building was being rushed in order to complete the work within the required time limits and workmen were constantly at work in other parts of the building. Furthermore, the time during which the tests could be made was a period of very warm weather.

The tests were made by adding loads in increments of 50 to 200 pounds until the load reached a maximum, after which the load was taken off in similar increments. After each addition or deduction of load was completed and the vibration of the structure dampened out the dials were read. During each test as great a number of observers were placed upon the project as possible so that no observer had to move about any more than necessary

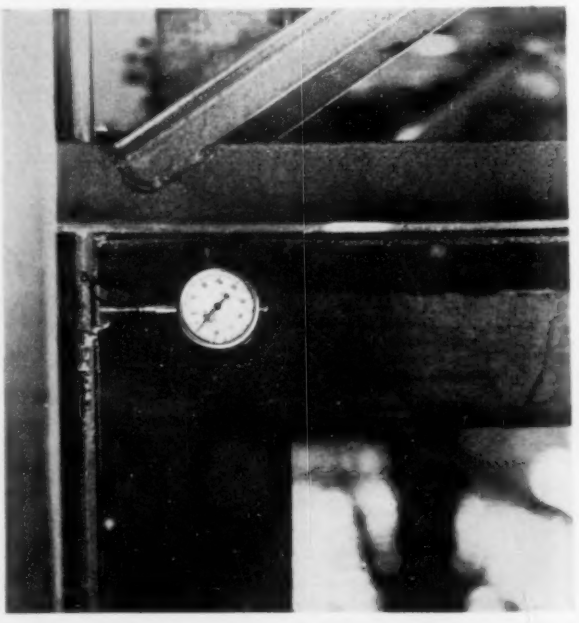


Fig. 2—Detail of Ames Dial Connection

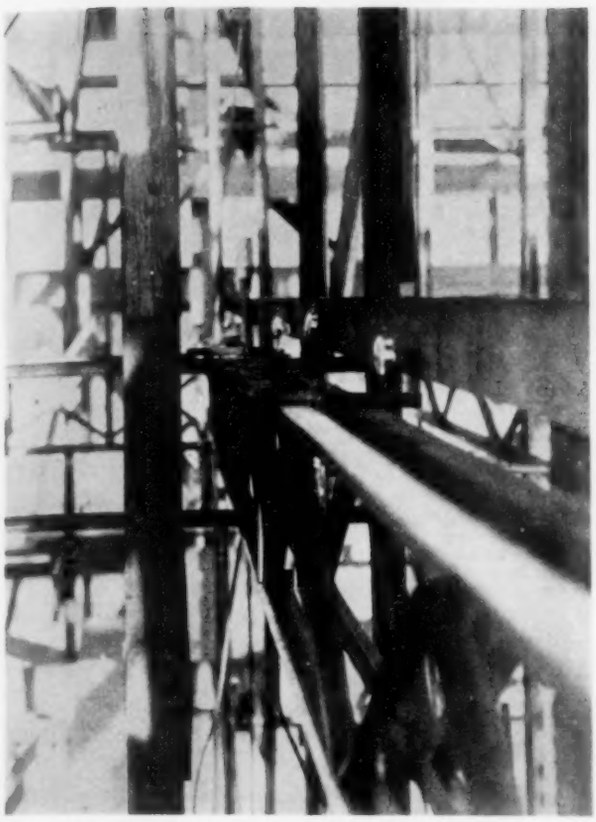


Fig. 3—Method of Applying Vertical Loads to Joists

to read all of the dials he was charged with. The latter precaution was found to be necessary for no matter how carefully the men moved there was always the danger of someone striking the frame supporting the dials.

For analytical determination of joist characteristics see Fig. 7.

COMPARISON OF FIELD TESTS AND ANALYTICAL COMPUTATIONS

In order to compare the results of the analytical determination of moment and the field tests it was necessary to compute the deflections of the second floor joist under

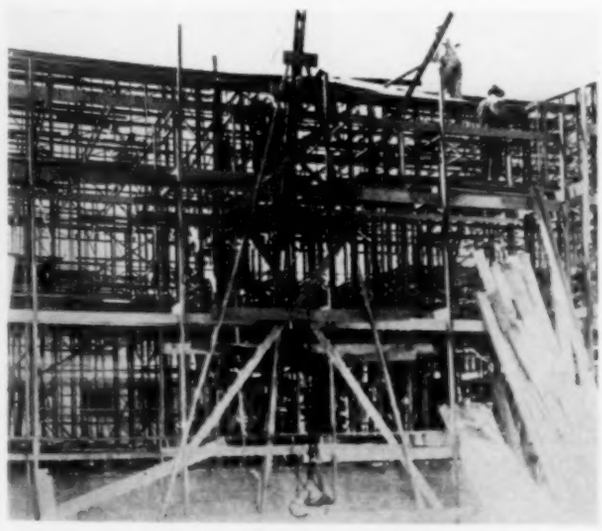


Fig. 4—Derrick for Application of Horizontal Loads

equal third point loads of 1000 pounds each and under the restraining end moments found by moment distribution analyses of the bent. The results are tabulated in Table 1.

Table 1—Comparison of Computed and Measured Deflections

Second Floor Joist—Inches Per 100 Pounds

Position	Computed Deflection Neglecting Resisting Moment	Computed Deflection Including Resisting Moment	Measured Deflection Increasing Loads	Measured Deflection Decreasing Loads
Outside 1/3 pt.	0.2240	0.1494	0.1352	0.1342
Middle	0.251	0.161	0.1495	0.1475
Inside 1/3 pt.	0.2240	0.1456	0.1527	0.1297

The results of column 2, although they diverge by approximately 10 per cent from the results of column 3, are very much closer than the deflections shown in column 1. Part of the divergence between column 2 and 3 is probably due to neglect of the stiffening effect of the welded joints in computing deflections.

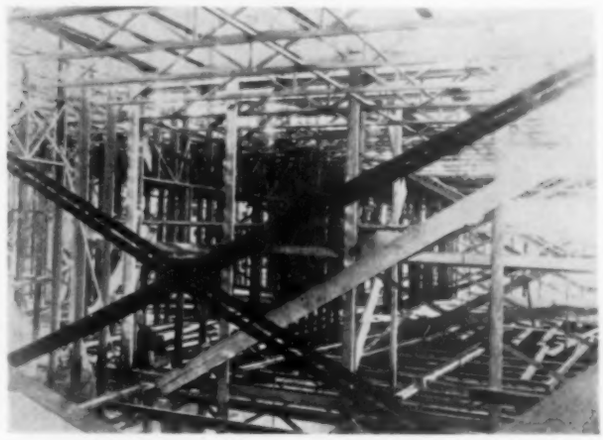


Fig. 5—Steel Framework of Lagunita Court

Such results very clearly and definitely indicate that considerable end restraint is developed, for although the magnitude of the error is appreciable, the qualitative indication of the results is conclusive.

Further computations were made to evaluate the end restraint which would be required to reduce the deflections to the observed values. The moments required by the various deflection values are found in Table 2.

Table 2—Restraining Moment Computed from Deflection Compared with Moment by Moment Distribution and the Fixed-End Moment

	Values of Moment in Inch-Pounds by (1) Deflections		(2)	(3)
	Increasing Load	Decreasing Load	Moment Distribution	Fixed-End Moment
Outside end	20,800 (*C*)	16,700	14,000 (*A*)	44,900
Inside end	32,200 (*D*)	37,000	32,600 (*B*)	44,900

It must be pointed out that the moments as computed for the deflections are not very accurate individually because a moment at one end causes almost equal deflections at either third point. Therefore, only a small change in the deflection at one third point will imply the

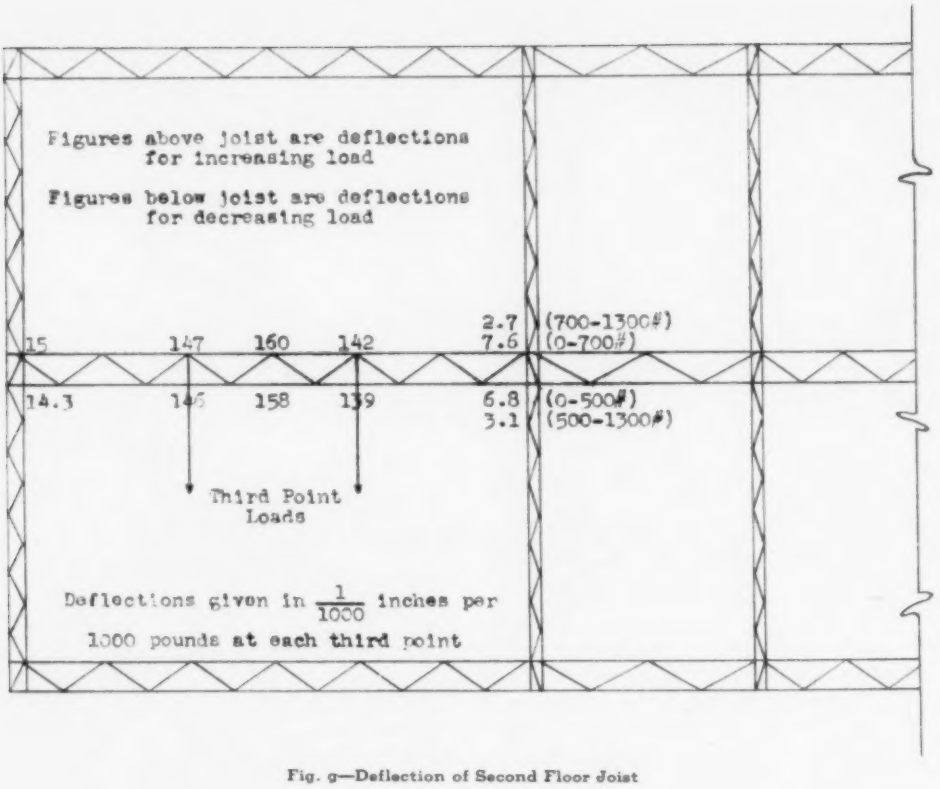


Fig. 6—Deflection of Second Floor Joist

transfer of a large moment from one end of the beam to the other. For example, suppose the deflection of the third points were changed by 0.0006 inches as shown below, a change of about 0.66 per cent.

INCREMENT OF DEFLECTION CAUSED BY RESTRAINT, INCH

	Deflection at Outside Third Point	Deflection at Inside Third Point
Original value	0.0914	0.0953
Values changed by 0.0006 inch	0.0920	0.0947

the values of the moments would then be

	Outside	Inside
Moment before change, inch-lb.	16,700	37,000
Moment after change, inch-lb.	19,800	33,800

or changes of 18.6 and 8.7 per cent, respectively. Therefore, to obtain a more reliable result for the moments it seems advisable to increase the sum of the moments (A) and (B) to the value of the sum of the moments (C) and (D), but to keep their relative values constant. The moments are then

Outside.	16,100 inch-lb.
Inside	36,500 inch-lb.

ROOM SPAN

CORRIDOR SPAN

	TRUSS	DIMENSIONS			TOP CHORD					BOTTOM CHORD						WEB MEMBERS					
		LENGTH	TOTAL DEPTH	EFFECT. DEPTH	MEMBERS	LENGTH	AREA	L/A	I	1/L = K	MEMBERS	LENGTH	AREA	L/A	I	1/L = K	MEMBERS	LENGTH	AREA	L/A	I	1/L = K
ROOM SPAN	FIRST FLOOR	201.75"	12"	10.28"	2-2×1½×⅜" ∠s 2" legs down	33.67"	0.84"	40.1	0.34	.0101	2 1¼×1¼×⅛" ∠s	16.83 33.67	0.60	28.06 56.1	.08	.00176 .00238	2-1×1×⅛" ∠s	10.28 19.72	0.46	22.32 42.8	.04	.00389 .00203
	SECOND FLOOR	201.75"	12"	10.28"	2-2×1½×⅜" ∠s 2" legs down	33.67	0.84	40.1			2-1½×1½×⅛" ∠s	16.83 33.67	0.72	23.4 46.8			2-1×1×⅛" ∠s	10.28 19.72	0.46	22.32 42.8		
	CEILING	201.75"	12"	10.28"	1-2×2×⅛" ∠	33.67	0.48	70.2			1-2×1½×⅜" ∠ 2" leg up	16.83 33.67	0.42	40.1 80.2			1-1×1×⅛" ∠	10.28 19.72	0.23	44.64 85.6		
CORRIDOR SPAN	FIRST FLOOR	87"	12"	10.525"	1-2×1½×⅜" ∠ 2" leg down	43.5	0.42	103.6	0.17	.00391	1-1¼×1¼×⅛" ∠	21.75 43.5	0.30	72.5 145.0	.04	.00164 .00092	1-1×1×⅛" ∠	10.525 24.16	0.23	45.8 105.	.02	.0019 .00078
	SECOND FLOOR	87"	12"	10.525"	1-2×1½×⅜" ∠ 2" leg down	43.5	0.42	103.6			1-1½×1½×⅛" ∠	21.75 43.5	0.36	60.45 120.9			1-1×1×⅛" ∠	10.525 24.16	0.23	45.8 105.		
	CEILING	87"	12"	10.525"	1-2×2×⅛" ∠	43.5	0.48	90.6			1-2×1½×⅛ ∠ 2" leg up	21.75 43.5	0.42	51.8 103.6			1-1×1×⅛" ∠	10.525 24.16	0.23	45.8 105.		

Fig. 7—Analytical Determination of Joist Characteristics

or an increase of 15 per cent over the values (A) and (B). This 15 per cent increase signifies either an error in the computations or a value of stiffness for the studs differing from that assumed.

CONCLUSIONS OF THIS THESIS

Summing up the complete work reported in this thesis the following conclusions can be stated:

1. All the characteristics of trussed members can be computed with sufficient accuracy for use in the Hardy Cross method of moment distribution by considering the trusses as pin connected, and some characteristics can be found from the geometric properties of the section or in the conventional manner for beams.

2. Moments in a welded steel frame such as used in Lagunita Court can be determined with an accuracy of the same degree as the loads acting on the structure are known.

3. Negative restraining moments at the columns exist in magnitude approaching the values indicated by a "moment distribution" analysis in such structures as Lagunita Court, and should be provided for in design.

RESIDUAL STRESS TESTS OF All-Welded Plate Girders*

By H. E. LANCE MARTIN, B.Sc.

INTRODUCTION

BY MEANS of fusion (electric-arc) welding, the maintenance of geometrical similarity between steel models and full size engineering structures is greatly facilitated, provided that the difficult operation of welding thin gage metal has first been successfully mastered. Models made by this process are particularly apt and useful. Not only may the welding procedures and the resulting residual strains and stresses be determined but also the behavior of the finished product may be found under the predetermined loads together with the fatigue limit if required.

Knowledge thus obtained from steel models may be applied directly to the design and fabrication of the corresponding full sized all-welded structures.

A large amount of fundamental work, extending over a number of years and relating chiefly to the experimental determination of the internal strains and stresses induced in edge-welded mild steel plates, has been accomplished.†

PART I

Preliminary Experiments

The experiments‡ already referred to, were made on $^3/_8$ inch fully annealed mild steel plates. Before undertaking the task of fabricating the steel model of the plate-girder, to be made from unannealed mild steel, it was necessary, therefore, to discover by further experimental work, the effect of edge-welding unannealed mild steel specimens of small gage thickness. The results of this work were so satisfactory—the residual strains and stresses were found to be of the same order and distribution as those in the $^3/_8$-inch plates—that it was decided to measure the residual displacements** and the resultant stresses in examples of the joints that were to be used in the model, viz., butt and fillet joints.

Typical results for each kind of joint are as follows:

N.B.—The figures given in the tables and marked upon the diagrams under the general heading of "divisions" refer to the micrometer readings of the measuring instrument. To convert into strains, multiply by 2.63×10^{-6}. For convenience scales have been included on the appropriate diagrams.

*Residual Strains** and Stresses in the Central Transverse Section of a Single Run Butt-Joint*

** Throughout this paper the terms displacements and strains are used synonymously. See note on next page.

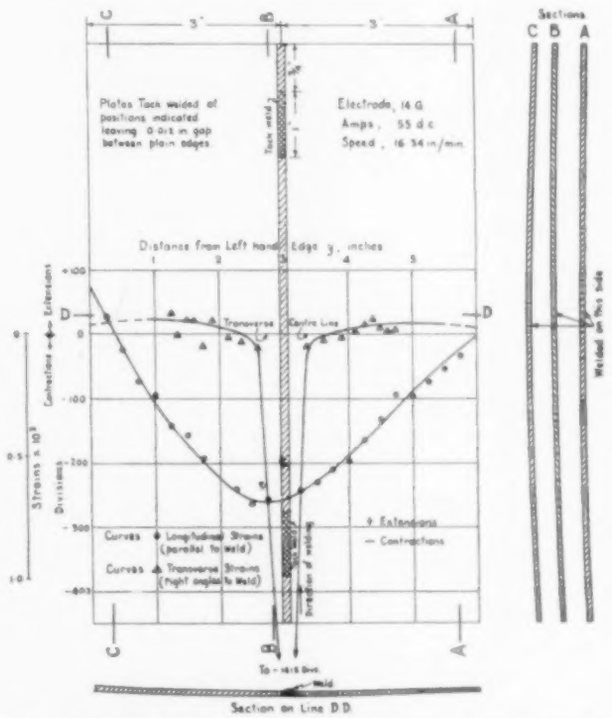


Fig. 1—Residual Strains in the Central Transverse Section of a Single Run Butt-Joint Between Two 9 Inch by 3 Inch by 14 G. Mild Steel Plates

* Paper read before the Institution of Structural Engineers at 11, Upper Belgrave Street, London, S.W.1, November 1938. Abstracted from the *Structural Engineer*. Ton used in this paper = 2240 lb.

† "Residual Stresses in Arc-Welded Plates," *Inst. Mech. Eng.*, Nov. 1936. "The Determination of the Residual Strains and Stresses in Arc-Welded Plates." *N.E.C. Inst. of Eng. and Shipbuilders Trans.*, LIII, 1937. "The Relief of Residual Strains and Stresses in Edge-Welded Mild Steel Plates." *Inst. of Welding Trans.*, I, No. 1, 1938.

‡ *N.E.C. Inst. of Eng., Inst. of Welding.* Loc. cit.

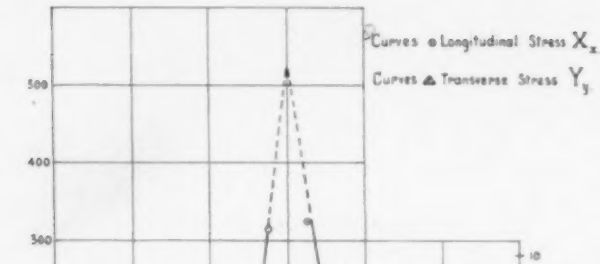


Fig. 2—Residual Stresses in the Central Transverse Section of a Single Run Butt-Joint Between Two 9 Inch by 3 Inch by 14 G. Mild Steel Plates

The joint was made between two unannealed mild steel plates, square edged, and each measuring 9 inches by 3 inches by 14 SWG (0.072 inch). On each face of the central portion of both plates highly polished surfaces of approximately $2^1/_2$ inches wide were prepared. Having been placed in an assembling jig with a slight gap between them, the plates were tack welded at the positions indicated in Fig. 1. The jig was so designed that it provided no mechanical restraint to the joint.

The gap width after tack welding was found to be 0.012 inch Standard gage lengths of approximately 2.013 inches were next marked off on the polished surfaces at intervals of $^1/_4$ inch. These lengths were accurately measured in terms of the micrometer divisions of the comparator in both the longitudinal (parallel to weld) and transverse (at right angles to weld) directions.

Having been welded in accordance with the particulars given on Fig. 1, templates of the final warped plate were made on three longitudinal sections AA, BB and CC and on a transverse section near to the central line, viz., DD, for reproductions of which see Fig. 1.

The differences between the measurements of the gage lengths made before and after welding are measures of the residual strains in terms of the divisions of the instrument. Both the longitudinal and transverse measurements for the joint are plotted in Fig. 1, the transverse curve being obtained from the measurements by a differential method.

The residual stresses were determined by means of the subdivision method described elsewhere.[1] Their values are plotted in Fig. 2.

NOTE.—All values of the strains and the stresses given for this joint, and as a matter of fact for all similar measurements throughout this paper, are averages for the standard gage length of 2.013 inches, and, furthermore, have not been subjected to any corrections.

General Remarks on the Results

Residual Strains.—It is of interest to note that the distribution and magnitudes of the residual strains in the longitudinal direction of the joint are similar to those found in edge-welded plates. The same may be said with regard to the transverse strains but in a more limited sense. Thus, between the values of $y = 0$ to $y = 2$ and from $y = 4$ to $y = 6$ the strains are of opposite signs to those of the longitudinal, taken over the same ranges. These correspond to the results previously found. In the range from $y = 2$ to $y = 4$, that is, the portion containing the weld of the joint, the strains are contractional, the maximum value being 0.0039 which is $5^1/_2$ times the maximum of that in the longitudinal direction. It thus appears that most of the transverse shrinkage takes place in the zone near to the weld.

Residual Stresses.—The chief points of interest are:

(1) The small values of the transverse stresses Y_y as opposed to the large shrinkage in the same direction.

(2) The large values of the longitudinal stresses X_x as opposed to the relative small shrinkage in the same direction.

(3) The remarkable similarity in the distribution of the longitudinal stresses for each half of the joint with that found for edge-welded plates.

The slight want of symmetry between the two sides of the joint shown in Figs. 1 and 2 may be due to the electrode having been kept nearer to the left-hand edge of the joint throughout its passage along the gap.

Because of the fact that the finished butt-joint is unrestricted, that is to say is not subjected to external forces, the stresses X_x on the measured section should fulfil the two conditions of equilibrium. It was found that for practical purposes these conditions were satisfied.

Residual Strains and Stresses in a Mild Steel Web Plate Due to Fillet Welding Thereon Four Stiffeners

The dimensions of the web-plate and stiffeners are shown on Fig. 3*a*. Gage marks were placed at intervals of 2 inches along the eight sections marked A to H. The four stiffeners were joined to the web-plate at the positions indicated by eight single fillet welds made according to the particulars enumerated on the figure. Typical distribution curves for the two sections midway between those at A and B ($x = 0$) and midway between those at D and E ($x = 4.625$) are given on Figs. 3*b* and 3*c*, respectively.

No visible warping of this specimen of thin material was present on the completion of the welding operations.

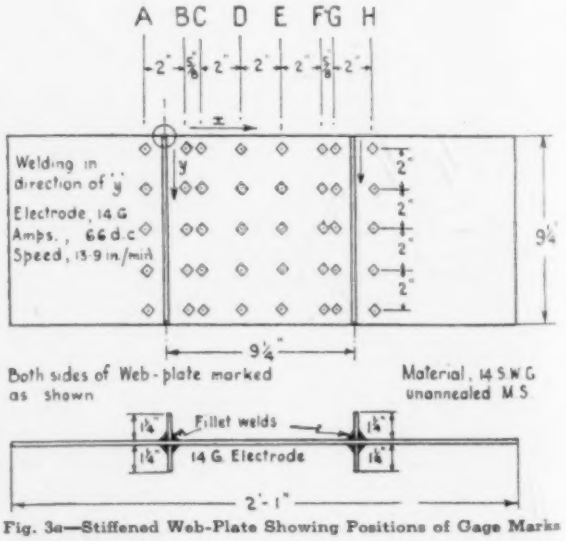


Fig. 3a—Stiffened Web-Plate Showing Positions of Gage Marks

[1] Loc. cit. *N.E.C. Inst. of Eng.*

General Remarks on the Results

The transverse strains at the sections of the web-plate on which the stiffeners were welded are large contractions, the average being −0.0094 (−3621 divs.) a figure well outside the strain (0.001) at the yield point of the mild steel employed. The longitudinal strains, at the same sections, are also contractions, their magnitudes, however, are relatively small being of the order of only −200 divs. From these measurements it appears, therefore, that the material in the immediate neighborhood of the fillet welds has contracted in two directions at right angles to each other. From similar experiments it appears that most of these shrinkages occur within $^1/_4$ inch of the welds.

It should be remembered that this fact does not necessarily mean that the residual stresses due to these movements will also be of the same nature. Further experimental work would have to be undertaken to determine the nature and magnitudes of the stresses in plastic or overstrained regions such as these.

On the other selected sections of the web-plate the measured strains are seen to be below that of the strain at the yield point. Advantage was taken of this to determine the residual stresses by means of the formulas:

$$X_x = \frac{E}{1-\sigma^2}(e_x + \sigma e_y); \quad Y_y = \frac{E}{1-\sigma^2}(e_y + \sigma e_x),$$

in which X_x and Y_y are the residual stresses; E, Young's modulus and σ, Poisson's ratio; e_x and e_y are measured elastic strains in the x and y directions, respectively. (In subdivision method e_x and e_y are measured elastic recoveries.)

Figure 4 shows the stress distribution curves for section $x = 4.625$ which may be taken as typical.

Strength Tests of a Short Span All-Welded Plate Girder

For the purpose of obtaining information with regard to the shear strength of fillet joints it was decided to fabricate a short length of girder having the same leading dimensions as those designed for the scale model girder with the exceptions of its span and distance between the stiffeners which were reduced.

Figure 5 is a dimensioned sketch of this preliminary girder. It also indicates the method of loading. Its fabrication afforded the welding operator further experience in the predetermined welding procedure which had previously been proved to yield work free from visible distortion.

The general behavior of this light girder under load—it weighed 10 lb. 1 oz.—is summarized in Table 1.

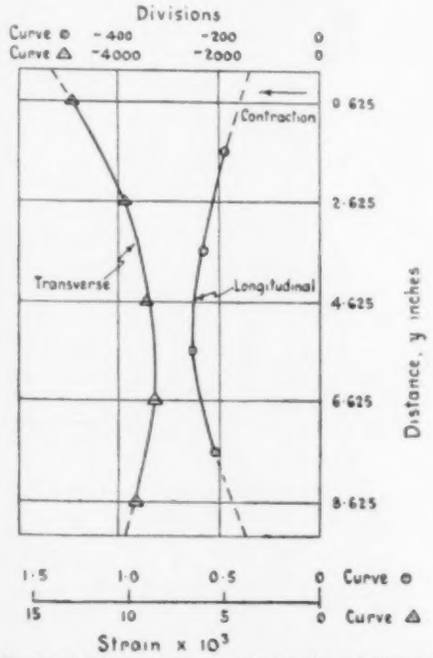


Fig. 3b—Residual Strains on Section X = 0 inch of Stiffened Web-Plate

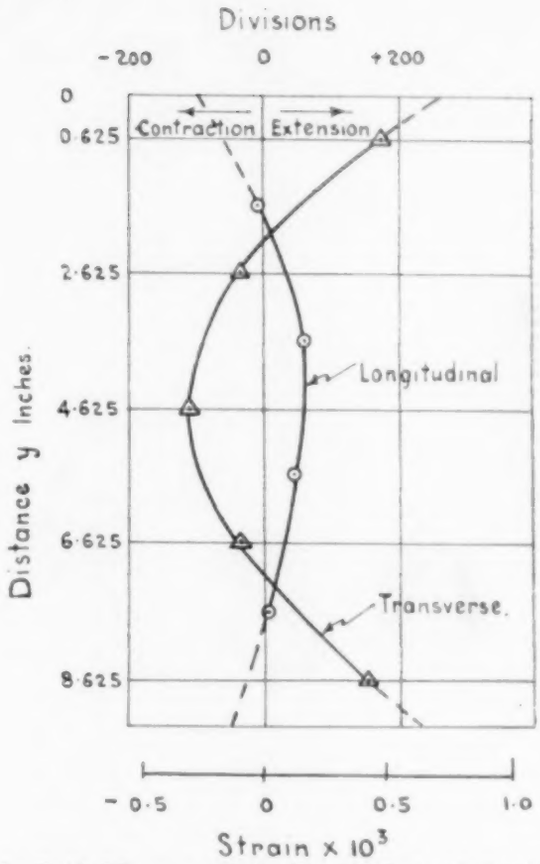


Fig. 3c—Residual Stresses on Section X = 4.625 Inch of Stiffened Web-Plate

Table 1

Central Load in Tons	Remarks
0	No visible distortions present.
5	No apparent movement.
14.25	Girder yielding but no signs of buckling of the web or compression flange.
16.33	Serious local buckling of the central stiffeners and of the top portion of the web between them.

A summary of the stress—calculated by methods usually adopted by engineers—induced in the component parts of the girder by the external loads is given in Table 2.

General Remarks on the Results

Careful inspection of the buckled girder indicated that no visible damage had been done to the welds, not even where local distortions had occurred in the web and stiffeners.

The figure of 0.44 tons per inch run of weld does not represent the ultimate shear strength of the fillet joints. It does indicate, however, that fillet welds of the same character should be fully capable of carrying the smaller shearing forces induced in the longer span of the proposed

Table 2

Component part.	External load. Tons.	Nature of Stress.	Stress in Tons/sq. in.	Remarks.
Web.	5.0	Mean shear	3.75	
	14.25	Mean shear	10.80	
	16.33	Mean shear	12.30	
Fillet welds between web and flanges.	16.33	Mean shear	8.80	Equivalent to 0.44 tons per in. run of weld, i.e., per $\frac{1}{4}$ in. run of girder.
Flanges at central section ...	5.0	Tension	4.60	
	14.25	and	13.50	Signs of yielding.
	16.33	Compression.	15.40	Web and stiffeners buckled.

Table 3—Design Data

	Full size girder.	Scale model girder.	Remarks.
Spans	40 ft. 0 ins.	8 ft. 0 ins.	In the clear.
Width of flanges—			
Through	18$\frac{1}{4}$ ins.	3$\frac{1}{4}$ ins.	
First cover	16$\frac{1}{4}$ ins.	3$\frac{1}{4}$ ins.	
Second cover	15$\frac{1}{4}$ ins.	3$\frac{1}{4}$ ins.	
Depths of webs and maximum distances between stiffeners	3 ft. 10$\frac{1}{2}$ ins.	9$\frac{1}{2}$ ins.	
Material	Mild steel	Mild steel	
Working loads	62.5 tons.	2$\frac{1}{4}$ tons	Concentrated at centres of spans.
Working stresses—			
Tension and compression ...	8 tons/sq. in.	8 tons/sq. in.	*See* note on factor of
Shear	3 tons/sq. in.	3 tons/sq. in.	safety.
Deflection-span ratio	1/700	1/700	

Table 4—Dimensions

Component parts.	Full size girder.	Scale model girder.	Remarks.
Widths and thicknesses of flange plates—			
Through	18$\frac{1}{4}$ in. by $\frac{3}{4}$ in.	3$\frac{1}{4}$ in. by 0.072 in.	
First cover	16$\frac{1}{4}$ in. by $\frac{3}{4}$ in.	3$\frac{1}{4}$ in. by 0.072 in.	
Second cover	15$\frac{1}{4}$ in. by $\frac{3}{4}$ in.	3$\frac{1}{4}$ in. by 0.072 in.	
Lengths of flange-plates—			
Through	40 ft. 10 ins.	8 ft. 2 ins.	Two butt-joints provided in these.
First cover	23 ft. 4 ins.	4 ft. 8 ins.	Slightly too short
Second cover	8 ft. 4 ins.	1 ft. 8 ins.	
Web thickness	$\frac{3}{4}$ in.	0.072 in. (14 G)	
Web length	40 ft. 10 ins.	8 ft. 2 ins.	Two butt-joints provided in this.
Web depth	46$\frac{1}{4}$ ins.	9$\frac{1}{4}$ ins.	
Size of stiffeners	46$\frac{1}{4}$ in. by 8$\frac{1}{4}$ in. by $\frac{3}{4}$ in.	9$\frac{1}{4}$ in. by 1$\frac{1}{4}$ in. by 14 G	
Spacing of stiffeners—			
Maximum	46$\frac{1}{4}$ in.	9$\frac{1}{2}$ ins.	
Minimum	10 ins.	2 ins.	
Estimated weights	7,225 lb.	57 lb.	
Moments of inertia—			
Web	3,460 ins.^{4}	4.75 ins.^{4}	Net values. Section of welds not included.
Flanges—			
Through	7,360 ins.^{4}	11.80 ins.^{4}	
First cover	6,840 ins.	10.95 ins.	
Second cover	6,500 ins.	10.40 ins.	
Girders ...	24,160 ins.	37.90 ins.	

Table 5

Stages after which measurements were made.	Component part of girder on which measurements were made.	Table in which measurements are recorded.	Figs.	Remarks.
Stiffeners to web and butt-joints in web (2)	Web-plate	7	12	Figures in brackets in first column refer to order of assembly.
Through flanges to stiffened web-plate (4).	Web-plate	8	12	
Through flanges to stiffened web-plate (4).	Through flanges	8	13	
	Compression			
	Tension			
First and second cover flanges to through plates (6).	Web-plate	9	12	Slight buckling of web-plate.
First and second cover plates to through plates (6).	First covers	9	14	
	Compression			
	Tension			
After release of external yield load of girder, viz., 3.75 tons.	Web-plate	10	12	Buckling of web modified under external load.
After release of external yield load of girder, viz., 3.75 tons	First covers	10	14	
	Compression	10	14	
	Tension			

Table 6—Summary of the Residual Stresses at Section AA

Component part.	Nature of stress and values in tons per sq in.		Remarks.
	Transverse.	Longitudinal.	
Web, at—			
y = 4.625 ...	Comp., 3.2	Tension, 0.35	Due to welding stiffeners and butt-joints.
y = 9.25 ...	Tension, 7.05	0.00	
y = 0.00 .	Tension, 5.60	0.00	
Web, at—			
y = 4.625	Comp., 5.6	Tension, 2.1	Due to welding stiffeners, etc., and through flanges to web.
y = 9.25 .	Tension, 3.5	—	
y = 0.00 .	Comp., 4.24	—	
Web—			
Average on section.	Comp., 9.5	Tension, 1.96	Due to all welding operations. Web buckled slightly under this stress.
First cover comp. flange	Probable maximum comp., 8 tons per sq. in. at centre of width.[7] Probable maximum tension 15 tons per sq. in. at welded edges.		

[7] These stresses have been taken from Exp. 4 in the paper entitled "The Relief of Residual Strains and Stresses." *Trans. Inst. of Welding*, Vol. I, No. 1, p. 49, in which the data there given is similar to that applying to the first comp. flange.

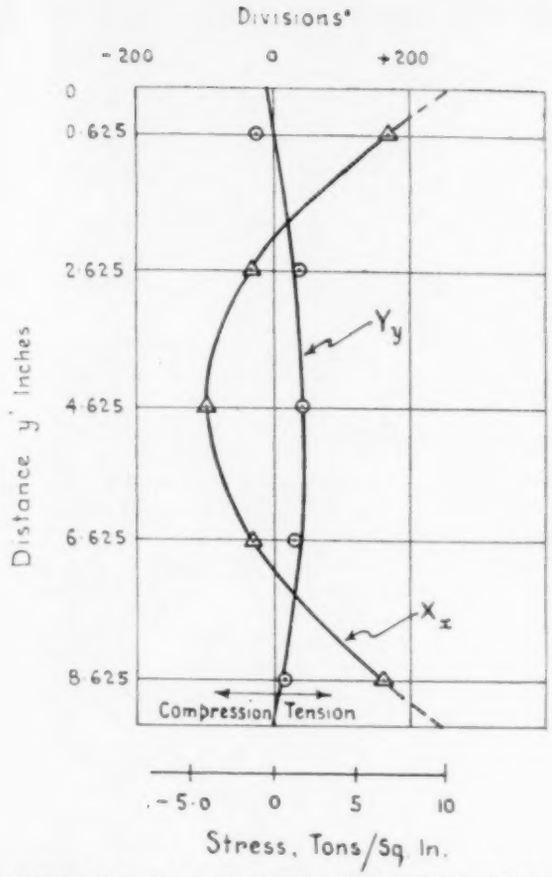


Fig. 4—Residual Strains on Section X = 4.625 of Stiffened Web-Plate

scale model. A number of simple shear tests, that is, those with little or no bending action present, have shown that 1 ton per inch run of weld may be taken as an average value of the ultimate strength of fillets of this size.

The absence of any buckling of the girder at the 14.25 ton load seems to indicate that the flanges yielded first. This contention is supported by the fact that the stresses in them were practically identical with the average yield stress of 13.71 tons per sq. in. as determined from tensile tests made with a number of specimens of the parent metal.

The manner in which the buckling occurred suggests that the remarkable load of 16.33 tons might have been exceeded had a central stiffener been provided.

PART II

The All-Welded Scale Model Plate-Girder

For the sake of brevity the conditions under which the girder was fabricated, the general data relating to its design and resulting dimensions are given in tabular form. This information is supplemented with outline sketches of the assembled girder and some of its parts. See Figs. 6, 9 and 10.

Conditions Under Which the Plate Girder Was Built

The following severe conditions were imposed and strictly adhered to:

1. That the geometrical scale ratio was to be 1 to 5.
2. That two butt-joints in each of the tension and compression flanges and two butt-joints in the web-plate were to be incorporated in the design.
3. That the maximum distance between the stiffeners was not to exceed the depth of the web-plate.
4. That the tension and compression flanges were to be formed from three plates each, the plates being of equal thickness. The through flange plates to be fabricated to the stiffened web-plate first and the cover flanges added, in turn, afterward.
5. That the material was to be of unannealed mild steel, cold straightened, if necessary.
6. That the lengths of the flange and web plates were to be in the direction of rolling.
7. That the web, flange plates and stiffeners were to be sheared to size from mild steel sheets and their edges to be trued to within ± 3 thousandths.
8. That only one welding operator was to be employed at one and the same time.
9. That no restriction, mechanical or otherwise, was to be permitted during or after welding.
10. That the maximum amperage possible was to be employed consistent with good deposit of weld metal and good penetration, with the object of making the test as severe as possible.
11. That, as far as possible, "down hand" welding was to be employed.
12. That all welds were to be made with one run. When continuing the same run with a new electrode the deposited metal at the junction was to be thoroughly cleaned from slag.

Note on the Factor of Safety

The mechanical tests of the 14 G mild steel sheet gave the following results:

Ultimate tensile strength, 23.73 tons per sq. in.
Stress at the yield point, 13.71 tons per sq. in.

On the basis of the ultimate stress the factor of safety is 2.97; on that of the stress at the yield point it is 1.72.

The critical *shear* stress,[2] for buckling of the web-plate for the condition that the distance between the stiffeners is the same as the depth of the web is approximately 7 tons per sq. in. Hence, taking this stress as a basis the factor of safety becomes 2.3.

Again, failure of the girder may occur by compressive buckling of the upper flange, an approximate estimate of which stress[3] is 9 tons per sq. in. for the particular through flange plates of single thickness. Hence, as the stress at the end of the first cover plate may be shown to be 6.72 tons per sq. in. (see Table 7), the factor of safety is reduced to 1.34.

Welding Particulars

Plant.—Static transformer with mercury rectifier.

Electrodes.—14 G heavy coated paste.

Current.—For butt-joints, 55 d.c.

For fillet welds, 65 d.c. Electrode connected to negative pole.

Speeds of Electrodes.—For butt-joints, 14 inches per minute.

For fillet welds, 16 inches per minute.

[2] Timonshenko & Lessells. *App. Elasticity*, p. 294.
[3] Loc. cit.

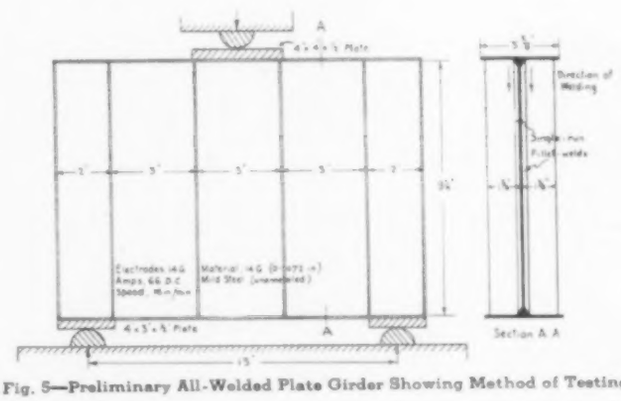


Fig. 5—Preliminary All-Welded Plate Girder Showing Method of Testing

The Order in Which the Scale Model Girder Was Assembled

1. Stiffeners welded to each of the three separate portions of the web-plates.
2. The two butt-joints in the stiffened web-plate completed.
3. The two butt-joints in each of the tension and compression through flange plates completed.
4. Through flange plates welded to the stiffened web-plate.
5. First cover flanges edge-welded to the through flange plates.
6. Second cover flanges edge-welded to the first flange plates.
7. End plates welded to web and through flange plates.

During the process of fabrication measurements were made to determine not only the average total contraction in the length of the girder but also the precise residual strains and stresses at the two selected cross sections of it shown at AA and BB[4] on the outline sketch of Fig. 6.

With the aid of an ordinary workshop trammel gage the average contraction in the stiffened web, before flanges were attached, was found to be approximately $^1/_4$ inch. Now it will be seen, on referring to the results of the precise measurements on the preliminary butt and fillet joints already described that this comparatively large contraction was not uniformly distributed along the length of the web-plate, but was principally due to the shrinkages at and near to the welds. Thus:

Average shrinkage across butt-joint, −1415 divs. = 0.00746 inch.

Average shrinkage across fillet welds, −3621 divs. = 0.019 inch.

Therefore, as there were two butt-joints and thirteen pairs of stiffeners in the web-plate the contractions are equivalent to 0.0149 inch and 0.25 inch, respectively, making the total 0.2649 inch as against $^1/_4$ inch by the trammel method.

It will be convenient to give here the total contraction for the wholly assembled girder. The additional shrinkages due to the contractions in the through flanges and cover flanges were 0.067 inch and 0.0275 inch, respectively. Hence, the total reduction in the over-all length of the girder due to all welding operations equals 0.3594 inch. The corresponding value by the trammel method was $^3/_8$ inch.

After certain stages had been completed in the fabrication of the girder the precise measurements at section AA were made, as shown in Table 5.

The extent of the buckling of the web due to the additional shrinkage imposed upon it by edge-welding the first and second flanges to the through plates may be gaged from the curves of distortions reproduced in Fig. 7.

[4] Section AA is only considered in this paper.

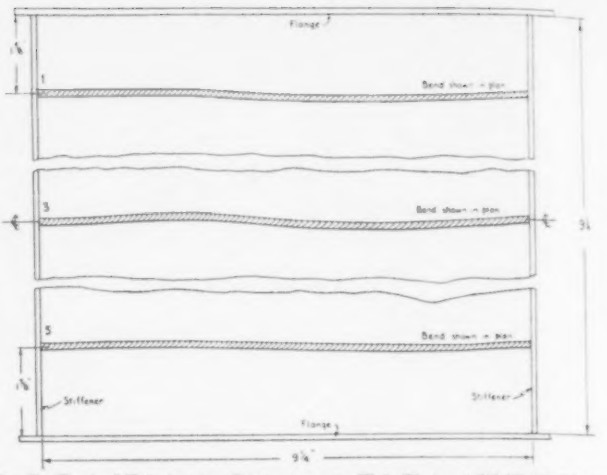


Fig. 7—Typical Fabrication Distortions in Web-Plate of All-Welded Plate Girder

At three horizontal positions in the depth of the web, in the selected panel of the girder, wooden templates were made to fit the horizontal sinoidal bends and from these templates the figure was made.

Figures 8, 9 and 10 show, in graphical form, some of the salient features of the measurements.

Space does not permit of a detailed discussion of these interesting results being given here but three outstanding items are worthy of mention. They are:

1. The increase in the transverse (parallel to the length of the girder) shrinkages after each welding operation, culminating in the buckling of the web in the form of horizontal sinoidal waves. These waves appeared in each of the bays in the length of the girder covered by the first cover flange plates.
2. The linear distribution of the residual strains across the width of the compression flange (Fig. 10).
3. The decrease in the total contractions after release of the external tensile stresses and the increase in the total contractions after the release of the external compressive stresses (Figs. 8 and 10).

PART III

Mechanical Tests of the Scale Model Girder

Having dealt with the construction and the internal state of the girder it now remains to show that it was capable of supporting, under the imposed conditions, its working or greater loads. Also to discover, if possible, the effect external loading had upon the residual strains and stresses.

First Test

The arrangement for loading and the method of taking the deflections may be gathered from Fig. 12. The load

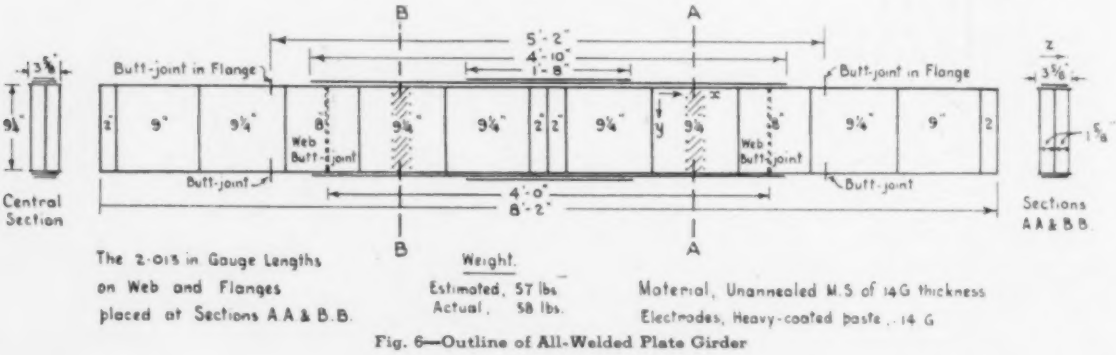


Fig. 6—Outline of All-Welded Plate Girder

Distance, y, inches
Strains × 10³
Divisions
Curves
① After welding Stiffeners to Web-plate
② After welding Through Flanges to Web
③ After welding Cover Flanges to Through Flange
④ After release of 1st Max. Central Load of 3.75 Tons.
⑤ After welding Cover Flanges to Through Flange
⑥ After release of Max. Central Load of 3.75 Tons
Longitudinal Strains
Direction of Recovery
Contraction
Extension

Fig. 8—Residual Strains in Web-Plate of All-Welded Plate Girder at Various Stages of Its Construction

Distance, z inches
Divisions
Strain × 10³
Contraction
(Transverse at Rt. mean value = -1445 Divs) Ls. to weld
3⅝"
Fillet Welds
Web
Part Section of Through Flange
* Web - Plates showing positions of Fillet Welds
N.B. The Recovery on this Component part could not be measured as of Section AA. the Cover flanges were in place

Fig. 9—Residual Longitudinal Strains, at Section AA, in Compression Through Flange of All-Welded Plate Girder

Table 7—Summary of Stresses Due to External Loadings

Component part.	External load in tons	Nature of stress and value in tons per sq. in.	Remarks.
Web	2.5	Mean shear, 1.88	Working load.
	3.75	Mean shear, 2.80	Yield load at first test.
	5.0	Mean shear, 3.76	Yield load at second test. Girder not weak.
Fillet welds between web and flanges.	2.5	Mean shear, 1.35	
	3.75	Mean shear, 1.91	
	5.00	Mean shear, 2.70	Equivalent to 0.128 tons per inch run of weld.
Web butt-welds.	5.0	Mean shear, 3.76	
		Probable max., 5.66	
Flanges at—		Tension and comp.	
Central section	2.5	7.70	
	3.75	11.50	
	5.0	15.40	N.B.—Yield point of unwelded parent metal, 13.71 tons sq. in.
Sections at ends of 2nd covers.	2.5	8.39	
	3.75	12.30	
	5.0	16.40	Maximum fibre stress in girder.
Sections at ends of 1st covers.	2.5	6.72	
	3.75	10.50	Through flanges rivetted at or just before this stress was reached.
	5.0	13.44	

was increased in 1/4 ton steps to 2.5 tons, which load was allowed to remain on the girder for several minutes. The deflection remained at the constant figure of 0.165 inch throughout this period. The deflection-span ratio, therefore, was 1/580 as against 1/700 specified. The load was then raised to 3.26 tons; the deflection remaining steady at 0.22 inch. On reducing the load to 1 ton it was found that a permanent deflection of 0.04 inch was registered. Finally the load was increased again until the girder showed signs of yielding, the maximum value attained being 3.75 tons with a deflection of 0.26 inch. This load was released fairly quickly so as to prevent unnecessary damage to the girder, it being desired to remeasure, with the comparator, the alterations of the strains at section AA after the structure had been allowed to rest. Inspection at the maximum load showed that the compression flanges near to the two outer supports had twisted slightly. On release of the load, however, they partially recovered.

The results of the remeasurements after a lapse of two months show that the release from external tension reduced the residual strains and that the release from external compression increased the residual strains.

Second Test

For the purpose of this test to destruction the girder was placed in the testing machine in the reverse way to that which it was in the first test, viz., the compression flange of the first was made the tension flange in the second. Figure 12 shows this arrangement.

On reaching the working load of 2.5 tons for the second time the deflection registered was 0.135 inch. Thus, the deflection-span ratio was reduced to 1/710 a value less than that specified.

The general behavior of the girder during the test may be seen from the salient features marked upon the deflection diagram of Fig. 11. Creeping commenced at 5 tons but apparently ceased after a short interval. At 5.5 tons failure took place by the serious buckling of the web and top flange at the end of the second cover plate.

The factor of safety as measured by the failing load is 2.5, a value lying between that based upon the ultimate and yield point stresses of the material.

It is difficult to say whether the final cause of failure was due to the buckling of the web or that of the compression flange. It will be seen from the details given on Fig. 11 that a very marked redistribution of the load was taking place in the girder during the test.

To make this brief review as complete as possible Tables 6 and 7 have been compiled. They relate to the residual and external stresses in various parts of the girder, respectively. The difficult matter of combining these two sets of stresses is beyond the scope of the present work. However, a certain amount of information has been obtained which seems to indicate that the strains may be additive but not the stresses. Be that as it may it must suffice to say that the results of the mechanical tests have definitely proved that welded scale models can be successfully constructed to fulfil the conditions applying to full size service structures.

CONCLUSIONS

Owing to the enormous field of inquiry opened by the successful issue of the work, only partially presented here, it would be unwise to attempt to give detailed statements of the numerous conclusions that may be derived from it. However, it is suggested that sufficient data have been included not only to substantiate the implied opinions expressed in the introduction but also to demonstrate to industry, and to the welding industry in particular, that many engineering difficulties may, in like manner, be overcome.

For the above reasons, therefore, only a few of the more outstanding conclusions are mentioned. They are:

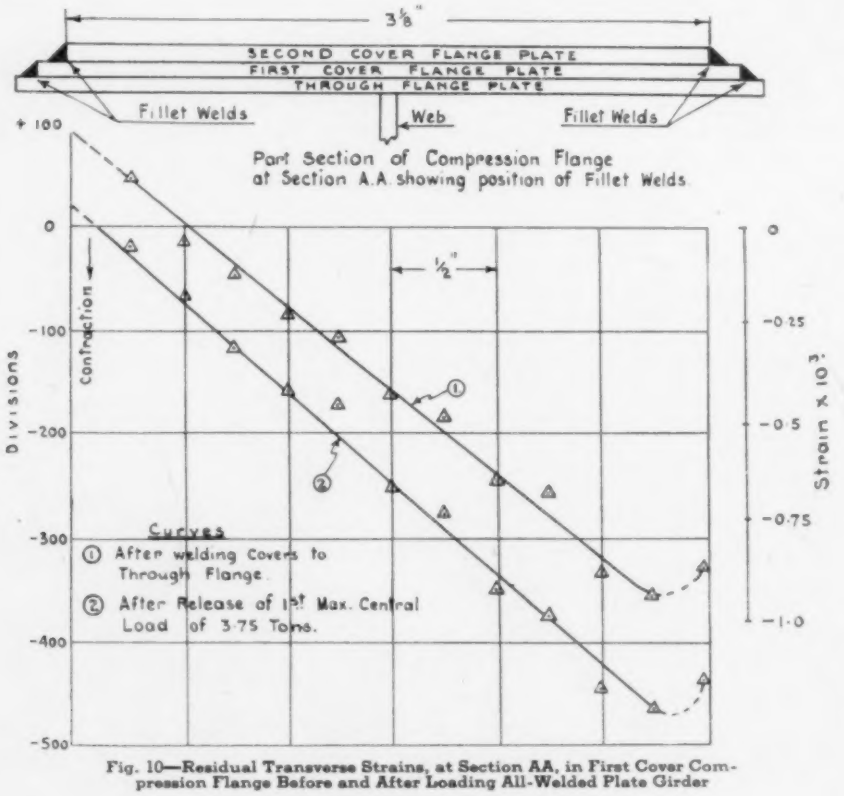


Fig. 10—Residual Transverse Strains, at Section AA, in First Cover Compression Flange Before and After Loading All-Welded Plate Girder

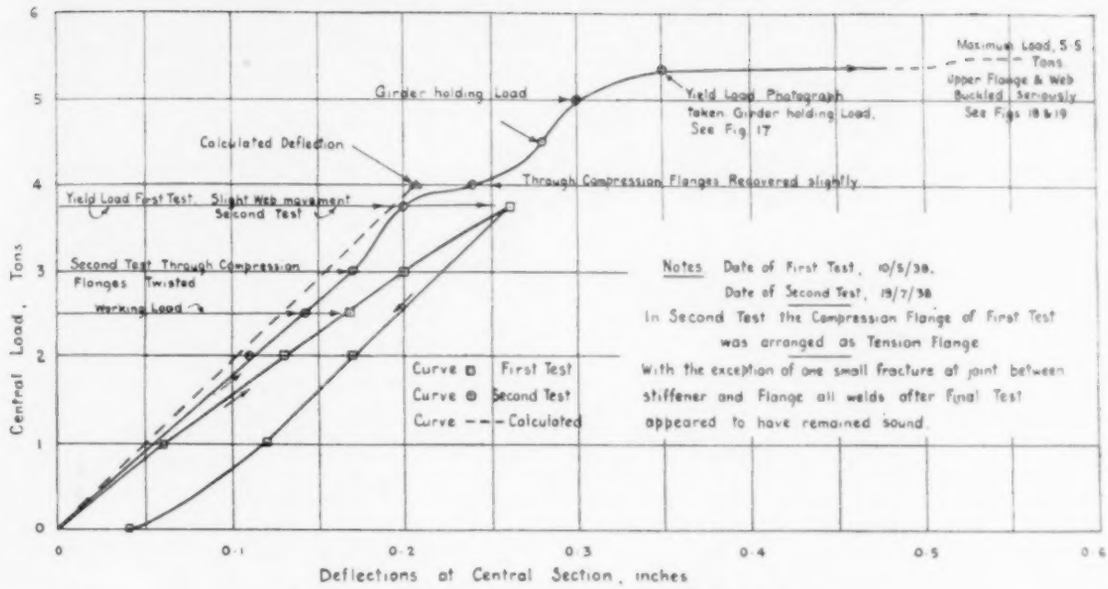

Maximum Load, 5·5 Tons
Upper Flange & Web Buckled Seriously
See Figs 18 & 19
Girder holding Load
Yield Load Photograph taken Girder holding Load. See Fig. 17
Calculated Deflection
Through Compression Flanges Recovered slightly
Yield Load First Test. Slight Web movement Second Test
Second Test Through Compression Flanges Twisted
Working Load
Central Load, Tons
Deflections at Central Section, inches
Notes Date of First Test, 10/5/38.
Date of Second Test, 19/7/38
In Second Test the Compression Flange of First Test was arranged as Tension Flange
With the exception of one small fracture at joint between stiffener and Flange all welds after Final Test appeared to have remained sound.
Curve □ First Test
Curve ● Second Test
Curve – – – Calculated

Fig. 11—Load-Deflection Diagrams of All-Welded Plate Girder

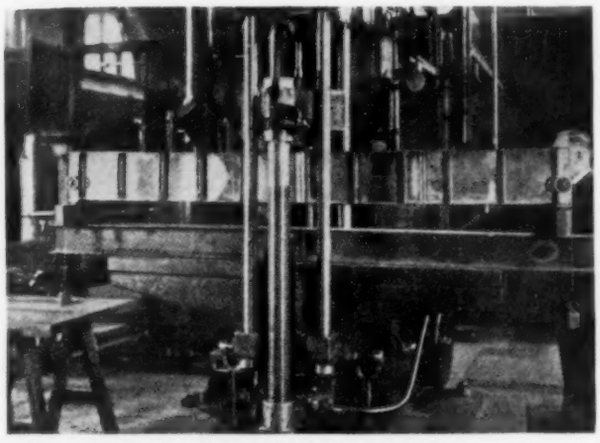


Fig. 12—All-Welded Girder Under Test

(1) That practical scale models, using the principle of geometrical, or alternatively that of dynamic, similarity can be successfully fabricated by the electric-arc process of welding.
(2) That the freedom from visible distortion of the fabricated structure proves the soundness of the welding procedure adopted and that the results of the mechanical tests demonstrate that the strength and ductility of the welds were entirely satisfactory.
(3) That the new technique of measurement has proved applicable to the accurate determination of residual strains and stresses in all-welded mild steel structures.
(4) That the technique has indicated some of the effects that the release of external loading has upon the residual strains and stresses in all-welded mild steel structures.
(5) That correct "welding procedures" applicable to fusion welding in naval architecture, structural and mechanical engineering may be quickly and at small cost studied and may be determined by the use of the methods briefly indicated.
(6) That the results of this preliminary work have clearly demonstrated that both economical and safe service structures will follow from the specialized design, fabrication and testing of the corresponding all-welded scale models.

ACKNOWLEDGMENTS

The author's best thanks are due to:
The Research Fund Committee of King's College, Newcastle-upon-Tyne, in the University of Durham and the Institute of Welding for grants; Messrs. Philips Lamps, Ltd., of London, for the loan of the welding plant; Prof. C. J. Hawkes and Dr. J. Morrow for their kindly interest in the work as it proceeded; Mr. E. R. Wheldon, his mechanic, for the enthusiastic help in welding the specimens and taking the necessary readings.

Temperatures in Flash Welding*

By H. KILGER

FLASH welds were made in mild steel bars, 1.18 inches diameter, in a well-known type of machine at four settings, Table 1. During welding the temperature at four points on one of the bars was measured by means of nickel-nickel chromium thermocouples spot welded to the surface. The thermocouples were connected through a sieve circuit to a six-loop universal oscillograph.

Ohmic and inductive coupling between the welding and measuring circuits created high a.c. voltages in the measuring circuits, which were absorbed in the sieve circuit. The sieve circuit consisted of series inductance and resistance together with a 3000 uF condenser in parallel with each loop.

The over-all accuracy based on an average deflection of 1 mm./mv. was ± 8° C. at low temperatures and ± 6° C. at high. Besides the four temperature records, each oscillogram contained a time scale and a record of the voltage between the welding clamps. The temperature records at each of the measuring points were averaged for each specimen at a given machine setting and converted to distances of 10, 20, 30 and 40 mm. from the completed weld, Fig. 1.

At all machine settings the temperature rises extremely rapidly to the beginning of welding. The rate of temperature rise increases as the power is increased and is

* Abstract from Production and Quality of Flash Welds (Fertigungstechnik und Güte abbrenngeschweisster Verbindungen) published by F. Vieweg, Braunschweig, 1936.

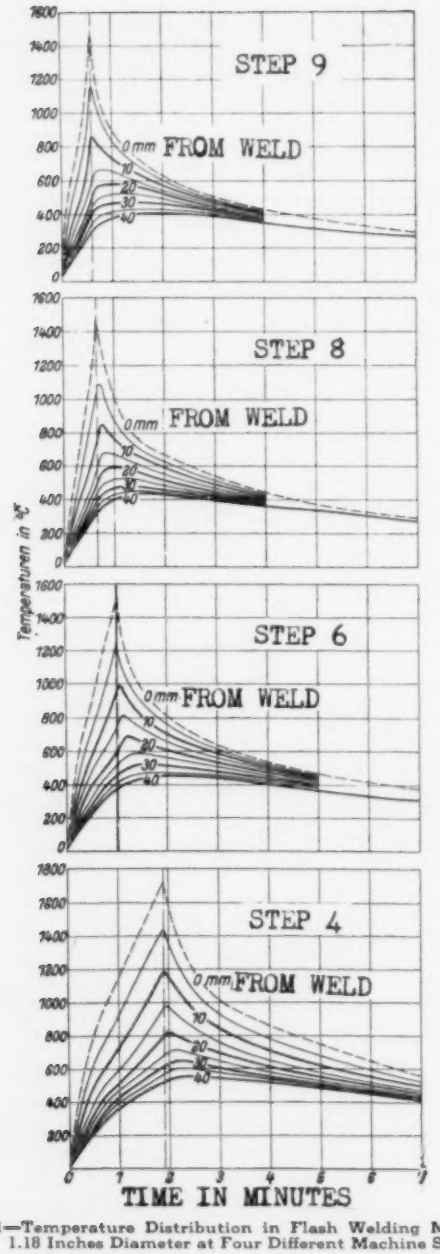


Fig. 1—Temperature Distribution in Flash Welding Mild Steel Bars, 1.18 Inches Diameter at Four Different Machine Settings

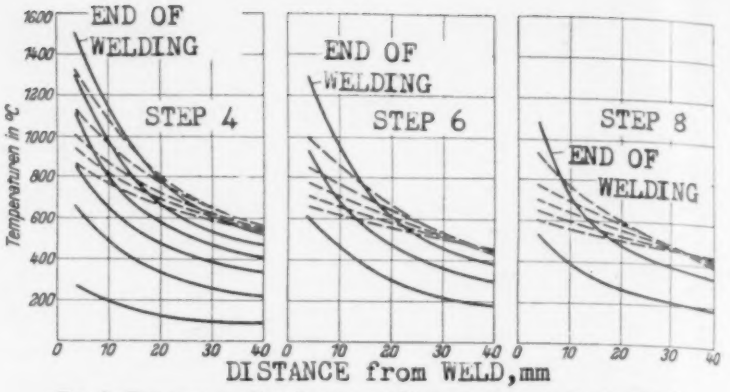


Fig. 2—Temperature Distributions at Different Instants During Flash Welding Mild Steel Bars, 1.18 Inches Diameter at Three Different Machine Settings

Full line—before push up
Dash line—after push up

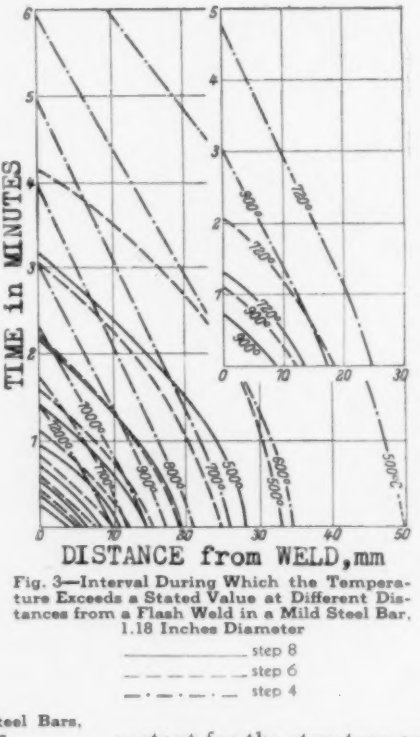


Fig. 3—Interval During Which the Temperature Exceeds a Stated Value at Different Distances from a Flash Weld in a Mild Steel Bar, 1.18 Inches Diameter

step 8
step 6
step 4

more rapid at points closer to the weld. When flashing begins the temperature rises less rapidly because less energy is supplied and loss of heat increases. Toward the end of flashing the temperature near the weld is about the same in all specimens because the thermocouple is very close to the surface to be welded as a result of loss of material during flashing. The temperatures once again rise rapidly during push up and reheating, for full power is being delivered. The maximum temperature at the weld is observed at the moment the current is switched off. The maximum temperature at points back of the weld is attained somewhat later. The farther the section is from the weld, the lower the maximum temperature and the later it occurs. The weld cools rapidly after the current is switched off.

The results in Fig. 1 are replotted in Fig. 2 to show the distribution of temperature back of the weld at intervals of 20 seconds. The slow increase in temperature at points remote from the weld is shown clearly. Since time of heating as well as maximum temperature is important for the structure and properties of the weld, the time occupied in attaining temperatures from 500 to 1200° C. are plotted in Fig. 3 for all points up to 40 mm. from the weld. The insert in Fig. 3 contains only the times for temperatures of 720 and 900° C., the approximate limits of the critical range for low-carbon steel. Although the diagram applies strictly only to unalloyed low-carbon steel (0.06–0.15 C), results obtained with a chromium-nickel steel (no details) were practically identical.

Table 1—Welding Conditions and Location of Thermocouples

Machine Step	Peak Power Kw.	Welding Time, Seconds				Length Flashed, Inch	Distance of Thermocouple from Weld, Inch			
		Preheat	Flash	Reheat	Total		1	2	3	4
4	17	55.8	51.0	3.6	110.4	0.59	0.32	0.55	0.98	1.46
4	17	59.5	55.3	3.1	117.9	0.55	0.32	0.59	1.02	1.46
6	27	19.2	41.2	1.4	61.8	0.71	0.28	0.55	0.95	1.46
6	26	14.1	46.0	0.3	60.4	0.71	0.32	0.55	0.98	1.38
8	36	13.5	24.6	1.4	39.5	0.91	0.20	0.43	0.87	1.38
8	38	8.8	31.3	1.0	41.1	0.83	0.16	0.43	0.83	1.34
8	34	17.1	31.3	2.0	50.4	0.75	0.28	0.51	0.95	1.42
9	41	9.7	23.2	3.3	36.2	0.75	0.32	0.55	0.98	1.50
9	41	8.8	25.4	1.6	35.8	0.79	0.28	0.51	0.91	1.42

THE ENGINEERING FOUNDATION

Welding Research Committee

Sponsored by the American Welding Society « « » » and American Institute of Electrical Engineers

Supplement to the Journal of the American Welding Society, March 1939

WELDING CHROMIUM AND Chromium-Nickel Corrosion and Heat Resisting Steels

A Review of the Literature to July 1, 1937

By W. SPRARAGEN* AND G. E. CLAUSSEN†

This report is prepared under the auspices of

THE LITERATURE DIVISION OF THE ENGINEERING FOUNDATION
WELDING RESEARCH COMMITTEE

TABLE OF CONTENTS

	Page
Summary	66
Introduction	70
Welding 18–8	71
Oxyacetylene Welding	71
Field of Application	71
Preparation of Edges	71
Spacing and Clamping	71
Filler Rod Composition	71
Filler Rod Size	71
Preheating	71
Starting the Weld	71
Size of Torch	71
Special Torch	71
Two Torch Vertical Method	72
Type of Flame	72
Flux	72
Forehand or Backhand Welding	72
Position of Inner Cone	72
Manipulation of Torch	72
Layers and Reverse Welding	73
Welding Speed	73
Metal Arc Welding	73
Field of Application	73
Preparation of Edges	73
Spacing and Clamping	73
Composition of Electrodes	73
Current and Voltage	74
Type of Current	74
Polarity	74
Arc Length	75
Manipulation of Electrode	75
Number of Layers	75
Peening	75
Welding Speed	75
Cold Rolled 18–8	75
Special Methods	75
Atomic Hydrogen Welding	75
Carbon Arc Welding	75
Recovery	75
Metal Arc Welding	75
Oxyacetylene Welding	76
Mechanical Properties	76
Metal Arc Welds	76
Oxyacetylene Welds	77
Metallurgy	78
Heat Treatment	78
Corrosion	79
Resistance Welding	79
Spot Welding	79
Properties of Spot Welds	81
Projection Welding	82
Seam Welding	82

* Secretary, Welding Research Committee.
† Research Assistant, Welding Research Committee.

	Page
Flash Welding	82
Resistance Butt Welding	83
18-8 Welding Rods	83
18-8 Electrodes	83
18-8 Oxyacetylene Rods	84
Surfacing with 18-8 Rods	85
Welding 18-8 to Plain Steel	85
Resistance Welding	85
Metal Arc Welding	85
Oxyacetylene Welding	86
Welding 18-8 to Non-Ferrous Metals	86
Welding 18-8 with Additions	86
Columbium	86
Manganese	87
Manganese-Columbium	87
Molybdenum	87
Molybdenum-Columbium	87
Nitrogen	87
Silicon	87
Tantalum	88
Titanium	89
Tungsten	90
Vanadium	90
Welding Other High-Alloy Corrosion and Heat Resisting Steels	90
25 Cr-12 Ni	90
25 Cr-20 Ni	91
20 Cr-20 Ni	94
15 Cr-9 Ni	94
23 Ni-8 Cr	94
25 Ni-15 Cr	94
35 Ni-15 Cr	95
18 Cr-8 Mn	95
18 Cr-9 Cu	95
Welding Stainless Irons	95
13 Cr Irons	96
18 Cr Irons	97
25 Cr Irons	100
Welding Martensitic Stainless Steels	101
Oxyacetylene Welding	101
Arc Welding	102
Atomic Hydrogen Welding	102
Resistance Welding	102
Soft Soldering	102
Silver Soldering	103
Brazing	104
Forge Welding	104
Pickling and Polishing	104
Flame Cutting	105
Acknowledgments	105
Bibliography	105
Suggested Research Problems	107

Summary

(A Tabular Summary is included at end of this section)

WELDING PROCEDURES FOR 18-8

In view of the low thermal conductivity, high thermal expansion and the necessity for maintaining carbon and chromium contents at low and high levels, respectively, the basic rules in welding 18-8 are: (1) do not use excessive heat; (2) the welding process must not carburize or oxidize the 18-8. These two principles underlie all welding procedures for 18-8.

Oxyacetylene Welding

In welding 18-8 sheet thinner than 0.05 inch oxyacetylene welding often is used.

Preparation of Edges.—Flange type welds without filler rod are recommended for sheets up to 16 or 18 gage (0.0625–0.050 inch). The edges are turned up $^1/_{16}$ inch. Plates thicker than $^1/_8$ inch are beveled. As in welding mild steel sheet, corner and edge welding favor distortion but often cannot be avoided. Any scale on the edges should be sand blasted with new sand, or ground off, or swabbed with 50% HCl before welding. Scratch brushing should follow the pickle. Chill bars, copper backing strips and clamps should be applied whenever feasible.

Filler Rod.—Bare rod is used and it should yield a deposit of the same chromium and nickel content as base metal. Since the weld metal may pick up as much as 0.02% C under good conditions, the carbon content of the rod should be perhaps a bit lower than base metal. Stabilized rods are discussed in the sections on Welding 18-8 with Additions. The diameter of the rod is usually a little greater than the thickness of the sheet.

Starting the Weld.—Some experts emphasize the importance of starting about 2 inches from a free end and welding in both directions from this starting point to reduce distortion.

Size of Torch.—The tip should be 1 or 2 sizes smaller than for welding mild steel of the same thickness.

Type of Flame.—Since neither oxidation nor carburization of the 18-8 puddle is permissible, it is general practice to use a neutral flame (equal parts by volume of oxygen and acetylene). A neutral flame being difficult to maintain, practically all authorities recommend that the flame have slightly excess acetylene rather than excess oxygen. An oxidizing flame creates porosity, whereas a reducing flame increases the carbon content of the weld.

Flux.—Although the majority agree that flux is necessary, particularly on the underside, methods of applying flux differ greatly, and there is a significant minority who regard flux as unessential.

Forehand or Backhand Welding.—Forehand (lefthand) welding is best for sheet less than $^1/_8$ inch thick with or without filler rod; backhand welding is best for thicker material.

Manipulation of Torch.—The weld should be made rapidly with as little movement of the torch as possible. Rapid welding minimizes distortion and prevents boiling of the puddle. Puddling must be strictly avoided because it produces porosity and coarse grain structure and increases distortion.

Metal Arc Welding

Metal arc welding generally is used to weld 18-8 plates thicker than $^1/_{16}$ inch.

Preparation of Edges.—As in oxyacetylene welding the edges to be welded should be free from scale, grease and oil. Beveling is not required for sheet thinner than $^1/_8$ inch. For heavier sheet and plate a 60° V weld is used, the scarfing extending to within $^1/_{16}$ to $^3/_{32}$ inch of the bottom. Plates $^3/_4$ inch and thicker (up to 1 inch) are double U or double V (60°) welded, with $^1/_8$-inch shoulder.

Thin sheet up to and including 0.050 inch (18 gage) is always clamped and butted tightly without opening. Heavier sheet is opened or butted tightly as in welding mild steel.

Tacks, if used, should be small, should be cleaned free from slag, and should not be hammered.

Clamps are used for positioning and heat removal. The heat effects due to welding are more severe on 18–8 than on mild steel, because the thermal conductivity of 18–8 is smaller and the coefficient of expansion is higher. Copper backing strips $^1/_2$ inch thick are often used to assist in heat removal.

Composition of Electrodes.—Coated electrodes having a thickness approximating that of the members to be joined with a maximum diameter of $^3/_{16}$ inch are generally used. Authorities in this country prefer an 18–8 core rod, although electrodes with a mild steel core rod and alloying elements in the coating have been used. The core rod should contain not over 0.07 C and the coating should be free from carbon. The U. S. Navy required that the core rod of the electrode for 18–8 contain 0.08 C max., 0.20–2.0 Mn, 0.030 P max., 0.035 S max., 0.50 Si max., 19.0 Cr min., 8.0 Ni min.

Current and Voltage.—As a rule the current is 10 to 15% less than for mild steel. The open circuit voltage rises from 30 to 80 as the diameter of the electrode is increased, but the arc voltage is about the same as for mild steel, 35 volts being usually the maximum. A short arc is always used.

Manipulation of Electrode.—Puddling is undesirable on account of overheating and attendant cracking. Back-step procedure is best to avoid distortion, especially in heavy material. A slight weaving motion without excessive side weaving, but with steady forward travel of the electrode, is a good procedure.

Number of Layers.—Sheet and plate up to $^1/_4$ inch thick are welded in a single layer. When multilayers are necessary, several light beads are less likely to crack and have better corrosion resistance that fewer heavy beads. Peening lightly is useful for correcting distortion and for avoiding cracks in the first bead. A jet of water on the underside is sometimes used to prevent carbide precipitation.

Atomic Hydrogen Welding

Atomic hydrogen welding is applicable particularly to 18–8 sheet. The heat input is intermediate between metal arc and oxyacetylene welding. Consequently, clamping devices and backing strips are recommended. No flux is needed, a filler rod of the same composition as base metal may be used, and the welds are remarkably smooth.

MECHANICAL PROPERTIES

Static and impact tensile tests at +20 to −80° C., on machined unheat treated butt welds made with a filler rod containing 0.083 C, 19.72 Cr, 8.91 Ni showed that static tensile strength and impact tensile fracture energy increased as the temperature was lowered. The static and impact ductilities were a little lower at −80 than at +20° C. The average tensile impact value of the welded joints ranged from 60 to 72% of the base metal.

Arc welds in steel containing 0.08 C, 17 Cr, 9 Ni had a tensile strength of 107,000 lb./in.^{2} unmachined, 95,500 lb./in.^{2} with reinforcement machined flush. Unwelded base metal had a tensile strength of 90,000 lb./in^2.

The rotating beam endurance limit of unmachined oxyacetylene butt-welded 18–8 tubes 1 inch o.d., $^1/_{16}$ inch wall, using 18–8 filler rods was found to be 16,000 lb./in.^{2} or 67% of the unwelded tubing. Heat treatment after welding ($^1/_2$ hr. at 870° C.) raised the endurance limit nearly 50% (equal to the base metal) without affecting the static tensile strength.

HEAT TREATMENT

Intergranular corrosion occasioned by carbide precipitation is bound to occur in the heat-affected zone of welded 18–8 in severely corrosive reagents, unless the joint is heat treated or unless the 18–8 contains an alloying element to prevent intergranular corrosion, or unless the carbon is exceedingly low. The A. S. M. E. Boiler Code Committee ruled that welded alloy 18–8 (0.07 C max., 0.40–2.50 Mn, at least 17 Cr, at least 9.5 Ni, alloyed with Cb (10 × carbon content, 1% Cb max.) or Ti (6 × carbon content, 0.60 Ti max.)) must be stress relieved, if the unfired pressure vessel is classified under Par. U-68. The 18–8 containing Cb is heated at not less than 840° C. (1550° F.) for 1 hr. per inch of thickness but not less than 2 hr. The 18–8 containing Ti is heated at not less than 840° C. (1550° F.) nor more than 900° C. (1650° F.) for 1 hr. per inch of thickness but not less than 4 hr. The welded structure is cooled slowly in a still atmosphere. Local stress relief is not permitted. According to Navy Department Specifications (1937), annealing (not less than 1070° C. (1950° F.) for 1 hr. per inch of thickest section) of welded 18–8 must not be done unless the entire structure can be water-quenched.

RESISTANCE WELDING

Spot Welding.—The principles of spot welding 18–8 are based on its low thermal and electric conductivity. Less power is required than in mild steel and this is accomplished by faster timing (from fractions to a few cycles of 60 cycle current). Due to greater stiffness more pressure is needed. To avoid depressions created by the heavy pressure combined with the low yield strength of 18–8, a copper block is sometimes inserted between the electrode and the polished side of the sheet.

For reasons of good corrosion resistance as well as strength the fused and severely heat-affected zone should amount to not less than 40 nor more than 80% of the thickness of the joint. The pressure should be 50 to 100% greater than for mild steel, as high as 50,000 lb./in.^{2} giving the best results with polished 18–8, 0.028 inch thick.

A spot of greater diameter than four times sheet thickness pulls a slug clear of the sheet, whereas smaller spots shear between the sheets. In designing spot welds in 18–8 one designer relied upon 32,000 T lb. static shear load for each spot in sheets the minimum thickness of which is T inches.

Seam Welding.—In the seam welding of vacuum tight and other parts of an 18–8 refrigerator evaporator, 0.025 inch thick, the weld was made on a manually fed machine at the rate of 72 inches/min. with the interrupter operation 2 cycles "on," 2 cycles "off." The 60-cycle current was 6300 amp., pressure 600 lb.

Flash Welding.—Satisfactory flash welds may be produced in 18–8 with shorter timing than in mild steel of the same thickness. The tensile properties of flash welded 18–8 are close to annealed base metal.

18–8 WELDING RODS

18–8 Electrodes.—Coated electrodes are always used. Tensile strengths of all-weld-metal deposits vary from 70,000 to 85,000 lb. per sq. in. The chief applications of the 18–8 electrode, aside from welding 18–8, are in welding plain carbon and low-alloy steels, as well as alloy steels that are otherwise difficult to weld. In plain carbon steels with 0.6 to 0.8 C, the 18–8 electrode makes good welds, but the zone which has been heated above the

critical range is hard and brittle. Boiler and pipe steels (Cr-Ni-Mo, Ni or plain carbon) are conveniently welded with 18–8 electrodes but base metal is best preheated. The 18–8 electrode is sometimes used for 4–6 Cr steel, and for avoiding cracks in welding 12–14 Cr iron.

18–8 Oxyacetylene Rods.—An oxyacetylene all-weld-metal made with an 18–8 rod had a tensile strength of 79,500 lb./in.^{2}. There was practically no hardening in the heat-affected zone of an oxyacetylene butt weld in mild steel (0.05 C, 0.31 Mn). Prolonged application of the flame produced a slightly overheated structure in the mild steel adjacent to the root of the weld. 18–8 rod is sometimes used for oxyacetylene welding stainless irons (16–30 Cr, 0.10–0.30 C), and low-alloy steel piping less than $^1/_4$ inch thick.

Surfacing with 18–8 Rods.—The chief application has been to cast iron turbine runners. The method consists of chipping out the eroded metal, studding, applying a priming coat of low carbon steel (0.12 C, 0.45 Mn, 0.05 Si), building up with two beads of 18 Cr iron (0.10 C, 18 Cr, 0.40 Si, 0.45 Mn), and finishing with a layer of 18–8 (0.07 C, 0.45 Mn, 0.40 Si, 18 Cr, 6 Ni) $^3/_{16}$ to $^1/_4$ inch thick. If too much heat was applied the entire deposit peeled off, carrying with it a slice of cast iron $^1/_{64}$ to $^1/_{32}$ inch thick. In view of the difference in coefficient of expansion between the ferritic materials and 18–8, peening was essential.

WELDING 18–8 TO PLAIN STEEL

Resistance Welding.—Successful resistance welding of 18–8 to plain carbon steel is entirely feasible, but there is bound to be a hard, martensitic zone. The joints almost invariably break under load in the plain carbon steel at a section removed from the weld.

In resistance butt welding mild steel to 18–8 the mild steel must project farther from the grips than 18–8 to compensate for the higher specific resistance of 18–8.

Metal Arc Welding.—In arc welding 18–8 to plain carbon steel there is a thin, hard, martensitic band on the steel side caused by diffusion of Cr and Ni. The majority favor the use of 18–8 electrodes. Preheating is sometimes advisable even when an 18–8 electrode is used, or an electrode containing 25 Cr, 12 Ni may be used. Unstabilized 18–8 may be seriously affected if preheating is prolonged or is at a high temperature.

Oxyacetylene Welding.—If the steel sheet is thicker than the 18–8 use a low-carbon 18–8 rod, according to one authority.

WELDING 18–8 WITH ADDITIONS

Columbium.—Columbium is added to 18–8 to prevent intergranular corrosion. An 18–8 containing columbium, if it is properly made (and also sometimes heat treated), is said to be stabilized. The A. S. M. E. Boiler Code Committee permits a design stress of 15,000 lb./in.^{2} with 90% joint efficiency up to 480° C. (900° F.) for welded, stress relieved 18–8 containing Cb (Cb = 10 × carbon content, not over 1% Cb) for unfired pressure vessels.

Molybdenum.—Molybdenum (2 to 5%) is added to 18–8 to increase the resistance to corrosion, particularly by non-oxidizing reagents such as HCl and H_2SO_4. From the standpoint of ease of welding, molybdenum does not have an unfavorable effect up to 5%.

Titanium.—Titanium is added to 18–8 to prevent intergranular corrosion. An 18–8 containing titanium, if it is properly made (and also sometimes heat treated), is said to be stabilized. Oxyacetylene and arc welds had about the same tensile properties on welded sheet 0.12 inch thick containing 0.11 C, 0.7 Si, 0.5 Mn, 17.6 Cr, 9.1 Ni, 0.5 Ti. The tensile specimens fractured outside the weld. Bend specimens withstood 180° bending without failure. The A. S. M. E. Boiler Code Committee established an allowable design stress of 15,000 lb./in.^{2} with 90% joint efficiency up to 480° C. (900° F.) for welded, stress relieved 18–8 containing Ti (Ti = 6 × carbon content, not over 0.60 Ti) for unfired pressure vessels.

Tungsten.—It is generally agreed that tungsten retards but does not prevent intergranular corrosion after welding.

WELDING OTHER HIGH-ALLOY CORROSION AND HEAT RESISTING STEELS

25 Cr–12 Ni.—The procedure for oxyacetylene and metal arc welding of steels of the 25–12 type (0.20 C max.) is the same as for 18–8. The filler rod has the same composition as base metal. In oxyacetylene welding plenty of flux is needed, especially for plates over $^1/_4$ inch thick to form a fusible oxide slag. The electrode should be the positive pole except for especially heavy sections. Spot welding of 25–12 and higher alloy steels is particularly easy on account of their low thermal and electric conductivity, and low melting point. One author states that 25–12 is no less subject to intergranular corrosion than 18–8. It should be pointed out that the main application of 25–12 is in high temperature service up to 1100° C. Occasionally 25–12 electrodes are used for mild steel and stainless irons. In welding stainless iron (nickel-free) with 25–12 electrodes, nickel rapidly diffuses into the base metal, and the weld metal contains only 6% Ni.

25 Cr–20 Ni.—Although 25–20 is primarily a heat-resisting steel, it is widely used as a filler rod for welding mild steel. Welds made with 25–20 electrodes in plain carbon steel have high ductility up to 0.56 C, beyond which the ductility decreases. Besides being useful for welding plain carbon steels, particularly up to 0.35 C, the 25–20 electrode is suitable for low-alloy steels. Oxyacetylene welding with a 25–20 rod can be applied to plates up to 0.39 inch thick. A neutral flame is used with a tip one size smaller than for a mild steel rod of the same size. The rod usually is 0.12 to 0.20 inch diameter; 0.24 inch is exceptional. 25–20 is less subject to intergranular corrosion than 18–8, should be preheated for welding and need only be stress relieved after welding, unless the requirements are severe.

Satisfactory welds can be made in 20 Cr–20 Ni, 15 Cr–9 Ni, 25 Ni–15 Cr and 35 Ni–15 Cr under suitable conditions.

18 Cr–8 Mn.—Steel containing 0.12 C max., 17–19 Cr, 8.5–10 Mn, 0.75–1.10 Cu, 0.50 Ni max., 0.2–0.5 Si can be satisfactorily arc or oxyacetylene welded. Oxyacetylene welding is done with a good chromium flux and a neutral or slightly reducing flame. The steel was said to be easier to weld than 18 Cr–8 Ni steel. Arc welds have a tensile strength of 100,000 lb./in.^{2} or more with over 20% elongation in the bend test. Sometimes columbium, tantalum or titanium is added to prevent intergranular corrosion of welded joints.

WELDING STAINLESS IRONS

Stainless irons, or ferritic stainless steels as they are sometimes called, may be grouped in three classes: 1. 13 Cr, 0.12 C max. (11–15 Cr), 2. 18 Cr, 0.12 C max. (16–20 Cr), 3.25 Cr, 0.3 C max. (20–30 Cr). Class 1 is cap-

able of being heat treated (oil or air quenching) to 350–400 Brinell, the grain structure being refined in this way. The other two classes of stainless irons are completely or nearly completely ferritic at all temperatures. They are subject to coarse grain growth on being overheated, which cannot be removed by heat treatment and which renders the iron brittle at room temperature, although the hardness is always below 200 Brinell unless the irons have been cold worked. The principal difference between Class 2 and Class 3 irons is the better resistance to scaling and to corrosion by some reagents possessed by Class 3 iron. The main difficulty in welding martensitic (Class 1) stainless iron is air hardening and embrittlement of the heat-affected zone together with some loss of corrosion resistance. The remedy is annealing or quenching and tempering after welding. In Class 2 and 3 irons, on the other hand, embrittlement (at room temperature or below only; the welded joints are tough above 200° C.) from grain growth in the heat-affected zone is the principal difficulty, and cannot be overcome by heat treatment.

WELDING MARTENSITIC STAINLESS STEELS

Martensitic stainless steels generally contain 11 to 18 Cr with 0.15 to 1.0 C. Their outstanding application is cutlery. They are not recommended for welding any more than are plain carbon steels containing over 0.35 C whose structure they resemble in some respects. Annealing and slow cooling are usually essential, one author suggesting cooling from 775° C. (1425° F.) at not over 30° C. per hr. (50° F. per hr.) to 450° C. (850° F.). Cracking increases as the carbon and chromium contents are increased.

SILVER SOLDERING

On account of the good corrosion resistance and color match of silver solder it is used extensively for joining stainless steels. Although the manipulation of wire and torch is the same for stainless steels as for mild steel, the stainless surface requires a different flux with sometimes a different solder, and the flow point of the solder may be in the range of temperatures of air hardening (for stainless steels) or of carbide precipitation and intergranular corrosion (for unalloyed 18–8 and some stainless irons and steels).

BRAZING

On account of the tendency to intergranular penetration and consequent loss of ductility, brazing or bronze welding is not good practice for Cr and Cr-Ni corrosion resisting steels.

Welding Chromium and Chromium-Nickel Corrosion and Heat Resisting Steels. Tabular Summary

(The Welding of 4–6 Cr Steel Is Reviewed in THE WELDING JOURNAL, **17** (7) Suppl., 7–9(1938))

	Chromium-Nickel Non-Hardenable Austenitic Steels	Straight Chromium Hardenable (Martensitic) Stainless Irons	Straight Chromium Non-Hardenable (Ferritic) Stainless Irons	Cutlery Stainless Steel
Composition	18 Cr–8 Ni, 0.07 C max., 25 Cr–12 Ni, 25 Cr–20 Ni	11.5–15 Cr, 0.12 C max.	16–18 Cr, 0.12 C max., 20–30 Cr, up to 0.35 C max.	12–14 Cr, 0.25–0.40 C or 15–18 Cr, 0.60–1.0 C
Welding procedure	Metal Arc Welding (for material over 0.05 inch thick): Covered electrodes adapted to deposit metal of the same composition as base metal are recommended. The carbon content should be as low as possible. Low current; short arc; single layer if possible. Oxy-acetylene Welding (usually recommended for thin sheet but plates can also be welded): Rod adapted to deposit metal of same composition as base metal and low carbon. Neutral flame with barely perceptible acetylene feather; rod held in inner cone; no puddling; smaller tip than for mild steel. Spot Welding: Use as few cycles as possible. Do not fuse surface. Atomic Hydrogen Welding (for sheet): bare rod, clamps	There is no difficulty in making welded joints but the welds are brittle.	Same as column 1, the difficulty with warpage being less	There is no difficulty in making welded joints but the welds are brittle. Welding is seldom used
Precautions	Jig to prevent warping of sheet. Heat treatment is applied only for some applications	Heat treatment is essential. Preheat to 150–250° C. to avoid cracks	Preheating (100–200° C.) and heat treatment are desirable	Heat treatment is essential
Danger zone	Zone heated to, but not beyond, 500–800° C., where carbides precipitate in grain boundaries. Zone has inferior corrosion resistance to heat-treated base metal, depending upon carbon content and heat input of welding	Zone heated above 1000° C., which becomes hard and brittle on air cooling after welding	Zone heated above 1200° C., which becomes extremely coarse grained and brittle. Also zone subject to intergranular corrosion in the coarse grained zone near weld	Zone heated above 1000° C., which becomes hard and brittle on air cooling after welding

Alloying elements to eliminate danger zone	Usually Columbium (= 10 times carbon content) or Titanium (= 6 times carbon content)	No results available	Ti and Cb (0.5%) may prevent intergranular corrosion and loss of ductility in arc welded joints. Nitrogen (0.008 × Cr) restrains grain growth and increases strength and ductility	No results available
Annealing temperature	Stress relief, 850–900° C., cool slowly. Full softening, 1100° C. followed by rapid cooling	750–830° C., long hold, cool slowly	850° C., long hold, cool rapidly or slowly (does not refine coarse grain structure)	800–900°C., cool slowly
Hardening temperature	Can be hardened only by cold work	780–920° C., oil quench	Can be hardened only by cold work	1000–1050° C., oil quench
Mechanical properties*				
Yield strength lb./in.² annealed unwelded	35,000–45,000 (18–8)	40,000	40,000–60,000	65,000
Yield strength hardened or cold worked, unwelded	60,000–250,000 (C. W.)(18–8)	150,000 (H)	90,000 (C. W.)	200,000–220,000 (H)
Yield strength, welded	45,000 (18–8)	30,000 unannealed 50,000 annealed	45,000	...
Tensile strength lb./in.² annealed, unwelded	80,000–95,000 (18–8)	75,000	75,000–95,000	100,000
Tensile strength hardened or cold worked, unwelded	105,000–300,000 (18–8)	190,000 (H)	100,000–175,000 (C. W.)	230,000–260,000 (H)
Tensile strength, welded†	85,000 (18–8)	45,000 unannealed 75,000 annealed	70,000	...
Elongation, % in 2 inches, annealed, unwelded	65–55 (18–8)	35	30–20	27
Elongation, % hardened or cold worked, unwelded	50–2 (in 10 inches) (C. W.) (18–8)	10 (H)	25–2 (C. W.)	8-2 (H)
Elongation, % in 2 inches, welded†	50 (18–8)	5 unannealed 20 annealed	5 unannealed 20 annealed	Nearly zero
Izod impact value, (20° C.) ft.-lb. annealed, unwelded	75–110 (18–8)	80–120	‡25 (15–18 Cr), 8 (25 Cr)	30
Izod (20° C.) hardened or cold worked	75 (C. W.) (18–8)	24 (H)	‡15 (C. W.) (15–18 Cr) 2 (C. W.) (25 Cr)	3 (H)
Izod (20° C.) welded†	75 (18–8)	...	‡1 unannealed 15 annealed (15–18 Cr)	...
Brinell hardness, annealed, unwelded	135–185 (18–8)	140	160–190	170
Brinell hardened or cold worked, unwelded	170–460 (C. W.) (18–8)	Up to 450 (H)	185–250 (C. W.)	Up to 600 (H)
Brinell, welded†	140 (18–8)	450 (sheet)	Up to 180	Up to 600
Electric resistance	7 times annealed mild steel	5 times annealed mild steel	5½ to 6 times annealed mild steel	5 times annealed mild steel
Corrosion resistance	Best of any group, resists most acids. Alkalies and salt water. Scale resistance up to 900–1150° C.	Not so good as higher chromium steels. Resists mildly corrosive attack. Scale resistance up to 600–700° C.	Increases with chromium content, in general. Not so satisfactory as 18–8 for severe service; scale resistance: 18 Cr—up to 900° C.; 25 Cr—up to 1150° C.	Same as column 2
Thermal coefficient of expansion (0–100° C.)	50% greater than mild steel	90% of mild steel	90% of mild steel	90% of mild steel
Thermal conductivity, 20° C.	30 to 40% of mild steel	50% of mild steel	40 to 50% of mild steel	60% of mild steel

* In the absence of a single comprehensive source, the mechanical properties have been selected from a large number of sources. The values are not averages of all the available test records but represent as closely as possible the attainments of good recent practice. H = Hardened condition; C. W. = Cold worked condition.

† Butt welds made by arc or gas process.

‡ The sharp drop in Izod value at room temperature occurs at 16–17% Cr. At higher temperatures the irons are tough.

Welding Chromium and Chromium-Nickel Corrosion and Heat Resisting Steels

INTRODUCTION

MUCH of the development of corrosion resisting steels has centered about welding. It is the purpose of the present review of literature to show to what point development has attained from the standpoint of both welding procedure and the properties to be expected of the welds.

The chromium and chromium-nickel corrosion resisting steels contain over 10% of alloying elements and retain few of the characteristics of unalloyed steel. There are three main types: austenitic Cr-Ni steels, martensitic

stainless steels, and ferritic stainless irons, the latter two types containing no nickel. All types have $^1/_3$ to $^1/_2$ the thermal conductivity and $^1/_7$ to $^1/_5$ the electric conductivity of unalloyed iron. The austenitic steels have 50% higher coefficient of thermal expansion than unalloyed iron and are not magnetic, whereas the martensitic and ferritic types have 10% lower coefficient of expansion than iron and are magnetic. Further general information relating to the corrosion resisting steels may be obtained from the Book of Stainless Steels (second edn. 1935, American Society for Metals, Cleveland, Ohio), the Metals Handbook and the numerous manufacturer's catalogs.

WELDING PROCEDURES FOR 18-8

In view of the low thermal conductivity, high thermal expansion, and the necessity for maintaining carbon and chromium contents at low and high levels, respectively, the basic rules in welding 18-8 are: (1) do not use excessive heat; (2) the welding process must not carburize or oxidize the 18-8. These two principles underlie all welding procedures for 18-8, which are summarized step by step in the following paragraphs.

Oxyacetylene Welding

Field of Application.—Aside from general considerations, such as non-availability of electric power and close quarters, it has been found that the oxyacetylene process is especially adapted to thin sheet, Table 1. Whitmer[1]

Table 1—Thickness of 18-8 for Which the Oxyacetylene Process Is the Preferred Process

Reference	Sheet Thickness, Inch
Machinery[2]—1938	0.050–0.0375 and lighter (18–20 U. S. gage and lighter)
Meslier[3]—1938	0.04 to 0.12 (two torch vertical method is applicable to plate 0.10 inch and over)
Whitmer[1]—1937	0.050 and lighter
Plücker[4]—1937	Up to 0.12
Taylor[5]—1937	Lighter than 0.0375 (20 gage)
Hatfield[6]—1936	Up to 0.08
Jones[7]—1934	0.063 ($^1/_{16}$ inch) and lighter
Hall[8]—1930	Up to $^1/_8$

and others consider the upper limit to be 0.05 inch beyond which the metal arc process is preferable. In France, some authorities apply resistance welding below 0.04 inch, wherever possible, sheets over 0.12 inch thick being arc welded. French oxyacetylene practice is different from procedure in this country in the inclination of the torch to the weld, the preference for a torch having a supplementary preheating flame, and the adoption of two torch vertical welding for the heavier gages.

Preparation of Edges.—Flange type welds without filler rod are recommended by Miller[9] and *Machinery*[2] for sheets up to 16 or 18 gage (0.0625–0.050 inch). The edges are turned up $^1/_{16}$ inch. Plates thicker than 0.10 inch (Boutté[10]), or $^1/_8$ inch (Miller) or 7 gage (0.1875 inch, *Machinery*) are beveled. Leroy[11] and Boutté used 90° V welds over 0.12 inch thick; Holler[12] used 60–80° V. As in welding mild steel sheet, corner and edge welding favor distortion but often cannot be avoided. Any scale on the edges should be sand blasted with new sand, or ground off, or swabbed with 50% HCl before welding, according to Phillips[13] and Thompson.[14] Scratch brushing should follow the pickle. The reduction in corrosion resistance caused by particles of the plain-carbon steel wire becoming embedded in the 18-8 should discourage scratch brushing. Pipe $^1/_8$ inch thick and over[277] (instead of $^3/_{16}$ inch and over for mild steel) is beveled to 90° V without shoulder. Good root fusion without overheating is assured by the sharp bevel together with small spacing.

Spacing and Clamping.—Chill bars, copper backing strips and clamps should be applied whenever feasible. According to Leitner,[15] the ends of the sheets tend to open out greatly during welding. The following spacings have been suggested:

Taylor[5]—1937 = $^1/_2$ sheet thickness for good penetration.

Sheet Metal Worker[16]—1937 = $^1/_4$ inch per foot of seam for 14 gage (0.078 inch); for lighter sheet, 1 sheet thickness per foot or 8 inches of seam.

Miller[17]—1937 = at least $^1/_{16}$ inch.

Sheet[2] should be tacked every $^3/_4$ inch. Pipe[277] should be spaced $^1/_{32}$ to $^1/_{16}$ inch for tack welding.

Filler Rod Composition.—All agree[14,18,19,20] that the rod should have the same chromium and nickel content as base metal. Without stating so directly, these authorities doubtless preferred[299] the rod to have a slightly higher chromium content than base metal as a precaution against loss during welding (private communications from a number of sources, 1938). Since the weld metal may pick up as much as 0.02% C under good conditions, the carbon content of the rod should be perhaps a bit lower than base metal. Stabilized rods are discussed in the sections on Welding 18-8 with Additions. Monel rods[13] have been used successfully to weld 18-8. Their low melting point was said to reduce grain growth. However, several welding shops (private communications, 1938) have had disastrous results with Monel welded 18-8.

Filler Rod Size.—The diameter of the rod is usually a little greater than the thickness of the sheet;[17] 2 gages thicker, according to Carmichael and Shaw.[21] *Machinery*[2] states that rods over $^3/_{32}$ inch diameter are seldom used. The same size is used by Boutté[22] for 18-8 as for mild steel, but he frowns upon the use of strip or sheet trimmings. *Machinery* does not.

Preheating.—Preheating is never resorted to. Distortion[9] of thin sheet may be counteracted to some extent by intelligent torch preheating. Wachowitz[23] is the only investigator who recommends preheating, which he states is used only for tank work if the sheets are not polished.

Starting the Weld.—In 1929 Holler[12] emphasized the importance of starting about 2 inches from a free end and welding in both directions from this starting point. Later, Pratt[24] made a similar recommendation.

Size of Torch.—The majority[2,6,21,25,26,27] believes that the tip should be 1 or 2 sizes smaller than for welding mild steel of the same thickness. Phillips[13] and *Sheet Metal Worker*,[28] on the other hand, state that the tip should be a size larger than for mild steel. The gage pressure should be 5 lb./in.^{2} for both oxygen and acetylene, according to Van Dyke.[27] If the pressure is too high, according to Raymond,[29] holes may be blown through thin sheet. French authorities[11,22,30,31] use a torch delivering 4.2 cu. ft. acetylene per hour for each $^1/_{16}$ inch of sheet thickness, whereas Bainbridge[32] recommends rather less:

Thickness, inch	$^1/_{32}$	$^1/_{16}$	$^1/_8$
Cu. ft. acetylene per hr.	1.25	2.7	8.0

Special Torch.—A special torch is often used for 18-8 in France in which an air-acetylene flame follows the oxyacetylene flame. Boutté[10] and Leroy[11] state that the supplementary flame is reducing and prevents oxidation

of the weld. The capacity is shown in Table 2. The air-acetylene flame may be replaced by an oil flame which is said[11] to have a good effect on penetration.

Table 2—Acetylene Consumption of Special French Torch. Boutté[10]

Thickness to Be Welded, Inch	Cu. Ft. Acetylene per Hr. in Welding Torch	Cu. Ft. Acetylene per Hr., in Supplementary Flame
0.02–0.06	1.5 to 4.4	0.7
0.08–0.20	5.3 to 12.5	1.5
0.24 and over	17.5 and more	2.8

Two Torch Vertical Method.—Each torch should deliver 1.1 cu. ft. acetylene per $^1/_{16}$ inch of plate thickness, the welding being done as in mild steel.[11] As in mild steel, the two torch vertical method conserves welding gases.

Type of Flame.—Since neither oxidation nor carburization of the 18–8 puddle is permissible, it is general practice to use a neutral flame[15,22,33,34] (equal parts by volume of oxygen and acetylene). A neutral flame being difficult to maintain, practically all authorities[19,21,25,27,32] recommended that the flame have slightly excess acetylene rather than excess oxygen. Excess oxygen gives rise to porosity and surface pinholes. If the acetylene feather on the end of the inner blue cone is not over $^1/_{16}$[1,35,36] or $^1/_8$[2] inch long, the flame is properly adjusted and the carbon pick up by the weld puddle is negligible. According to Plücker,[4] the setting of the flame is correct when, on decreasing the oxygen supply, the gray discoloration on the plate under the flame just disappears.

Although *Machinery*[2] favors the slightly reducing flame for use with ordinary fluxes, it recommends that a slightly oxidizing flame be used with the cryolite flux sometimes required when columbium-bearing rods are used for titanium-stabilized 18–8. Under any circumstances, Norris[37] in 1931 used a neutral flame with a very slight excess of oxygen. His statement that the excess oxygen makes the metal fuse faster is not reassuring. In 1930 Lansing[38] claimed that oxyacetylene welded 18–8 (0.2 C max.) had higher strength using a reducing flame than using a neutral flame, and that the ductilities were identical.

Flux.—Although the majority agree that flux is necessary, methods of applying flux differ greatly, and there is a significant minority who regard flux as unessential. The use of flux is strongly recommended by Ward,[39] Duncan,[40] and *Industry & Welding.*[18] Best practice, according to Taylor,[5] is to apply flux to the underside only of the edges to be welded. For some reason flux is not applied to the underside in other fusion welding processes. If flux is applied to the rod, the weld is porous. According to J. H. Zimmerman (private communication, Dec. 1938), porosity is not necessarily produced by flux on the rod, provided flux also is applied to the underside of the weld. Whitmer[1] considers it essential to have flux on the underside, and Boutté[22] and Hatfield[6] state that flux on the underside is a great aid in securing penetration. Hall[8] found that flux on the underside is not essential but is advantageous in securing a smooth neat bead, easy to grind and polish, as *Machinery*[2] points out. Titanium stabilized steels require flux, according to Hatfield[6] and *Machinery*[2], whereas with other grades of 18–8 the use of flux is not essential. The use of flux on the rod is recommended by Boutté,[10] who dips the rod in waterglass before spreading on the flux. Jones[7] and Raymond[29] apply flux to both sides of the sheets, but not on the rod. Still another variation in procedure is the application of flux to both sides of the sheets and pipes[27] as well as to the rod (*Sheet Metal Worker*[16]).

In most instances the composition of the flux is not stated, special proprietary fluxes[2,7,41] being relied upon. Moreau[42] suggested a boric acid-sodium silicate flux to dissolve chromium oxides. A flux of 45% borax, 55% boric acid was recommended by Eyles,[43] and another consisting of a sirupy solution of waterglass containing equal parts of sodium silicate, boric acid and fused borax by Spraragen.[44] A borax, boric acid and waterglass mixture was believed by Plücker[4] to prevent carbon pick up and formation of Cr_2O_3. In Green's[45] opinion, calcium fluoride (CaF_2) is the only successful flux for 18–8 Gordon,[36] who regards the use of flux on the underside as a matter of personal taste, states that proprietary fluxes usually consist of borax with salt or china clay, sometimes with an addition of fluorspar. Copper welding fluxes made up with oil were suggested by Trunschitz.[46] The heat of combustion of the oil was erroneously supposed to decrease the surface tension of the puddle. Johnson's[47] flux consisted of boron oxide, sodium oxide and silica, doubtless another way of stating borax and waterglass.

The belief that flux is unnecessary is shared by P. H. Miller[17] and Thompson[14] in England. Trunschitz[46] also regarded flux as unnecessary for plates over 0.08 inch thick. According to Zeyen,[48] flux is necessary only for alloys containing over 40% Ni.

Forehand or Backhand Welding.—Forehand (lefthand) welding[24] is best for light sheet below 0.08[33,46] or $^1/_8$ thick with or without filler rod; backhand welding is best for thicker material. According to Priestley,[34] backhand welding can be used with a columbium-bearing rod because weld metal and adjacent sheet are held at a dull red longer than with forehand welding. For this reason distortion was said to be minimized, although the contrary also might be deduced. Besides being faster than forehand welding for 18–8 pipes, backhand welding[27] reduces joint contraction ahead of the weld, an important factor with beveled ends. Forehand welding was preferable for untreated stainless steel pipes in the thinner gages. Leitner[15] preferred backhand welding. As Thum[49] and others stated, forehand is best for the welding of flange-type seams.

Position of Inner Cone.—The rod should not be lifted clear of the inner cone of the flame, according to Clark[50] and *Machinery*,[2] otherwise oxidation is rapid. Although Carmichael and Shaw[21] assert that the inner cone should touch the sheet, they state that the filler rod should be kept near the inner cone, but should not be exposed to the blue part of the flame if spongy deposits are to be avoided. Leitner[15] strongly advises against permitting the inner cone to touch the puddle on account of carbon pick up. Foaming was attributed by Plücker[4] to holding the tip too close to the puddle.

Manipulation of Torch.—The weld should be made rapidly[9] with as little movement[10,11,27] of the torch as possible. Rapid welding minimizes distortion and, according to Thum,[49] prevents boiling of the puddle. Puddling must be strictly avoided[2,8,15,24,25] because it produces porosity[27] and coarse grain structure,[44] and increases distortion. Weaving is bad,[22] and torch manipulation must be restricted to a slight back and forth movement along the weld.[2] Within the limitations imposed by the preceding section the torch should be held as close as possible to the puddle,[27,51] and the rod should be held above the weld so that base metal and rod fuse simultaneously. Although French authorities[10,30] recommend that rod and torch be inclined to the weld, Fig. 1, *Machinery*[2] suggests that the flame should be pointed

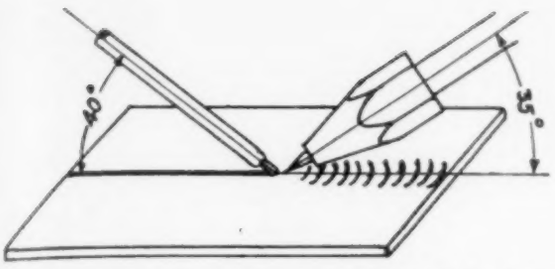


Fig. 1—Correct Position of Torch and Filler Rod in Welding 18-8, According to Boutté[10]

directly at the weld with rod in the reducing portion. Plücker's[4] reason for inclining the torch 35 to 40° to the weld is that the torch then drives the flux ahead.

Layers and Reverse Welding.—Although the general principle that as little heat as possible be used in welding might suggest that multilayer welding was beneficial, all authorities agree that the best weld is made in a single pass. Hignett[52] prefers using two torches simultaneously rather than depositing two layers. Retracing a hot weld is bad practice, according to Pratt.[24] An early German writer[20] suggested welding from both sides but his 18-8 contained 0.22 C. Without offering an explanation, Bignier[53] also recommended welding from both sides; possibly he had two torch vertical welding in mind. Nowadays, reverse welding is done only if absolutely necessary, in which case the underside[54] must be descaled before the reverse bead is deposited. For plate thicker than 0.12 inch, Boutté[10] and Leroy[11] recommend two passes, the length of each not exceeding 2 inches. A reinforcement of 0.1 to 0.3 times the thickness is recommended for butt-welded 18-8 pipe.[277]

Welding Speed.—Oxyacetylene welding, according to Miller,[17] requires 25% more time for 18-8 than for mild steel. Smith,[35] Miller[17] and Whitmer[1] found that oxyacetylene welding is slower than arc welding.

Metal Arc Welding

Field of Application.—Metal arc welding is called for when the plates are thicker than can be handled advantageously by the oxyacetylene process, Table 1. The oxyacetylene process is better than the arc for the thinner sheets because the temperature can be more closely controlled. With special equipment Hougardy[55] has metal arc-welded sheet as thin as 0.032 inch. Metal arc welding is best for sheet not less than 0.0375 inch thick (20 gage) in Taylor's[5] opinion. Johnston[56] limits metal arc welding to sheets not less than 0.050 inch thick (18 gage); great difficulty is experienced with thinner sheet. Carmichael and Shaw's[21] limit is 0.064 inch, and as an extreme case Meslier[3] considers metal arc welding suitable only for plate thicker than 0.16 inch.

Preparation of Edges.—As in oxyacetylene welding the edges to be welded should be free from scale, grease and oil. Beveling[57,58] is not required for sheet thinner than 1/8 inch. For heavier sheet and plate a 60° V weld is used, the scarfing extending[57] to within 1/16 to 3/32 inch of the bottom. Plates 3/4 inch and thicker (up to 1 inch) are double U (40°) or double V (60°) welded, with 1/8-inch shoulder. Similar details are given by Whitmer[1] and Carmichael and Shaw.[21] Boutté[22] recommends the same preparation as for mild steel. Wachowitz[23] is the only investigator who recommends preheating, which he states is used only for tank work if the plates are not polished.

Spacing and Clamping.—According to Johnston,[56] thin sheet up to and including 0.050 inch (18 gage) is always clamped and butted tightly without opening. Heavier sheet is opened or butted tightly as in welding mild steel. *Machinery*[57] recommends an opening of 1/2 sheet thickness from 0.0625 to 0.1875 inch (16 to 7 gage). The spacing for beveled welds is about 1/8 inch. In arc welding the longitudinal seam of an 18-8 drum 9/32 inch thick, 6 feet diam. 5 1/2 ft. long, a writer in *Nickel Bulletin*[284] allowed a gap of 3/8 inch at the end for contraction. Other details of construction of the welded filter are given, including the application of pre-distortion in making the final longitudinal weld, and the use of plug welds in reinforcing the fillet welds joining the end plates to the drum.

Clamping is always applied to sheet 0.050 inch and thinner. Johnston[56] suggests tacking 0.050 inch sheet (18 gage) at intervals of 2 to 4 inches, 0.141-inch sheet (10 gage) at intervals of 3 to 9 inches, unless clamps are employed. Sheets 0.109 inch (12 gage) and thicker often are welded without clamps, according to Johnston. On the other hand, *Machinery*[57] recommends clamping for all sheet up to 0.1875 inch (7 gage), and tacks especially for the thinner sheets, as close together as 3/4 inch. Tacks should be small, should be cleaned free from slag, and should not be hammered. Hammering[56] causes separation of the sheets.

Clamps are used for positioning and heat removal. Johnston[56] estimates that the heat effects due to welding are more severe on 18-8 than on mild steel, because the thermal conductivity of 18-8 is smaller and the coefficient of expansion is higher. By "heat effect" Johnston appears to have meant rate of cooling and distortion, for the metallurgical effect of heat on 18-8 during welding can be evaluated only in terms of 18-8, not of a totally different material, such as mild steel. Heat effect is a vague term for which there is no quantitative scale, despite Johnston's attempt to provide one.

Nearly all the expansion and contraction is confined by clamps to the weak, plastic weld metal. Without clamps the buckling limit of the sheet is exceeded. Copper backing strips[37,56,57,58] 1/2 inch thick often are used to assist in heat removal.

Composition of Electrodes.—Coated electrodes are used. Authorities in this country prefer an 18-8 core rod,[59] although electrodes with a mild steel core rod and alloying elements in the coating have been used. According to Jennings,[58] the core rod should contain not over 0.07 C and the coating should be free from carbon. *Machinery*[57] and Plücker[4] recommend that the alloy content (Cr and Ni) of the electrode should be slightly higher than that of base metal to offset loss by oxidation. The U. S. Navy[60] required that the core rod of the electrode for 18-8 contain 0.08 C max., 0.20-2.0 Mn, 0.030 P max., 0.035 S max., 0.50 Si max., 19.0 Cr min., 8.0 Ni min. Some authorities[21,58] state that electrode should have the same composition as base metal. The core rod should contain 13 to 14% Ni, according to Sasaki,[61] in order to lower the elastic limit of the weld metal thereby eliminating shrinkage stresses. No measurements of these stresses were reported.

The coating has several functions: it stabilizes the arc,[1] provides a protecting slag blanket over the metal and prevents pick up of oxygen and nitrogen.[59] A brief description of the manufacture of covered 18-8 electrodes by Prentiss[62] includes pickling with a non-acid substance, cold drawing, grinding to remove drawing compound, coating under a pressure of 25,000 lb./in.,[2] and drying. Bibber[59] in 1935 stated that coatings usually consist of silica and lime. Moisture and organic compounds (carbon pick up) always are low. Some coatings contain metallic chromium, others may contain calcium fluoride (beware of fluoride fume), and still others contain manganese (weld metal may contain up to

3.42 Mn). According to Thum[49] and Moritz,[63] coatings contain a deoxidizer (ferromanganese, ferrotitanium or aluminum), a slag-forming or oxide-dissolving material (lime, silica, clay or cryolite), and a binder (sodium silicate). According to Garriott[279] (1938), all chromium and chromium-nickel steels containing 4% Cr or more require the same kind of coating, of which two types have been developed. A coating made of calcium carbonate, titanium dioxide, sodium silicate and ferromanganese deposits a smoother bead than the fluoride type of coating in which calcium fluoride is substituted for TiO_2. The fluoride coating removes the oxides from the weld metal, the carbon dioxide from the $CaCO_3$, of which large proportions are necessary to raise the coating's melting point and to overcome blistering due to the low coefficient of thermal expansion of stainless steel, shielding the chromium from oxidation. A Russian coating[64] for an 18–8 electrode containing 0.6 Ti, 0.22 C had 48% $CaCO_3$, 30% feldspar, 8% ferromanganese, 8% waterglass, 3% TiO_2, 3% ferrochromium. In 30% H_2SO_4 the corrosion rate of weld metal was the same as base metal, which demonstrated the efficacy of the coating.

In 1929 Bull and Johnson[65] stated that 18–8 electrodes were of two types: (1) 18–8 core rod wrapped with asbestos twine and coated with a deoxidizer, such as ferromanganese, calcium silicide or aluminum; (2) mild steel core rod with alloying elements and deoxidizer in the coating (synthetic electrode). Several years later Thompson,[14] asserted that the synthetic electrode was not so satisfactory as the electrode with 18–8 core rod. Nevertheless, Harris,[66] following Monypenny's[19] lead, showed that the synthetic electrode was still in common use in 1935. A nickel plated mild steel core rod was covered with asbestos and a binder containing powdered ferrochromium and a deoxidizer. Sometimes the rod was wound with an aluminum wire, which acted as deoxidizer. The synthetic electrode usually contained 4% Cr beyond that desired in the weld.

The electrodes with 18–8 core rod deposit 18 oz. per kw.-hr., which is 50% greater than for medium steel electrodes, according to Bibber.[59] The rate is more constant for vertical and overhead welding than with unalloyed electrodes. The medium steel electrodes have 50% higher spatter loss than 18–8, the spatter loss depending to some extent on the coating.[57]

Current and Voltage.—Some suggested values for current and voltage are listed in Table 3. The values are lower[11] than for covered mild steel electrodes, and there has been a tendency in recent years to lower the current. As Boutté[10] states it, whereas 100–120 amp. may be used for a given thickness of mild steel, 90–100 amp. is sufficient for 18–8 of the same gage. As a rule the current is 10[1] to 15%[58] less than for mild steel. Johnston's[56] rule states that short circuit should be broken when the electrode is pulled away from the weld less than $^1/_{16}$ inch. In Bibber's[59] opinion the lower melting point of 18–8 accounts for the lower current compared with unalloyed steel, which does not have so smooth an arc as 18–8. If the current is too high, Spooner[67] asserts that gas holes are produced, or, according to Hougardy,[68] spatter will be excessive.

The open circuit voltage rises from 30 to 80 as the diameter of the electrode is increased, but the arc voltage is about the same as for mild steel, 35 volts being the maximum that can be expected to weld satisfactorily, according to Johnston[56] and others.

It seems clear from Table 3 that for thin material the electrode, of necessity, is thicker than the sheet, whereas for thick material the electrode is thinner than plate. Although a French authority[22] recommends the same size of electrodes for 18–8 as for unalloyed steel, Thum[49] states that electrodes over $^3/_{16}$ inch diameter sometimes produce porous welds. For overhead welding the diameter should not exceed $^1/_8$ inch, according to *Machinery*,[57] which states that penetration increases as size and current are increased. It appears to be believed commonly that large pools of molten metal, which are produced by large electrodes, give rise to cracks. The cracks are said to be created by the excessive shrinkage of the large pool, coarse grain structure also playing a part, perhaps. As Bibber[59] points out, center grip electrodes are best to prevent excessive heating and to avoid tip vibration with slender electrodes.

Type of Current.—D.C. is usually preferred[42,55] to A.C. because the D.C. is easier to manipulate,[36] is more suitable for all-position welding,[22] the current is easier to regulate,[73] and the required open-circuit voltage is higher with A.C.[74] Macfarlane[75] goes so far as to state that it is difficult to weld 18–8 with A.C. Yet Carmichael and Shaw[21] consider A.C. and D.C. equivalent, and Plücker[4] prefers A.C. for plate over 0.12 inch thick. Raymond[299] prefers A.C. under all circumstances. An A.C. welder with high frequency keep-alive circuit is recommended by Staggs[76] for 18–8.

Polarity.—With one exception,[67] the electrode is made the positive pole (reversed polarity), although Johnston[56]

Table 3—Current and Voltage for Covered 18–8 Electrodes

Diameter of Electrode, Inch	Current, Amp.	Voltage, Open Circuit	Sheet Thickness, Inch	Reference
$^5/_{64}$ (0.078)	25–50	30–35	0.050 and thinner	*Machinery*[57]—1938
$^3/_{32}$ (0.094)	45–60	...	....	Spooner[67]—1936
$^3/_{32}$ (0.094)	25–60 (flat)	20–24 (arc)	....	Lincoln Handbook[69]—1936
$^3/_{32}$ (0.094)	30–45 (overhead)	20–24 (arc)	....	Lincoln Handbook[69]—1936
$^3/_{32}$ (0.094)	50–75	35–40	....	Norris[37] and Alexander[70]—1931
$^3/_{32}$ (0.094)	40–60	30–40	$^1/_{16}$	Maurath[71]—1929
$^1/_8$ (0.125)	50–125	40–50	0.0625 to 0.188 ($^3/_{32}$-inch rods may be used for the thinner sheet, $^5/_{32}$-inch rods for the thicker sheets)	*Machinery*[57]—1938
$^1/_8$ (0.125)	90–100	...	....	Leitner[15]—1933
$^1/_8$ (0.125)	75–100	...	$^1/_8$	Spraragen[44,72]—1930
$^5/_{32}$ (0.156)	100–110	...	....	Spooner[67]—1936
$^5/_{32}$ (0.156)	120–150	...	....	Leitner[15]—1933
$^3/_{16}$ (0.188)	100–175	55–60	$^1/_4$ and thicker	*Machinery*[57]—1938
$^3/_{16}$ (0.188)	100–165	23–26 (arc)	....	Lincoln Handbook[69]—1936
$^3/_{16}$ (0.188)	150–175	....	$^1/_2$	Spraragen[44,72]—1930
$^3/_{16}$ (0.188)	100–175	55–60	$^5/_{32}$	Maurath[71]—1929
$^1/_4$ (0.250)	140–225	23–26 (arc)	....	Lincoln Handbook[69]—1936
$^1/_4$ (0.250)	225–275	...	....	Norris[37]—1931
$^1/_4$ (0.250)	225–300	70–80	....	Alexander[70]—1931

and *Machinery*[57] mention exceptional coatings and exceptional circumstances with heavy sections which demanded straight polarity for good penetration and fusion.

Arc Length.—A short arc is always used. Long arcs produce surface pinholes[77] and promote loss of alloying elements.[4]

Manipulation of Electrode.—Although Spooner[67] and Nielson[78] state that the electrode should be held vertical, *Machinery*[57] recommends that the electrode be sloped back 60°. Cracks lengthwise of a weld are proof of puddling, according to Johnston,[56] who believed that the overheating caused by puddling made the weld metal hot short. All agree that puddling is undesirable. Back step procedure is best to avoid distortion,[56] especially in heavy material.[57] In the back step procedure a short bead is started from one end of the seam. Another bead is started at the other end. The welder then returns to the first bead, etc. When starting a new electrode, strike the arc in the unwelded section a short distance from the weld.

Nielsen[78] recommended a smooth, overlapping weave, whereas Norris[87] believed a succession of long ovals was better than weaving to keep the puddle molten and allow the gas bubbles to rise. *Machinery*[57] suggested a slight weaving motion without excessive side weaving, but with steady forward travel of the electrode. In Spooner's[67] back-and-forth procedure, the electrode is moved forward a distance equal to its diameter to heat the edges, and then is carried back over the metal already deposited to fill the crater. In this way the electrode is deposited on heated steel, and a flat deposit is secured. With thin sheet as the welding progressed the sheet became hotter and it was necessary to increase the speed of welding. Straight beads often crack, according to Sasaki.[61] If the slag is not removed from the preceding bead, the subsequent layer will be porous.[35] The floating out of slag by successive layers cannot be relied upon.[1]

Number of Layers.—Sheet and plate up to $^1/_4$ inch thick are welded in a single layer, according to *Machinery*.[57] When multilayers are necessary, several light beads are less likely to crack and have better corrosion resistance than fewer heavy beads.[58] More layers than for the same thickness of unalloyed steel are required.[44] It is true that successive layers have an annealing effect[71] on preceding layers, but the effect may not be advantageous. Welding from both sides is desirable to obtain smooth beads on sheet that must be polished on both sides.[57]

Peening.—Peening lightly is useful for correcting distortion and for avoiding cracks in the first bead.[57,61] Peening with pneumatic hammers was successfully used[284] in stretching longitudinal butt welds and girth fillet welds to overcome buckling in an arc-welded 18–8 pulp filter $11^1/_2$ ft. long, 6 ft. diameter, $^9/_{32}$–$^3/_8$ inch thick. There was no difficulty, provided the weld metal was ductile. Welds in sheet should be ground flat to avoid cracks, Stephenson[79] states.

Welding Speed.—Metal arc welding of 18–8 occupies 10% more time than for the same thickness of unalloyed mild steel, according to Miller.[17]

Cold Rolled 18–8.—The metal arc welding of cold rolled 18–8 was not favored by Bibber[59] who stated that welding partially annealed the sheet and warpage may be excessive.

Special Methods.—In the Water Cooled Welding Process for 18–8 described by Holmberg,[60] a jet of cold water is played on the underside of the weld during welding in order to prevent carbide precipitation. Nielson[78] recommended blowing a blast of air immediately behind the crater from the back of the plate during welding. To produce a reducing atmosphere for welding Eyles[42] directed a Bunsen flame on the arc during welding. First class welds in 18–8 plate $^3/_4$ inch thick have been made in a single pass by Wallace[302] using the unionmelt process.

Atomic Hydrogen Welding

Good results are[1,14,22,81] secured with atomic hydrogen welding. The heat input is intermediate between metal arc and oxyacetylene welding. Consequently, clamping devices and backing strips are recommended. Leroy[11] believed that the process was restricted to thin sheet for the erroneous reason that no filler rod is used. The reason, according to *Machinery*,[57] is that sheets thicker than 0.109 inch (12 gage) are beyond the range of penetration of the process. No flux is needed, a filler rod of the same composition as base metal may be used,[19] and the welds are remarkably smooth. The welding procedure is the same as for oxyacetylene welding; that is, the sheet is sweated and fused to metal from the filler rod almost simultaneously.[36]

The atomic hydrogen process involves no gas containing carbon. The problem of carbide precipitation in base metal is not overcome despite Miller's[82] misleading statement that atomic hydrogen welds in 18–8 need no heat treatment because they are decarburized. Monypenny[19] states that 18–8 is less rapidly decarburized in atomic hydrogen welding than plain carbon steel. Atomic hydrogen welding does not prevent scaling on the underside, according to Hall.[8] That excellent penetration is attained in atomic hydrogen welding is attested by Catlett's[83] metallographic study.

Carbon Arc Welding

The carbon arc process is recommended by Gordon[36] for thin sheet. The carbon is negative, there was said to be no carbon pick up and the weld metal is fluid. Distortion was said to be minimized by using carbon arc welding. *Welding Engineer*[84] describes 18–8 gear guards butt welded from 18–8 sheet 0.078 inch thick (14 gage). The carbon electrode was $^5/_{32}$ inch diameter and the welding speed was 18 inches per minute. Janovsky[285] also was successful in automatic carbon arc welding stainless steel pipe (16 gage, $^3/_4$ inch diameter).

Some authorities are less sanguine about the carbon arc process. According to Spraragen,[72] the process is acceptable if an atmosphere of cracked ammonia is used. Hall[8] stated that carbon arc welding is not used for 18–8. It was found by Leverick[85] and *Machinery*[57] that carbon arc welding was not advisable on account of carbon pick up, which is in agreement with J. H. G. Monypenny (private communication, Dec. 1938).

RECOVERY

Metal Arc Welding.—Tests by Bibber,[59] Table 4, on a number of covered electrodes (18–8 core rod) showed that the deposits had less chromium than the electrodes. Unfortunately, the composition of coatings and slags was not reported. The coating was held responsible for most of the increased carbon content of the weld. Bibber's

Table 4—Composition of 18–8 Electrodes, Plate and Welds. Bibber[59]

	Composition of Electrodes		Composition of Base Metal	Composition of Weld Metal	
	Range	Average		Range	Average
Carbon, %	0.03– 0.10	0.07	0.09	0.08– 0.14	0.11
Manganese, %	0.31– 0.60	0.45	0.44	0.21– 3.42	...
Silicon, %	0.19– 0.95	0.53	0.16	0.27– 0.60	0.39
Nickel, %	8.53–10.31	9.07	9.61	9.10–10.80	9.95
Chromium, %	17.10–19.08	18.31	18.90	16.70–17.54	17.20
Copper, %	0.06– 0.14	0.11	0.08	trace to 0.15	0.03
Nitrogen, %	0.020–0.040	0.031	0.054	0.036–0.050	0.041

Table 5—Static and Impact Tensile Tests of Welded 18–8 at +20 to −80° C. Henry[88]

Temperature of Test, ° C.	Specimen	Static Tensile Test: Tensile Strength, Lb./In.²	Static Tensile Test: Elongation, % in 2 Inches	Static Tensile Test: Reduction of Area, %	Impact Tensile Test: Fracture Energy, Ft.-Lb.	Impact Tensile Test: Elongation, % in 0.80 Inch	Impact Tensile Test: Reduction of Area, %
+20	Unwelded	96,000	90	37	160	70	70
+20	Arc	80,000	39	42	95	37	50
+20	Oxyacetylene	50,000	8	13	110	46	68
−20	Unwelded	120,000	60	27	200	75	70
−20	Arc	80,000	17	17	130	40	50
−20	Oxyacetylene	60,000	12	11	115	40	35
−80	Unwelded	152,000	60	34	195	62	68
−80	Arc	123,000	30	30	130	35	35
−80	Oxyacetylene	65,000	8	12	140	39	23

results do not conflict with statements[10,49,57,66] that 1 to 1½% Cr is lost from the electrode. Thum[49] stated that metal arc welds usually contain 0.04–0.06% nitrogen, but Bibber's results suggest that little nitrogen is picked up from the welding atmosphere. Nitrogen pick up sometimes amounts to 0.30%, according to Thum.[49] Although Plücker[4] and Herrmann[86] believed that chromium was lost by evaporation, the more general belief is that chromium is absorbed as oxide by the slag. Plücker and Herrmann also believed mistakenly that 0.5% nickel was lost by oxidation to NiO. Although no quantitative information is available it is believed that loss of chromium increases[19] as the arc is lengthened. The loss of chromium[19,66] from synthetic electrodes (mild steel core rod) is 4%.

Oxyacetylene Welding

Most authorities[8] state that no elements are lost in oxyacetylene welding. Nevertheless Leitner[15] detected a loss of 2% Cr in his experiments. He found that 0.2 to 0.3% C was picked up if the acetylene feather was twice as long as the luminous cone. If the inner cone was repeatedly dipped into the melt, the carbon content rose to 1%.

In metal sprayed deposits of 18–8 there is a loss of 40, 10 and 2%, respectively, of the initial carbon, chromium and nickel contents of the feed wire, according to Rice.[87]

MECHANICAL PROPERTIES

In the following section the available information is summarized on the mechanical properties of welds in unstabilized 18–8. The mechanical properties of 18–8 weld metal is dealt with particularly in a later section on 18–8 Welding Rods. Stabilized 18–8 also is discussed in later sections.

Metal Arc Welds

1937—Henry[88]

Static and impact tensile tests at +20 to −80° C., Table 5, on machined unheat treated butt welds made with a filler rod containing 0.083 C, 19.72 Cr, 8.91 Ni showed that static tensile strength and impact tensile fracture energy increased as the temperature was lowered. The static and impact ductilities were a little lower at −80 than at +20° C. The scatter of results for the welds in both static and impact tensile tests being large, the average results in Table 5 are conservative. If low values were not considered in the averages, the arc welds would have made a better showing. Base metal for the impact tensile specimens was ⅝ inch thick and contained 0.06 C, 19.89 Cr, 9.21 Ni; for the static tensile specimens the plate was ¾ inch thick and contained 0.075 C, 19.72 Cr, 8.86 Ni. The static tensile specimens were 0.505 inch diameter; the impact tensile specimens were 0.20 inch diameter. Although the ductilities of the specimens, strictly speaking, cannot be compared, the ductility in impact tension appears to be better than in static tension at all temperatures from +20 to −80° C.

1937—Zschokke[89]

Arc welds in steel containing 0.08 C, 17 Cr, 9 Ni had tensile strength of 107,000 lb./in.² unmachined, 95,500 lb./in.² with reinforcement machined flush. Unwelded base metal had a tensile strength of 90,000 lb./in.² and the bend angle (no details) was 180° for all specimens.

1936—U. S. Navy[60]

The tensile strength of arc-welded 18–8 must be at least 70,000 lb./in.², and in 180° restricted bend tests on flush machined specimens no defect exceeding ⅛ inch must appear.

1935—Chevenard and Portevin[90]

Micro-mechanical tests of butt welds containing 0.2–0.3 C, 18–19 Cr, 8–9 Ni, 0.15 Ti in a steel 0.39 inch thick containing 0.05 C, 18.8 Cr, 8.9 Ni showed that the shear strength and shear ductility of heat-affected zone was the same as unaffected base metal. The values for the weld metal were up to 30% lower. The specimens were only 0.06 inch diameter.

1935—Zeyen[48]

Automatic and manual metal arc welds in steel 0.12 inch thick containing 0.09 C, 8.8 Ni, 17.3 Cr had yield strength = 47,500 lb./in.², tensile strength = 89,500 lb./in.², elongation = 43 to 45% in 11.3 $\sqrt{A}$, where A = cross section. Fracture occurred next to the weld. There were no cracks after 180° bend.

Table 6—Mechanical Properties of Arc-Welded 18–8, 1 Inch Thick. Hessler and Kautz[91]

Specimen	Yield Strength, Lb./In.² (0.2% Elongation)	Tensile Strength, Lb./In.²	Elongation, % in 1.18 Inches	Reduction of Area, %	VGB Notch Impact Value, Mkg./Cm.²	Cold Bend Elongation, % in 0.79 In.	Cold Bend Elongation, % in 6 In.
Unwelded	47,000	92,000	80	63	43.5	..	..
Perpendicular in weld	45,000	88,000	44.2	34.2	27	54.2	28.7
Parallel to weld	37,700	84,000	55.0	45.3	..	40	22.8

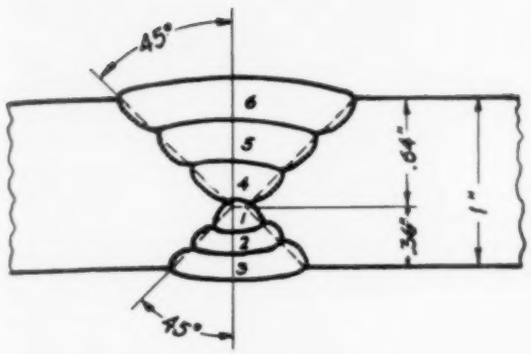


Fig. 2—X Welds Used by Hessler and Kautz[91]

1935 — Johnston[56]

A good metal arc weld in 18–8 has a yield strength of 20,000 to 45,000 lb./in.², tensile strength = 75,000 to 90,000 lb./in².

1934—Hessler and Kautz[91]

Tensile, notch impact and cold bend tests were made on welds in 1-inch plate containing 0.14 C, 0.24 Si, 0.48 Mn, 0.015 P, 0.010 S, 18.1 Cr, 9.0 Ni, Table 6. The welds were made 90° X, Fig. 2, with 18–8, multiple-dip electrodes (composition not stated) 0.20–0.24 inch diameter, 140–160 amp. (0.20 inch), 210–230 amp. (0.24 inch), 28–35 volts at arc. The joints were heated 20 minutes at 1080 to 1130° C. and water quenched to 20° C. There were no X-ray or microstructural defects. The tensile specimens perpendicular to the weld were 1 × 1 inch, machined flush; the reduced section being only 1.18 inches long. The tensile specimens parallel to the weld in weld metal were 0.47 inch diameter and had a modulus of elasticity of 24,000,000 lb./in². The VGB impact specimen was notched in the side of the weld and the cross section of fracture was 0.59 inch square. The bend specimen was 1 inch thick, machined flush, and 1.6 inches wide. The bend angle was 180°. The plunger was 2 or 3 inches diameter, the rollers $2^3/_4$ inches diameter.

The results show that the heat treated welds were not so good as base metal. The specimens across the weld had lower tensile ductility but higher bend ductility than the specimens parallel to the weld.

An arc-welded unstabilized 18–8 tank, 51 inches diameter, 7 ft. long, 0.39–0.47-inch wall, designed for 60 lb./in.² was subjected accidentally to an internal explosion that raised the pressure to 850 lb./in². No welds failed and the diameter of the 0.39 inch section was increased $1^3/_8$ inches. Other examples of arc-welded tanks that survived falls or collapse without cracking are cited by Hessler and Kautz.

1933—Phillips[13]

Arc welds in sheet (67–74 B Rockwell) containing 0.07 C max., 7.0–10.5 Ni, 16.5–20.0 Cr had a hardness of 70 to 71 B Rockwell.

1930—Hansen[286]

Arc welds in 18–8 sheet (12 gage) consistently showed a tensile strength of 95,000 lb./in.² with an elongation of 65% in 2 inches.

1929—Maurath[71]

V butt welds made with coated electrodes in 18–8, 0.14 inch thick (10 gage) had yield strength = 50,500 lb./in.², tensile strength = 101,100 lb./in.², elongation = 38.5–44.5% in 8 inches, fracture occurring in base metal 2 to 3 inches from the weld. Multilayer welds were 179 to 228 Brinell.

Oxyacetylene Welds

1937—Henry[88]

The results in Table 5 show that unheat treated oxyacetylene butt welds in 18–8 $^5/_8$ or $^3/_4$ inch thick had inferior static and impact tensile properties at +20 to −80° C. and that the customary practice of restricting oxyacetylene welding to sheet is well founded.

1936—Priestley[34] and Duncan[40]

Oxyacetylene butt welds in annealed plate $^1/_4$ inch thick containing 0.07 C, 18.22 Cr, 8.91 Ni made with a rod of the same composition had a yield strength = 39,200 lb./in.², tensile strength = 76,500 lb./in.², elongation = 22% in 2 inches. In the free-bend test no cracks occurred at 180°. The welds were not heat treated.

1936—Johnson[47]

The rotating bend endurance limit of unmachined oxyacetylene butt welded 18–8 tubes 1 inch o.d., $^1/_{16}$-inch wall, using 18–8 filler rods is shown in Table 7. Heat treatment after welding ($^1/_2$ hr. at 870° C.) raised the endurance limit nearly 50% without affecting the static tensile strength.

Table 7—Rotating Bend Endurance Limit of Oxyacetylene Butt Welded 18–8 Tubes. Johnson[47]

Heat Treatment	Tensile Strength Welded Tube, Lb./In.²*	Endurance Limit, Unwelded Tube	Lb./In.² Welded Tube	Ratio of Endurance Limits of Welded to Unwelded Tube
As-welded	85,000	24,000	16,000	0.67
$^1/_2$ hr. 870° C.	84,000	24,000	23,000	0.93
$^1/_2$ hr. 1090° C.	81,000	...	16,000	..

* Based on cross section of unwelded tube.

1935—Harvey and coworkers[92]

Their corrosion fatigue tests are discussed in the review of literature on Fatigue Strength of Welded Joints (American Welding Society Journal, **16** (1) Suppl. 21 (1937)). Carbon pick up affected the welds.

1935—Zeyen[48]

The tensile ductility, Table 8, of oxyacetylene butt welds in steel containing 0.09 C, 8.8 Ni, 17.8 Cr, 0.6 Si, 0.4 Mn was better if the weld was made from both sides but the yield strength was lowered. Both welds withstood 180° bend without cracking.

Table 8—Tensile Tests of Oxyacetylene Welded 18–8, 0.12 Inch Thick. Zeyen[48]

Specimen	Yield Strength, Lb./In.²	Tensile Strength, Lb./In.²	Elongation, % in $11.3\sqrt{A}$
Welded from one side	55,500	90,000	42.0
Welded from both sides	45,500	91,000	49.3

A = Cross section.

1933—Phillips[13]

Oxyacetylene welds in sheet (67–74 B Rockwell) containing 0.07 C max., 7.0–10.5 Ni, 16.5–20.0 Cr, had a hardness of 67 to 70 B Rockwell.

1928—W. B. Miller[93]

Oxyacetylene butt welds in 1/8-inch sheet containing 0.12 C, 17.42 Cr, 7.58 Ni, 0.32 Mn, 0.13 Si, Table 9, had lower tensile strength and ductility than base metal and were improved by furnace annealing (no details). Torch annealing was unsatisfactory. It was said that moderately cold worked, light-gage sheet usually yields higher weld strengths than fully annealed sheet. The specimens were 1 1/2 inches wide and were not machined.

Table 9—Effect of Heat Treatment on Oxyacetylene Welded 18–8. Miller[93]

Specimen	Yield Strength, Lb./In.²	Tensile Strength, Lb./In.²	Elongation, % in 2 Inches	Bend Angle, Degrees
Sheet unwelded	53,500	102,100	68	180
As-welded	54,200	66,200	15	150
Torch annealed	50,400	58,000	9	140
Furnace annealed	56,500	74,000	24.5	180

METALLURGY

The main metallurgical problem in the welding of 18–8 has been carbide precipitation in the heat-affected zone, which was discussed fully in the review of literature on Corrosion Resistance of Welded Joints (AMERICAN WELDING SOCIETY JOURNAL, **16** (8) Suppl., 16–25 (1937)) (consult references[49,96,97,98] for general information). The importance of carbides is recognized in a U. S. Navy specification[60] which states that 18–8 arc weld metal upon examination at a magnification of 500 diameters after being etched with Murakami's reagent must show no appreciable amounts of carbides at the grain boundaries. No other problem of importance has appeared.

In metal arc welding Whitmer[1] stated that 18–8 weld metal is more fluid than mild steel weld metal. On the other hand, an Austrian welder[33,94] observed that 18–8 oxyacetylene weld metal has an exceptionally high surface tension. It was thought that flux reduced the surface tension. Nielson's[78] belief that 18–8 weld metal is "gassy," particularly if overheated, is not commonly held. Cracks in 18–8 welds were believed by Leitner[15] to be caused by the hot shortness of 18–8 at about 900° C., for which there is no evidence whatever. Several causes of cracks were listed in the sections on Welding Procedures. Faulty procedure, not the metal, appears to be the origin of cracks, nor can heat treatment such as Plücker[4] suggests be relied upon to prevent cracks. A metallographic study of arc welded 18–8 led Sasaki[61] to the conclusion that no grain refinement can be expected of multiple layers. Coarse grain structure seems to be a characteristic of fusion welded 18–8. Nevertheless, as Hodge[95] pointed out, although all austenitic Cr-Ni welds are dendritic, they are tough.

HEAT TREATMENT

It was shown in the review of literature on Corrosion Resistance of Welded Joints (AMERICAN WELDING SOCIETY JOURNAL, **16** (8) Suppl., 16–25 (1937)) that intergranular corrosion occasioned by carbide precipitation is bound to occur in the heat-affected zone of welded 18–8 in severely corrosive reagents, unless the joint is heat treated or unless the 18–8 contains an alloying element to prevent intergranular corrosion, or unless the carbon content is exceedingly low. The addition of alloying elements is discussed mainly in later sections, but it will become clear that welded alloy 18–8 may require stress relief apart from quenching. According to Jennings,[58] unalloyed 18–8 requires no heat treatment except, perhaps, stress relief, if the carbon content is below 0.07%. On the other hand, the specifications of a leading chemical firm (Sept. 1938) state that if unstabilized 18–8 (0.07 C max., with 17.5 Cr min., 8.0 Ni min., or 20.0 Cr min., 10.0 Ni min.) equipment is intended for any corrosive service, it must be heat treated after welding by quenching in air or water from 1100° C. (2000° F.) to obtain maximum corrosion resistance.

The A. S. M. E. Boiler Code Committee[99] ruled that welded alloy 18–8 (0.07 C max., 0.40–2.50 Mn, at least 17 Cr, at least 9.5 Ni, alloyed with Cb (10 × carbon content, 1% Cb max.) or Ti (6 × carbon content, 0.60 Ti max.)) must be stress relieved, if the unfired pressure vessel is classified under Par. U-68. The 18–8 containing Cb is heated at not less than 840° C. (1550° F.) for 1 hr. per inch of thickness but not less than 2 hr. The 18–8 containing Ti is heated at not less than 840° C. (1550° F.) nor more than 900° C. (1650° F.) for 1 hr. per inch of thickness but not less than 4 hr. The welded structure is cooled slowly in a still atmosphere. Local stress relief is not permitted. The stabilized 18–8 may be used for vessels constructed according to paragraphs U-68 and U-69, the allowable stress being 15,000 lb./in.² (90% joint efficiency for U-68, 80% for U-69; maximum operating temperature = 480° C. (900° F.) for U-68; 320° C. (600° F.) for U-69).

Johnson[47] recommended air cooling after stress relieving 18–8 at 875° C. (1575–1625° F.). After arc welding an 18–8 ring 1/2-inch thick, 48 inches diameter, 1 inch wide, to a mild steel valve disk, Pavlik[282] stress relieved the assemblage 3 hr. at 540° C. (1000° F.). Under most circumstances this heat treatment will decrease the corrosion resistance of 18–8.

According to Navy Department Specifications[100] (1937), annealing (not less than 1070° C. (1950° F.) for 1 hr. per inch of thickest section) of welded 18–8 must not be done unless the entire structure can be water-quenched. Holmberg[101] states that heating welded 18–8 at 1100° C. (2000° F.) followed by rapid cooling always improves the joints. *Machinery*[2] regards annealing as essential only for welded unalloyed 18–8 intended for severe service. The heat treating temperature is 1010 to 1100° C. (1850–2000° F.), heavy sections requiring the higher temperatures and being preheated to 760–820° C. (1400–1500° F.). The 18–8 is held at temperature only long enough to assure uniform temperature distribution, and is cooled rapidly. Rapid cooling is particularly important for 18–8 containing molybdenum, which is heat treated at 1070–1100° C. (1950–2000° F.). For maximum softness and ductility Ti and Cb 18–8 are given the same heat treatment as unalloyed 18–8, although welded Ti and Cb 18–8 require no heat treatment to assure full corrosion resistance.

To prevent intergranular corrosion in welded 18–8 Prever[102] suggested water quenching from 1050–1100° C., whereas Holler[12] believed water quenching from 1170 to 1200° C. was essential for oxyacetylene welded 18–8. A heat treatment for welded 18–8 that was discussed by the A. S. T. M., according to Poste,[103] consisted of "heating the welded vessel at 980° C. (1800° F.) for 1 hr. for each 1/2 inch of thickness followed by rapid cooling in air to relieve stresses and improve the corrosion resistance." The proposal never was put into effect. It is the opinion of Hessler and Kautz[91] that the carbon content of 18–8 is not important for welding but determines whether heat treatment is required after welding. Heat treatment is required for 18–8 with 0.08 to 0.15 C with or without 2.5

Mo. None is required if the carbon content is not over 0.07% or if 0.50 Ti is present with up to 0.12 C, with or without 2.5 Mo. To avoid scaling McEvoy[104] recommends that annealing be done in a non-oxidizing atmosphere.

CORROSION

The review of literature on Corrosion Resistance of Welded Joints summarized information existing in January 1937. Brief references to other corrosion tests are dispersed throughout the present review. Among recent investigators of the corrosion of welded corrosion-resisting Cr and Cr-Ni steels, Leroy[105] describes weld metal (no details) in 18–8 0.12 inch thick containing Ti, which was severely attacked by HNO_3. Both base metal and weld metal were unattacked in laboratory tests in boiling acid $CuSO_4$ reagent and 30% H_2SO_4 (30° C., 96 hr.). However, the weld metal contained only 17.4 Cr, and the 99% HNO_3 causing the attack was found to contain 0.032 to 0.105 milligram Al per liter. The combination of high-purity acid containing aluminum, and low-chromium weld metal was believed to account for the failure. With higher chromium content the weld metal resisted attack. Furthermore 95% HNO_3 with or without Al had no effect on the lower chromium weld metal.

Oxyacetylene welds made by Daubois[106] using a supplementary reducing flame in different grades of 18–8 (0.06–0.14 C, 18 Cr, 9 Ni) 0.06 inch thick were tested in the buffed condition in a 25% salt spray and in a Leclanché cell (specimens negative, 25% NaCl solution). The filler rod was base metal but the welds, which were not analyzed, doubtless picked up carbon from the flame. Whether or not the specimens were bent, the salt spray had no effect. In the cell tests (time not stated but presumably 8 to 10 hr.), there was a band of general corrosion (no pits) about 0.8 inch from the weld, regardless of carbon content of base metal, which represented the zone of carbide precipitation. If a weld and an unwelded specimen that had been bent cold were placed together in the cell, only the welded specimen corroded. If both were water-quenched from 1050° C. before test, the weld metal corroded a little with pit formation. In other words, the corrosion did not occur in the sensitized zone, but in the weld, which had an irregular surface and, doubtless, a higher carbon content than base metal.

The corrosion of spot welded 18–8 has been studied by Ffield.[107] Exhaust pipes for use with doped fuel were spot welded by Ffield in a controlled atmosphere. If the sheet (0.050 inch thick) contained 0.07 C and no other alloying elements failure occurred by intergranular corrosion after 500 hr. at 480° C. (900° F.), despite the fact that the pipe had been air quenched from 1100° C. after welding. Intergranular corrosion occurred in all parts of the pipe and not solely at the welds. If the pipe was made of low carbon 18–8 containing titanium and was specially heat treated before and after welding, failure did not occur. Loiseau[108] observed a slight discoloration at spot welds in 18–8 (no details) after 600 hr. in salt spray.

Spot welds in 18–8, 0.02 inch thick (no details) were tested by Zehbe[109] in a solution containing 1% lactic acid, 0.25% NaCl. Regardless of welding time, which was best at 1.7 seconds, and despite polishing the weld, iron and nickel were detected in the solution after 3 to 5 days. The unwelded heat treated sheet was completely resistant. Spot welds quenched from a salt bath ($BaCl_2$) at 1140–1150° C. into brine at 55° also were completely resistant to the lactic acid solution after 20 days. Reheating to 200° C. had no effect. The heat treated welds were fine grained, whereas the unheat treated welds were coarse grained. No explanation was offered, although some cold work may have been done under the tips.

Krivobok[110] stated that shrinkage stresses in welded 18–8 set up stress corrosion (no details). According to *Sheet Metal Worker*,[111] bolted construction of sheet metal in Chicago stockyards has been prohibited by the government. Presumably the superior corrosion resistance of welded stainless steel chutes and other equipment, as well as the absence of crannies for lodging of food, accounts for the regulation.

RESISTANCE WELDING

Spot Welding

The principles of spot welding 18–8 are based on the low thermal and electric conductivity, as Whitmer[1,112] points out. Since less power is required than for black steel, either the switch is dropped a point or the time is reduced slightly in changing from mild steel to 18–8 on a given spot welder. An oscillographic study convinced an English writer[113] that more pressure and less current are required to spot weld 18–8 than Dural of the same gage. To avoid depressions[1] created by the heavy pressure combined with the low yield strength of 18–8, a copper block often is inserted between the electrode and the polished side of the sheet. Aluminum blocks are not so satisfactory on account of arcing. The copper block also is said to prevent stain from the electrode on the polished surface.[114] According to Sciaky,[115] the stain is an overheated, yellow tint which is obtained with tungsten-copper electrodes, on account of their high resistivity. Unlike mild steel, 18–8 may be spot welded through a large number of thicknesses, Ragsdale[116] found.

For reasons of good corrosion resistance, which were discussed in the review of literature on Corrosion Resistance of Welded Joints, the fused and severely heat-affected zone should amount to not more than 50 to 80% of the thickness, according to Larsen,[117] who states that the pressure should be 50 to 100% greater than for mild steel. Hess and Ringer[297] obtained best results with 50,000 lb./in.^{2} pressure in spot welding polished 18–8, 0.028 inch thick. The high pressure is required to secure good point contact and to insure rapid conduction of heat from the weld. Shallow welds are brittle. The penetration of the fused portion was 40 to 70% in the best welds made by Hess and Ringer.[297] Three examples of successful machine settings for spot welding 18–8 are given in Table 10. A hard copper-base alloy was used for the tip.

Using a 100 kva. press type welder (ignitron control, throat depth = 12 inches, 60 cycles, 440 volts on primary, copper-chromium alloy tip 1/4 inch diameter), Thomason[118] secured good welds in two sheets of 18–8, each sheet being 0.032 inch thick, with 650-lb. pressure, 1 cycle, and 7% machine capacity. He used 2 cycles and 550-lb. pressure to weld 18–8 to galvanized iron (26 gage),

Table 10—Successful Machine Settings for Spot Welding Two Sheets of 18–8. Spraragen[119]

	Example 1	Example 3	Example 3
Thickness of each sheet, inch	0.010	0.030	0.050
Tip diameter, inch	3/16	5/8	5/8
Area of weld, sq. in.	0.00165	0.0193	0.0145
Electrode pressure, lb.	75	500	500
Welding time, sec.	0.0083	0.0583	0.0833
Welding current, amp.	1472	4415	4270
Current density in welded area, amp./sq. in.	894,000	308,700	298,200

sheet steel (0.040 inch thick) and Olympic bronze (0.050 inch thick), the power being 21, 13 and 18% of machine capacity, respectively. To weld 18–8 to aluminum Ferguson[77] recommended high-dome Elkaloy (copper-rich alloy) tips, which required 2.55 volts. If a tungsten tip was used against the aluminum the voltage was 1.45.

The relationship between power and thickness, according to Sciaky,[115] Fig. 3, shows that A.C. demands more power than D.C. Spot diameters and minimum spacings are given in Table 11. Hensel's[120] extensive tests on the

Table 11—Spot Diameters and Minimum Spacings for Spot Welding Two Sheets of 18–8. Sciaky[115]

Thickness of Each Sheet, Inch	Diameter (Inch) of Spot on Surface Where Electrode Made Contact	Diameter of Spot at Weld, Inch	Minimum Spacing
0.012	0.12	0.10	1 diameter
0.039	0.20	0.18	1 diameter
0.079	0.26	0.24	0.63 inch

effect of pressure, timing and power on the area of the weld are summarized in Fig. 4, which applies to two sheets of 18–8, each 0.040 inch thick, using a conical, hard copper-base alloy electrode. Figure 4 A shows that timing is critical at 25,000 lb./in.^{2} and 16 kva. Almost no weld is secured with 1 cycle, a good weld is secured with 4 cycles, yet with 4 cycles the weld is unsatisfactory on account of the deep depression (Brinelling) created by the electrode. Best welding conditions prevail between 2 and 3 cycles; additional cycles cause Brinelling. At 2 cycles and 25,000 lb./in.^{2}, Fig. 4 B, shows that there is a

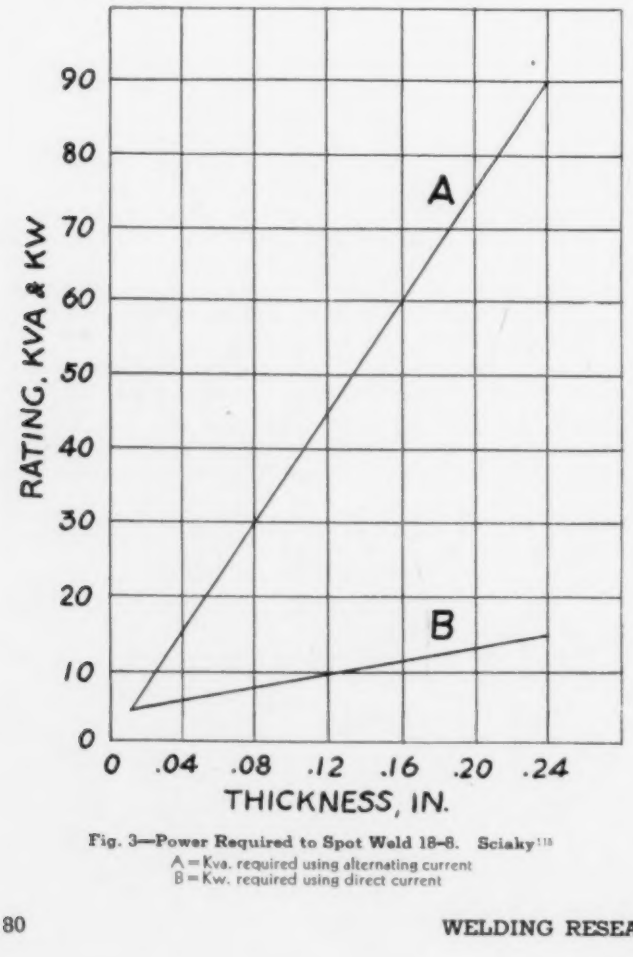


Fig. 3—Power Required to Spot Weld 18–8. Sciaky[115]
A = Kva. required using alternating current
B = Kw. required using direct current

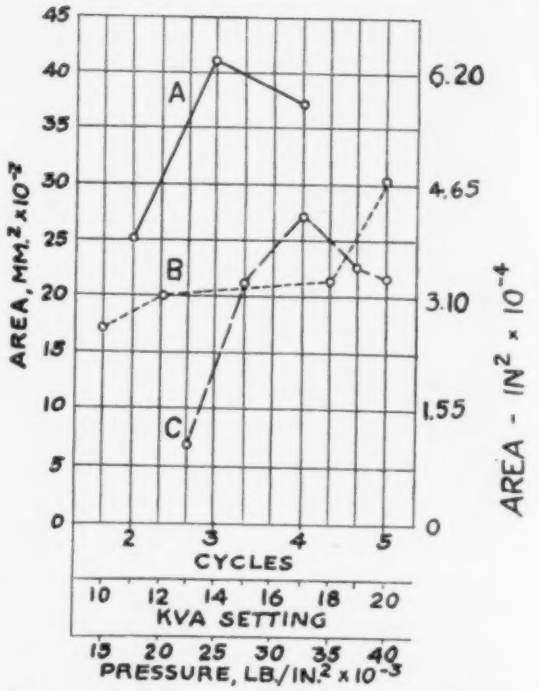


Fig. 4—Effect of Spot Welding Variables on the Area of the Weld in 18–8. Hensel[120]
A = Effect of timing
B = Effect of power
C = Effect of pressure

steep increase in size of weld between 13 and 16 kva. At higher power Brinelling decreases the size of the weld. The area of the weld increases rapidly, Fig. 4 C, as the pressure is increased, especially above 30,000 lb./in.^{2}, the other variables being held constant at 2 cycles and 16 kva. The well-known, columnar or dendritic macrostructure of spot welds in 18–8 is illustrated by Hensel.

Although Hensel and Larsen recommend hard copper-base alloy tips, Sciaky[115] believes hard electrolytic copper tips yield the best results. The number of welds that an electrode can make in two sheets of 18–8 each 0.028 inch thick is some inverse function of the pressure, Zimmerman[121] found. Unfortunately, he did not state the composition of the tip. Ffield[107] cites exhaust pipes made of 18–8, 0.050 inch thick, containing 0.07 C which were spot welded in a controlled atmosphere (no details) and air quenched from 1100° C., yet failed through intergranular corrosion throughout the pipe after service with doped fuel for 500 hr. at 480° C. (900° F.). Nielson[78] found that local annealing was not satisfactory for resistance welded 18–8. An automatic spot welder of British manufacture especially designed for 18–8 was described by *Aircraft Engineering*.[122] Shop equipment, pincers and spot-welded 18–8 products are illustrated by Loiseau.[108] Spot-welded 18–8 sections made of 18–8 sheet 0.012–0.032 inch thick used in building a stainless steel motor boat are described by a Russian writer.[290]

Developed particularly for 18–8, the shot welding process[123] is nothing but spot welding with unusually precise control of timing, by means of ignitrons. The essential feature of the process is the short welding interval. Metal more than one sheet thickness from the fused zone never reaches the temperature for carbide precipitation. Although, according to Ragsdale,[116] no close relation exists between time of dwell and total thickness to be welded, it is exceptional for a weld to require more than half a cycle of 60 cycle A.C. The heat needed for a single weld in two sheets of 18–8 each 0.012 inch thick is only 0.035 B. t. u. Indirect welding (elec-

trodes not in line) is likely to be uncertain. Machine settings for spot welding, Table 10, apply as well to shot welding. Machines weld up to 100 spots a minute.

Spot welding is widely used for 18–8 wire and sheet in dental work. Steininger[124] uses copper electrodes with 2–5 volts, 100–150 amp. secondary. Worn or corroded electrodes produce poor results, according to a dental journal,[125] which describes a turret head spot welder and several applications. To weld a thin wire to a thicker wire it is suggested that the electrode for the thin wire had a large curved surface to prevent fusion or excessive deformation of the thinner wire. Charlier[126] shows that irregularities, holes and tears in heavily cold worked parts can be smoothed out by using a spot welder. Indirect spot welding (both electrodes on the same side of the sheets) is also feasible provided the inactive electrode has a large surface area so that fusion does not occur beneath it.

Properties of Spot Welds.—The important property for the spot welding designer is the shear strength, which is the load required to shear a single spot between two sheets in tension. Between 0.02 and 0.075 inch direct proportionality between the shear strength and the thickness of each sheet was found by Loiseau,[108] Table 12,

Table 12—Shear Strength of Spot-Welded 18–8. Loiseau[108] and Verticchio[270]

Thickness of Each of Two Sheets, Inch		Shear Strength, Lb.	
0.0216	Loiseau	825	
0.295		1170	
0.039		1850	
0.059		3740	
0.075		4840	
Thickness of Each Sheet, Inch		**Load at Fracture, Lb.**	
0.04 to 0.04	Verticchio	1630	(Spot 1/2 inch diameter, using electrode 0.24 inch diameter and pressure of 1100 lb.)
1/8 to 1/8		7700	(Spot 0.27 inch diameter, using electrode 0.63 inch diameter and pressure of 2540 lb.)
0.079 to 0.106		5100	
0.106 to 0.106		6050	
0.079 to 0.106 to 0.079		12,900	
Three thicknesses 0.079		10,500	
Three thicknesses 0.106		13,000	
Four thicknesses 0.079		15,400	

who averaged 200 tests of each gage. A 50 kva. spot welder with electromagnetic control was used. The diameter of the electrode was 4 times sheet thickness for 0.04 to 0.06 inch sheet, but larger than 4 times for thinner sheet. Practically the same strengths were obtained by Salelles,[127] who quoted 750 lb. for two sheets each 0.012 inch thick. The spread of values never exceeded 8.5%. Sciaky[117] found only 550 lb. for two sheets of 18–8 each 0.013 inch thick and only 3500 lb. for two sheets each 0.079 inch thick.

Using a 100 kw. Budd-Piaggio spot welder (capacity was 15 thickness of 0.039 inch sheet, or 3 thicknesses of 1/8 inch sheet, 1/10 to 1/50 second), Verticchio,[270] Table 12, found that the fracture load in single shear of spot welds in cold rolled 18–8 containing 0.10–0.15 C (tensile strength = 150,000 lb./in.^{2}) was directly proportional to the area in shear. The shear strength was 130,000 to 135,000 lb./in.^{2} and the spots could be twisted 90° without failure. The stainless steel cars fabricated by the Budd-Piaggio process are described in a railway publication.[272] In Dussourd's[128] tests of spot welds in 18–8 (0.19 C, 0.36 Si, 0.55 Mn, 8.84 Ni, 17.14 Cr, 0.10 and 0.20 inch thick, tensile strength = 93,000 lb./in.^{2}) the electrodes were 0.39 inch diameter, but the time of welding was too brief to be measured. The breaking load in shear divided by the cross section of the spot measured after shear was 55,500 to 59,700 lb./in.^{2} for single spots in either thickness. Although the fusion zone did not extend to the surface, there were large blow-holes or shrinkage cavities in the middle of the welds. Base metal was 145 Brinell, heat-affected zone was 155 Brinell, and the weld was 165 Brinell.

Hibert's[129] tests on shot welded cold rolled 18–8 0.026 inch thick, containing 0.08 C, 16.8 Cr, 9.0 Ni, tensile strength = 161,000 lb./in.^{2}, Table 13, showed that

Table 13—The Effect of Current-Time Changes on the Strength of Spot Welded 18–8 After 48 Hr. Boiling Acid Copper Sulfate Test. Hibert[129]

Time of Current Dwell, Sec.	Current Setting*	Shearing Strength of a Single Spot, Lb.
0.050	*K*	650
0.066	*J*	644
0.083	*H*	641
0.100	*G*	650
0.116	*F*	665
0.133	*E*	680

* *E* = Medium current, *F* to *J* = Intermediate currents, *K* = High current (no details).

changes in current and time had little effect on shearing strength and corrosion providing the fused zone did not reach the surface and the area of the weld was adequate. None of the specimens developed intergranular corrosion (no carbides at 1500 diameters) and there was no visible evidence of a corrosion ring. Coloration around the depression was no evidence of carbide precipitation. Hibert regarded 0.10 sec. as a practical limit below which carbide precipitation need not be feared.

The complexity introduced by testing specimens with multiple spots is illustrated by Hibert's Table 14. The results are not extensive enough for the formulation of a relationship between strength and arrangement of spots. The corrosion test had no effect on strength. After 100 hr. in boiling acid copper sulfate solution, however, the three specimens with 0.11–0.15 C failed in the welds on being bent. The failures were attributed to the high-tensile strength and carbon content of the drastically cold-rolled sheets. Hibert found that a spot of greater diameter than four times sheet thickness pulls a slug clear of the sheet, whereas smaller spots shear between the sheets. All welds with normal strength had splashes between the sheets and had pipe or gas holes located in the plane between the two sheets but not necessarily in the center of the spot. Without these defects the welds had low strength and could not be rotated through 60°.

As the welding pressure was increased from 250 to 800 lb. in Zimmerman's[121] tests on spot-welding 18–8, 0.028 inch thick, the shear strength increased in direct proportion from 400 to 800 lb. However, the unit shear stress remained constant at about 90,000 lb./in.^{2} which is surprisingly high. Unfortunately, Zimmerman did not make clear whether he used the cross section of the sheared spot in his stress calculations.

In designing spot welds in 18–8 Eksergian[130] relied upon 32,000 T lb. static shear load for each spot in sheets the minimum thickness of which is T inches. He reported

Table 14—Shear Tests of Multiple Spot Welds. Hibert[129]

Unwelded Sheet					Number of Rows of Spots	Total Number of Spots	Machine Setting			Total Load, Lb. Before Corrosion† Test	Total Load, Lb. After Corrosion† Test	Type and Location of Failure
C	Cr	Ni	Tensile Strength, Lb./In.²	Thickness, Inch			Time, Sec.	Current*	Pressure, Lb.			
0.11	17.5	8.78	198,600	0.005	2	9	0.050	C	75	600	560	Spots pulled out
0.15	17.1	7.97	188,500	0.010	2	7	0.066	H	150	1800	1920	Spots pulled out
0.11	18.3	9.96	192,300	0.015	3	6	0.083	G	150	2350	2380	Base metal
0.08	18.1	9.20	119,500	0.020	3	6	0.116	F	125	2570	2550	Spots pulled, some sheared
0.08	16.8	9.02	162,300	0.026	2	7	0.133	I	175	4290	4370	Spots sheared
0.08	18.4	9.20	166,000	0.040	4	10	0.116	I	200	..	6880	Spots sheared

* C = low Current, F, G, H = Medium currents, I = High current.

† Corrosion test consisted of a cycle of dipping in 20% NaCl solution for 1 minute and drying in air for 15 minutes, continued for 720 hr. There was a little staining between the sheets at the conclusion of the test.

tests on spot-welded 18–8 corrugated roof sections, but had not yet developed formulas. For design purposes an 18–8 rivet, according to Ragsdale,[116] has only one half the resistance to shear of a spot weld of the same diameter. Ragsdale referred to the poor fatigue strength of the heat-affected metal but quoted no tests. For spot welds in two sheets each 0.015 or 1/8 inch thick, the diameter of the sheared weld should be 1/16 or 3/8 inch, respectively.

Projection Welding

The same principles apply to the projection welding of 18–8 as to spot welding according to Whitmer.[1] To projection weld 18–8 studs 5/16 inch diameter with 2-inch radius dome to 18–8 sheet, Gillette[131] used 8 cycles on a 50 kva. machine. The pressure was 1000 lb., but information on power was not available.

Seam Welding

Seam welding being a method of making overlapping spot welds, the metallurgical principles are the same as in spot welding, as Whitmer[1] intimates in his brief general survey. A description is given by Gillette[131] of the seam welding of vacuum tight and other parts of an 18–8 refrigerator evaporator. The 18–8 contained 0.12 C max., 17–19 Cr, 7–9 Ni and was 0.025 inch thick. The vacuum tight weld was made on a manually fed machine at the rate of 72 inches/min. with the interrupter operating 2 cycles "on," 2 cycles "off." The 60-cycle current was 6300 amp., pressure 600 lb. On intermittent seam welds the interrupter operated at 4 cycles "on," 4 cycles "off;" the current was 3700 amp., 1275 lb. pressure, 105 inches per minutes. Thum[49] mentioned a seam welder for 18–8 producing 600 welds per minute.

Flash Welding

The flash welding of 18–8 is simpler than of stainless iron[132] and is more satisfactory than resistance butt welding.[6,8] The process, according to Whitmer[1] and Taylor[5] is the same for 18–8 as for mild steel but, as in spot welding, the power or time should be a little lower for 18–8. Clark[133] particularly stressed the importance of short welding times to suppress carbide precipitation. Thum[49] quoted 0.38 second for a flash weld in 18–8, 1/2 inch diameter. If the welding time is prolonged, Taylor[5]

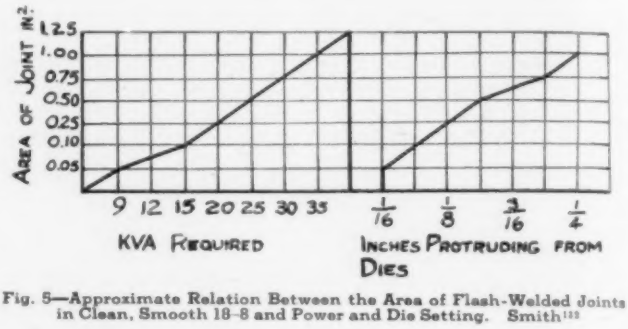


Fig. 5—Approximate Relation Between the Area of Flash-Welded Joints in Clean, Smooth 18–8 and Power and Die Setting. Smith[132]

found that fluid metal will flow away from the welded area and leave holes in the joint. Hall[8] and Hatfield[6] emphasized close temperature control.

Approximate flash welding machine settings have been suggested by W. E. Smith,[132] Fig. 5. The pressure was not stated. The dies were hammered copper or hard copper alloy. The tolerated line drop was only 5% (10% for mild steel). Less metal extends beyond the dies than in flash welding mild steel. At the start of welding the projection is 1/2 inch; at the completion of welding the dies are not over 1/16 inch apart.

The tensile properties of flash-welded 18–8, Table 15, are close to annealed base metal. Smith[132] also found that flash-welded 18–8 flats 0.06 × 0.092 inch, ground flush had an elastic limit of 42,000 to 48,000 lb./in.², tensile strength = 90,000 to 97,000 lb./in.², elongation =

Table 15—Tensile Strength of Flash-Welded 18–8. Smith[132]

Primary Current, Amp.	Power Kva.	Elastic Limit, Lb./In.²	Tensile Strength, Lb./In.²	Elongation, % in 2 Inches
100	22	35,350	94,600	58.5
110	24.2	36,000	97,200	58.5
120	26.4	35,850	95,600	63.5

The bars were 1/2 inch diameter and were finish turned after welding. Fractures occurred 1/2 + 5/8 inch from the weld. The welds were made with 23 cycles of 60 cycles a.c., 6.0 volts on secondary, 220 volts primary.

Table 16—Static and Impact Tensile Tests of Flash-Welded 18–8. Galibourg and Ballay[134]

Heat Treatment	Brinell Hardness	Static Tensile Test: Yield Strength, Lb./In.²	Static Tensile Test: Tensile Strength, Lb./In.²	Static Tensile Test: Elongation, % in 4 3/4 In.	Static Tensile Test: Reduction of Area, %	Impact Tensile Test: Fracture Energy, Ft.-Lb.	Impact Tensile Test: Elongation, % in 1.3 In.
As-welded	247	53,800	110,000	29.9	55	116	33.5
Water-quenched from 1100° C.	188	50,500	100,000	51.8	63	123	39

The bars were 0.63 inch diameter, 30 kva. machine, 6.25 volts secondary. The static tensile specimen was 0.39 inch diameter; the impact tensile specimen was 0.18 inch diameter.

Table 17—Static and Impact Tests of Resistance Butt-Welded 18–8. Galibourg and Ballay[134]

Specimen	Static Tensile Test: Yield Strength, Lb./In.²	Static Tensile Test: Tensile Strength, Lb./In.²	Static Tensile Test: Elongation, % in 4¾ In.	Static Tensile Test: Reduction of Area, %	Impact Tensile Test: Fracture Energy, Ft.-Lb.	Impact Tensile Test: Elongation, % in 1.3 In.
As-welded	58,000	88,000	9	10	89	26
Water-quenched from 1100° C.	50,800 to 52,000	83,000 to 94,000	17 to 29	18 to 28	28 to 80	10 to 24

The bars were 0.63 inch diameter, 30 kva. machine, 3.5 volts on secondary, 17.6 kva. used. Dimensions of specimens are given in Table 16.

50 to 51% in 2 inches. The welds were made with 15 cycles of 60 cycle a.c., 12.1 kva., 55 amp., 220 volts primary, 4.8 volts secondary.

Static and impact tensile tests on flash-welded 18–8 (0.20 C, 20 Cr, 8 Ni) made by Galibourg and Ballay,[134] Table 16, showed that the welds had high impact and static tensile properties. Heat treatment had a favorable effect on ductility. Adelson's[53] flash-welded 18–8 tubing was 95 B Rockwell as welded, 75 B Rockwell after heat treatment. The tensile strength and elongation before and after heat treatment were 100,000 lb./in.², 35% in 2 inches, and 90,000 lb./in.² and 65% in 2 inches, respectively. No details of welding and testing were provided.

Resistance Butt Welding

Resistance butt welding is not generally recommended for 18–8. Good contact must be made across the entire cross section and gentle pressure must be applied as long as the current is flowing, according to Phillips.[13] Static and impact tensile tests were made by Galibourg and Ballay,[134] Table 17, on resistance butt-welded 18–8 (0.20 C, 20 Cr, 8 Ni). The low results with resistance butt welding probably were due to defects in the weld. Strange but unexplained microstructures were observed, twinning being absent in the heat-affected zone. Zeyen[48] reported a yield strength of 57,000 lb./in.², tensile strength = 99,000 lb./in.², and elongation of 30.7% in 11.3 $\sqrt{A}$ (A = area of cross section in sq. mm.) for a resistance butt weld in 18–8 (0.09 C, 8.8 Ni, 17.8 Cr) 0.04 inch thick. Perhaps the sheet was flash welded, instead of resistance butt welded. Resistance butt-welded 18–8 tubing (longitudinal seam, roller electrodes) had a yield strength of 45,000 lb./in.² tensile strength = 90,000 lb./in.², elongation = 35% in 2 inches, 90 B Rockwell, in tests described by *Product Engineering*.[135]

18–8 WELDING RODS

Since 18–8 welding rods are used for other purposes than welding 18–8 base metal, the properties of the filler metal and its applications are summarized in the following sections.

18–8 Electrodes

Coated electrodes are always used. The mechanical properties that may be expected of 18–8 all-weld-metal, Table 18, are lower than unwelded 18–8. The heat treatment applied by Bibber lowered the elongation below that specified by the Navy. The low modulus of elasticity of 18–8 weld metal is due probably to its strained condition and dendritic structure, according to Hodge.[137] There was no difference in properties in weld metals deposited in the flat, overhead and vertical positions in Bibber's tests. For some reason Bibber stated that the weld metal in a welded joint in 18–8 yields first, yet the results in Table 18 suggest the opposite.

The hardness and micro-shear properties of downhand, 60° V butt welds (0.04 inch spacing, 4 layers) in the steels 0.47 inch thick shown in Table 19 made with covered electrodes by a commercial firm were determined by Waché.[280] Only the first and last steel were used with the 18–8 electrodes, but all three steels were employed in tests of higher alloyed electrodes described in later sections. The hardness surveys were made with a Pomey sclerometer. The shear tests were made by means of a Chevenard micro-machine using specimens 0.06 inch diameter.

Table 19—Steels and Electrodes Used by Waché[280]

Tensile Strength, Lb./In.²	Plates: C	Si	Mn	S	P	Ni	Cr
57,000	0.07	trace	0.33	0.027	0.013	0.46	0
71,000	0.25	0.13	0.59	0.025	0.015	0.10	0
85,000	0.20	0.24	0.50	0.019	0.018	1.92	1.31

	Electrodes: C	Si	Mn	Ni	Cr	Mo
E	0.05	0.40	0.54	9.80	18.54	0
F	0.14	1.46	0.73	7.02	22.48	0
G	0.03	0.35	0.55	9.74	18.40	2.35
H	0.03	0.69	0.64	7.24	22.78	2.03

The hardness results, converted to Brinell units, Table 20, show that the heat-affected zone is not nearly so hard

Table 18—Properties of All-Weld-Metal from 18–8 Electrodes

Reference	Proportional Limit, Lb./In.²	Yield Strength, Lb./In.²	Tensile Strength, Lb./In.²	Elongation	Reduction of Area, %	Young's Modulus, Lb./In.²	Notch Impact Value	Specimen
Zeyen[136]—1937	....	32,700	81,500	31.0**	33	.....	13.1†	All-weld-metal
U. S. Navy[60]—1936	....	....	70,000	20‡	..	.....	...	All-weld-metal
Bibber[59]—1935	18,500	41,800	85,300	58.8‡	70.1	30,567,000	...	Unwelded 18–8
Bibber[59]—1935	23,200	53,800	77,900	23.1‡	30.3	26,460,000	...	All-weld-metal*
Bibber[59]—1935	19,200	49,800	70,900	18.3‡	23.8	25,140,000	...	Ditto heated 30 min. 980° C., air cooled

* Average of 5 tests with different electrodes; see Table 4.
† Mkg./cm.², DVMR notch impact specimen.
‡ % in 2 inches.
** % in 5 diameters.

Table 20—Brinell Hardness of Welded Joints. Waché[230]

Electrode	Tensile Strength of Plate, Lb./In.²	Deposited Metal Minimum	Deposited Metal Maximum	Heat-Affected Zone	Base Metal
E	57,000	150	374	114	100
	85,000	150	419	238	165
F	57,000	205	426	114	100
	85,000	203	391	209	165
G	85,000	151	366	189	140
H	85,000	180	370	183	145

as the deposited metal. The microstructure of the first layer deposited by electrode *H* in the 2 Ni, 1 Cr steel was martensitic, whereas the remaining layers were a mixture of alpha and gamma phases without martensite. Consequently the maximum hardness of the weld metal was found in the first layer, the composition and Charpy value of which are given in Table 21. The dilution of

Table 21—Composition and Charpy Value of Weld Metal Deposited by Electrodes‡ *G* and *H* in 2% Ni, 1% Cr Steel. Waché[230]

	C	Si	Mn	Ni	Cr	Mo	Charpy Notch Impact Value, Mkg./-Cm.²
Electrode *G*							
top part	0.08	0.44	1.20	8.74	15.78	2.10	9.0*
Root part	0.12	0.33	0.94	6.92	11.32	1.44	7.0†
Electrode *H*							
top part	0.08	0.75	1.37	6.48	19.48	1.88	6.2*
Root part	0.15	0.51	1.16	5.22	13.64	1.46	5.5†

* Specimen from top with notch toward root.
† Specimen from root with notch toward top.
‡ See Table 19.

weld metal by base metal was serious only in the first layer, and depended on the operator's skill, and other welding conditions. The hardness was accompanied by low ductility in the microshear tests, the results of which agreed with the hardness tests. The austenite-ferrite structure obtained with the 18–8 electrodes alloyed with molybdenum produced particularly good microshear strength and ductility. However, none of the 18–8 electrodes was considered advisable for use in the first layer of a weld in low-alloy steel. The first layer should preferably be deposited by an electrode with 12–15 Ni and 22–25 Cr. The maximum hardness in the heat-affected zone was found adjacent to the last bead.

The sensitivity to cracking of weld metal from austenitic electrodes may be measured with a specimen, Fig. 6, developed by Krupp's and described by Stieler.[135] After a thin root layer is deposited, a thick layer of another electrode is deposited on the reverse side. The shrinkage of the second layer may crack the first layer.

The chief applications of the 18–8 electrode, aside from

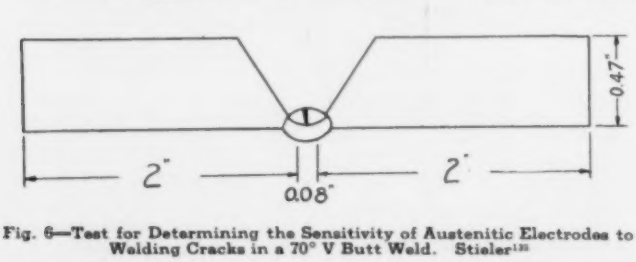


Fig. 6—Test for Determining the Sensitivity of Austenitic Electrodes to Welding Cracks in a 70° V Butt Weld. Stieler[135]

welding 18–8, are in welding plain carbon and low-alloy steels, as well as alloy steels that are otherwise difficult to weld. Many references to the use of 18–8 or other chromium-nickel austenitic steel electrodes for welding alloy steels have been made in other reviews. In plain carbon steels with 0.6 to 0.8 C, as Jennings[139] states, the 18–8 electrode makes good welds but the fusion zone is hard and brittle. To overcome the hardened zone in welding high strength steels with 18–8 electrodes, Portevin[140] recommended that the scarves be coated with a thick layer of 18–8 and annealed prior to welding with 18–8 electrodes. Apparently annealing did not adversely affect the initial 18–8 deposit. The heat of welding is absorbed by the 18–8 deposit and does not raise the heat-affected zone of the annealed high-strength steel above the critical point.

Dorey[141] objected to the omission of heat treatment for 18–8 arc welds in mild steel boilers, because a martensitic zonemust always be present and shrinkage stresses are unavoidable. The study of the microstructure made by Rapatz and Hummitzsch[142] of a weld in unalloyed steel (0.14 C) made with an electrode containing 0.09 C, 9.71 Ni, 17.3 Cr revealed a band of martensite about 0.001 inch wide in the fusion zone at the root of the weld. There was also some intergranular penetration of 18–8 into the mild steel. The fusion zone at the root had a different structure from the face of the weld, which was not explained. It was thought that chromium tended to favor a wide martensitic zone compared with the zone observed in 28% nickel arc welds in mild steel. There was no reason to suppose that annealing, following Dorey's suggestion, would prevent the formation of a martensitic zone. On the contrary, the diffusion occurring in heat treatment tends to widen the zone.

Boiler and pipe steels (Cr-Ni-Mo, Ni or plain carbon) are conveniently welded with 18–8 electrodes, according to Schöne,[143] but base metal is best preheated to 80° C. Higher preheating temperatures generally are used, according to G. W. Plinke (private communication, November 1938). Johnson[144] recommended the 18–8 electrode for 4–6 Cr steel, and Czyrski[145] for heat resisting (chromium?) steel in general. The 18–8 electrode is good for avoiding cracks in welding 12–14 Cr iron,[61,78,110,146] but there is difference of opinion about its use for 18 Cr iron. Jones[7] favors the 18–8 electrode for 15–18 Cr iron, but Whitmer[1] believes it is unsuitable, except for patching. Poor fusion was said to be obtained in welding, whereas for patching the 18–8 electrode was necessary to avoid cracks. Austenitic welds in stainless iron containing 18 Cr, 2 Si fractured outside the weld with good ductility in Schottky's[151] tests. A Russian investigator[147] was successful in welding 25 Cr iron and Silchrome with 18–8 electrodes.

18–8 Oxyacetylene Rods

An oxyacetylene all-weld-metal made with an 18–8 rod, according to Zeyen,[136] had a yield strength = 37,000 lb./in.², tensile strength = 79,500 lb./in.², elongation = 27.2% in 5 diameters, reduction of area = 31%, DVMR notch impact value = 10.4 mkg./cm². The deposit from 18–8 electrodes had slightly better properties. There was practically no hardening in the heat-affected zone of an oxyacetylene butt weld in mild steel (0.05 C, 0.31 Mn) made by Portevin and Séférian.[148] Prolonged application of the flame produced a slightly overheated structure in the mild steel adjacent to the root of the weld. Boutté[22] suggested the 18–8 rod for oxyacetylene welding stainless irons (16–30 Cr, 0.10–0.30 C), and Dawson[149] recommended its use for oxyacetylene welding low-alloy steel piping less than $^1/_4$ inch thick.

Surfacing with 18–8 Rods

Since 1927 when Hoffmann[150] mentioned the application of corrosion resisting surface layers of 18–8 to mild steel by oxyacetylene or metal arc process, the use of 18–8 filler rods for surfacing has steadily expanded. The chief application has been to turbine runners. Stabley[152] successfully applied layers of 18–8 from an electrode coated with a mixture of $CaCO_3$, $MgCO_3$, and sodium silicate to pitted areas of cast-iron hydraulic turbine runners containing 3.00 C, 1.5 Si, 0.75 Mn. The method consisted of chipping out the eroded metal, studding, applying a priming coat of low carbon steel (0.12 C, 0.45 Mn, 0.05 Si), building up with two beads of 18 Cr iron (0.10 C, 18 Cr, 0.40 Si, 0.45 Mn) and finishing with a layer of 18–8 (0.07 C, 0.45 Mn, 0.40 Si, 18 Cr, 8 Ni) $^3/_{16}$ to $^1/_4$ inch thick. If too much heat was applied the entire deposit peeled off, carrying with it a slice of cast iron $^1/_{64}$ to $^1/_{32}$ inch thick. In view of the difference in coefficient of expansion between the ferritic materials and 18–8, peening was essential. Overhead welding was easiest if the 18–8 contained 0.035–0.07 C. With higher carbon content the 18–8 was more fluid. After 2 years of service no pitting of the 18–8 layer was detected.

A similar procedure was adopted by Hess,[153] who omitted the 18 Cr iron layers. Deposited 18–8 was rather soft: 240 Brinell, which was believed to account for the cavitation of the 18–8 surface after two years of service. Eroded turbine runners have also been sprayed with 18–8. According to Torkildsen,[154] an 18–8 electrode was used to resurface the cracked surface of a mild steel (0.06 C) hydraulic turbine runner, of the Francis type. The blades were 0.79 inch thick. Since the first layer appeared to have a high-carbon content for some reason, it was coated with a second. To offset dilution of the surface layer in resurfacing turbine runners, Bibber[59] believed that the electrode should have a higher alloy content than the usual 18–8 electrode. In addition to using the 18–8 rod to surface cast steel turbine runners, Seewer[155] welded inserts of 18–8 over large eroded areas with an 18–8 rod. Several brief accounts of the use of 18–8 electrodes for resurfacing turbine runners are given by Reid.[156]

Besides turbine runners, mild steel shafts have been surfaced with 18–8 electrodes as Hoz[157] describes. The shafts were intended for operation in highly corrosive solutions. Monroe[274] describes gate valve seats for high-pressure, high-temperature steam upon which a layer of 18–8 electrode metal $^1/_8$ inch thick was deposited and machined smooth. A cast-iron roll surfaced with 18–8 to endow it with a rustless surface was briefly described by Johnson.[158]

WELDING 18–8 TO PLAIN STEEL

Resistance Welding

Successful resistance welding of 18–8 to plain carbon steel is entirely feasible, as Hodge[159] explains, but there is bound to be a hard, martensitic zone. Nevertheless, the joints almost invariably break under load in the plain carbon steel at a section removed from the weld.

Machine settings suggested by Thomason[118] for spot welding 18–8 to sheet steel and galvanized iron were summarized in the section on Welding Procedures for 18–8. Instead of 1 cycle and 650-lb. pressure used with two 18–8 sheets each 0.032 inch thick, 2 cycles, 550 lb.; and higher power are needed when one of the sheets is low carbon steel. Boutté[19] found it necessary to insert a piece of mild steel between tip and mild steel sheet to compensate for the greater resistance of the 18–8 sheet. The use of spot welding in attaching a lining of 18–8 to a mild steel tank is described by Hopkins.[160] Goldmann[161] showed that a spot weld of 18–8 to mild steel is likely to be hard and to crack. Extremely interesting diffusion phenomena were observed by Hensel[120] in his metallographic study of a spot weld between 18–8 and cold rolled, low carbon steel, but he supplied no details whatever.

In resistance butt welding mild steel to 18–8 the mild steel must project[13,19] farther from the grips than 18–8 to compensate for the higher specific resistance of 18–8. An account of a service failure of austenitic Cr-Ni steel superheater tubes containing 0.5 W resistance welded to mild steel tubing[162] is discussed in the section on Tungsten in 18–8. Ullmann[287] describes a combined rail joint fish plate and bond of spring steel with welded 18–8 contact pads. These end pads are welded to an 18–8 strip on the rail web, the spring steel fish plates being bolted to the web to form the joint in the usual way.

Metal Arc Welding

As in spot welding, Monypenny[19] points out that in arc welding 18–8 to plain carbon steel there is a thin, hard, martensitic band on the steel side caused by diffusion of Cr and Ni. The majority[2,59,70,163] favor the use of 18–8 electrodes. According to Bibber,[59] the 18–8 weld metal has the same strength but lower ductility than usual. However, Schwarz[163] found, Table 22, that the 18–8 to mild steel weld using an 18–8 electrode has about the same strength as mild steel. Doubtless the joints failed in the mild steel.

Table 22—Tensile Strength of 18–8 Welded to Mild Steel. Schwarz[163]

Electrode	Type of Weld	Tensile Strength, Lb./In.^{2}
Covered 18–8	18–8 to 18–8	84,500
Covered 18–8	18–8 to mild steel	66,500
Covered mild steel	18–8 to mild steel	63,000

Table 22 indicates that mild steel electrodes may be used to obtain joints with adequate strength, but the ductility is likely to be low. Bibber found only 1.6% elongation in 2 inches and 2% reduction of area in weld metal deposited by a covered mild steel electrode between mild steel and 18–8 plates. *Nickel Steel Topics*[164] attributed the extreme brittleness of welds made with covered mild steel electrodes between 18–8 and low-alloy nickel steel to the pick up of alloying elements by the weld metal to form a brittle alloy steel. Preheating is sometimes advisable even when an 18–8 electrode is used or an electrode containing 25 Cr, 12 Ni may be used. According to Johnston,[55] a single bead of 18–8 electrode deposited on mild steel contains 12 Cr, 5 Ni; in a similar way the deposit from a 25–12 electrode on mild steel contains 18 Cr, 8 Ni. Consequently, Johnston preferred the 25–12 electrode for welding 18–8 to mild steel. Unfortunately, Johnson quoted no tests to support his argument.

A method of lining steel tanks with 18–8 sheet $^5/_{64}$ inch thick described by Hopkins[160] consists of perforating the 18–8 and plug welding to the tank walls with an "alloy" electrode. The cost of lining mild steel pipe flanges, drums and similar parts with 18–8 sheet is analyzed by Hoffman,[165,292] who used flux coated 18–8 electrodes $^5/_{32}$ inch diameter, 150 amp., 20–22 volts at arc, and reversed polarity. If a ring of 18–8 is to be welded into a mild steel plate or box, Child[166] recommends welding the 18–8 ring to a larger mild steel ring before welding the latter into the plate or box. In this way Child believed

he was avoiding difficulties related to differences in coefficient of expansion between 18–8 and mild steel.

Before 18–8 is welded to galvanized iron it is essential that the galvanizing be removed[2,70] in the vicinity of the weld so that fuming does not occur during welding. If not, the 18–8 weld metal is brittle and is full of cracks at the junction with mild steel. Sometimes the cracks are several inches long. Bibber[50] could not detect zinc in the cracked weld metal and concluded that zinc oxide or zinc vapor was the cause of the cracks. On the other hand, Duma[291] found zinc-rich alloy in the cracks, which were intergranular and occurred only when galvanized iron was T or lap welded to 18–8 with 18–8 rod. If the plates were over $^1/_8$ inch thick the cracks occurred chiefly in, or close to the surface of, the bead and were perpendicular to the direction of welding. In thinner material cracks occurred in both weld and base metals. The Navy[100] specifies that the galvanizing must be completely removed for a distance of $^1/_2$ inch from the scarf or edge to be welded.

Oxyacetylene Welding

Machinery[2] recommends the same procedure for oxyacetylene as for arc welding 18–8 to mild steel. The proportions of Cr and Ni that enter the weld decrease as the rod approaches the perpendicular. Admixture of the mild steel with the 18–8 rod unavoidably lowers the Cr and Ni content of weld metal and its resistance to corrosion. In Alexander's[70] opinion, if the steel sheet is thicker than the 18–8 use a low carbon 18–8 rod. Otherwise a low carbon mild steel rod is called for. Monypenny[19] recommends the mild steel rod under all circumstances. C. R. Whittemore (private communication, November 1938) prefers surfacing the edge of the preheated mild steel with Monel Metal before welding to the 18–8 sheet with 18–8 rod. A French writer[20] in 1927 mentioned that there was no difficulty oxyacetylene welding 18–8 to mild steel. The oxyacetylene welding of 18–8 to Cr-Mo aircraft tubing was regarded by Johnson[47] as unsatisfactory because the joints may be brittle.

WELDING 18–8 TO NON-FERROUS METALS

The spot welding of 18–8 to aluminum[77] and Olympic bronze[118] was discussed in the section on Resistance Welding. Brass sheet has been successfully bronze-welded with the oxyacetylene torch to non-magnetic stainless steel, according to *Industrial Gases*.[278]

WELDING 18–8 WITH ADDITIONS

The effect of additional alloying elements on the welding of 18–8 is of the greatest importance, particularly from the standpoint of corrosion resistance (see the review of literature on Corrosion Resistance of Welded Joints, AMERICAN WELDING SOCIETY JOURNAL, **16** (8) Suppl., 16–25 (1937)). Apart from carbide precipitation, the effects of alloying elements on welded 18–8, process and product are of a minor order. The individual elements are arranged in alphabetical order in the following sections.

Columbium

Columbium is added to 18–8 to prevent intergranular corrosion. An 18–8 containing columbium, if it is properly made (and also sometimes heat treated), is said to be stabilized. No other function of columbium is important. Oxyacetylene weld metal deposited by columbium 18–8 rods is free from cross-weld effects, as Pratt[124] stated. The same welding procedure is used as for unalloyed 18–8, the columium 18–8 rods being free flowing. In good oxyacetylene welding 75 to 95% recovery of columbium is secured from rods containing 0.50–0.75 Si,[296] 0.50–1.0 Cb, 0.07 C max. The columbium-carbon ratio in the weld must be at least 6:1 for vessels operating below 375° C. (700° F.) and 10:1[277] for service above 375° C. Using a rod containing 0.07 C, 18.34 Cr, 9.01 Ni, 0.81 Cb, Priestley[34] and Duncan[40] found, Table 23, that quite as good tensile and bend properties are secured with the columbium 18–8 as with unalloyed 18–8.

In metal arc welding the chief consideration is the recovery of columbium from the covered 18–8 electrode. The majority[69,110,169,170] state that there is no significant loss of Cb. The loss of columbium is said to be 20 to 30% by *Machinery*,[87] 40% by Hodge,[167] and 50% by Thum.[49] The electrode should contain at least 10 times as much columbium as carbon, according to Mathews.[169]

The specifications of a leading chemical firm (Sept. 1938) state that vessels for service below 375° C. must have a ratio of columbium to carbon of at least 6 to 1, and may be used as-welded or after a stabilizing heat treatment (2 hr. at 840° C., min.), which is also a stress-relieving treatment. Vessels for continuous service from 375 to 870° C. or constructed in accordance with Cases 834 or 861 of the A. S. M. E. Code for Unfired Pressure Vessels must contain at least 10 times as much columbium as carbon. The alloy contains 0.07 C max., 18.00 Cr min., 10.50 Ni min., 1.25 Mn min., 0.75 Si max., 1.0 Cb max. If the chromium content is increased, the nickel and manganese must be increased proportionately so that the structure of the alloy is completely austenitic at room temperature.

Krivobok[168] recommends the use of 18–8 containing Cb (1.0%) when (*a*) sections thicker than $^1/_2$ inch are involved (because the cooling rate after welding is too slow in thick material for other stabilizing elements to be effective), (*b*) cross welds are necessary, (*c*) multiple beads are required, (*d*) stress relief of thick sections is necessary yet rapid cooling is impossible. He observed no intergranular corrosion at an intersecting weld in plate $^1/_4$ inch thick containing 0.08 C, 19.9 Cr, 8.97 Ni, 0.82 Cb after exposure for four 30-minute periods to a solution containing 10% HNO_3, 4% HF at 60° C.

The A. S. M. E. Boiler Code Committee[99] permits a design stress of 15,000 lb./in.^{2} with 90% joint efficiency up to 480° C. (900° F.) for welded, stress-relieved 18–8 containing Cb (Cb = 10 × carbon content, not over 1%

Table 23—Tensile and Bend Tests of Unheat Treated Oxyacetylene Welds in Annealed Plate $^1/_4$ Inch Thick. Priestley[34] and Duncan[40]

Composition of Plate, %				Yield Strength, Lb./In.^{2}	Tensile Strength, Lb./In.^{2}	Elongation, % in 2 Inches	Free-Bend Test
C	Cr	Ni	Other				
0.07	18.22	8.91	....	39,200	76,500	22	180°, no cracks
0.10	18.75	9.02	0.98 Cb	37,200	75,400	20	180°, no cracks
0.07	17.42	8.88	0.47 Ti	41,000	75,500	17	180°, no cracks

Cb) for designs according to Par. U-68. According to Franks,[296] 18–8 containing six times as much columbium as carbon are suitable for welded equipment without heat treatment after welding if the operating temperatures are below 300° C. Evidently, long exposure at higher temperatures creates susceptibility to intergranular corrosion, unless the columbium-carbon ratio is higher.

Manganese

Up to 3.42 Mn has been found in deposits from covered 18–8 electrodes, according to Bibber.[50] An unusual steel containing 18 Cr, 8 Ni, 2 Si, 2 Mn was described by Norwood.[171] The manganese made the use of flux coated rods unnecessary, it was claimed. Welded joints had a yield strength of 50,000 lb./in.², tensile strength = 80,000 lb./in.², elongation = 10% in 2 inches, fracture occurring in the weld. The specifications of a leading chemical firm (Sept. 1938) state that greater ductility will be maintained in the weld metal if approximately 2% Mn is present in the welding rod.

Manganese-Columbium

Franks[296] obtained high quality arc and oxyacetylene welds having high corrosion resistance in boiling 65% nitric acid by using rods containing approximately 19.5 Cr, 10 Ni, 0.7 Si, 0.9 Cb, 1 to 4 Mn. The function of manganese in the welding rod was not discussed fully, but obviously was different from the improvement in hot workability of 18–8 due to manganese.

Molybdenum

Molybdenum (2 to 5%) is added to 18–8 to increase the resistance to corrosion, particularly by non-oxidizing reagents such as HCl and H_2SO_4. From the standpoint of ease of welding, molybdenum does not have an unfavorable effect up to 5%.[169,172] There is practically complete recovery of molybdenum.[49] As Hodge[159] states, 18–8 with 1.5–3.5 Mo is as easy to weld as unalloyed 18–8, but the mechanical properties of welds generally are slightly inferior to welds in unalloyed 18–8. Corrosion tests by Whittemore[173] on welded 18–8 (0.07 C, 18.0 Cr, 8.0 Ni, 0.7 Si, 0.3 Mn, 2.5–3.0 Mo) revealed a uniform loss of 10% and 12.6% by weight from specimens as welded and heated to 650° C., respectively. The welds were subjected to a 100-hour test, the first 50 hr. of which was in a boiling solution of 10% $CuSO_4$, 10% HSO_4. Evaporation was made up continuously with hot water. In the succeeding 50 hours, evaporation loss was made up only at $1^1/_4$-hour intervals between which crystals precipitated on the plate. The welds were bent 180° without failure after they had been tested in corrosion and ground flush (private communication, Nov. 1938).

Although Monypenny[174] states that 18–8 with 4% Mo can be made so as to be welded without heat treatment, others are of the contrary opinion. Monypenny did not state whether the carbon content of his steel was especially low or whether stabilizing elements were added. Hessler and Kautz[91] heat treat all welded 18–8 (0.08–0.15 C) whether or not 2.50 Mo is present. The specifications of a leading chemical firm (Sept. 1938) also require heat treatment (quenching or spraying from 1100 to 1150° C.) of welded molybdenum 18–8 (0.07 C max., 17.5 Cr min., 11.0–14.0 Ni, 1.5 Mn min., 0.75 Si max., 2.75 Mo min.), which is used particularly for tubes and nozzles. It was specified that a reduced section tensile specimen $1^1/_2$ inches wide at the machined weld must develop at least 95% of the minimum tensile strength of base metal (75,000 lb./in.²). An elongation of 20% with no evidence of fissuring was required in a free-bend test. Without supplying details, Andrews and Welsh[175] observed intergranular corrosion as well as corrosion of the ferrite grains in welded 18–8 containing 4% Mo. Krivobok[110] also found that 18–8 with 3% Mo, 0.10 C max., is subject to intergranular corrosion and must be heat treated after welding.

Without stating the thickness of the material, which notwithstanding was an important factor in his opinion, Krivobok[271] reported serious corrosion in cross welds (no details) in steel containing 0.07 C, 17.5 Cr, 12.4 Ni, 2.9 Mo, but no appreciable corrosion in cross welds in a steel containing 0.07 C, 18.9 Cr, 11.4 Ni, 3.5 Mo. The welding rods had the same composition as base metal and the appearance was observed after four 30-minute immersions in a solution containing 10% HNO_3 and 4% HF at 60° C. The difference in chromium content between the two alloys was supposed to account for the difference in corrosion resistance. Waché[280] found that 18–8 electrodes containing 2% Mo were satisfactory for all but the first bead in welding plain carbon or low alloy steels (see section on 18–8 electrodes).

Molybdenum-Columbium

In oxyacetylene welding a steel $^1/_8$ inch thick (0.09 C, 19.02 Cr, 12.07 Ni, 1.99 Mn, 3.06 Mo, 0.83 Cb) that had enhanced resistance to pitting corrosion in 10% NaCl solution containing 5% $FeCl_3$ (65 hr. at room temperature), Franks[296] found that welds made with a rod of the same composition were not resistant to pitting, whereas welds made with a rod containing 0.08 C, 22.41 Cr, 13.93 Ni, 2.05 Mn, 2.72 Mo, 0.84 Cb were completely resistant.

Nitrogen

Lee[176] observed that there is little difficulty in welding 18–8 containing nitrogen and Thum[49] stated that nitrogen pick up (0.04–0.06 N_2) in metal arc welds refines the grain structure and strengthens the welded 18–8. Nitrogen is added to 18–8 for the primary purpose of refining the ingot structure.

Silicon

Apart from the effect of additions of silicon to 18–8 on the corrosion resistance in acids, silicon up to 6% sometimes is added to 18–8 to form ferrite islands and prevent carbides from precipitating wholly at grain boundaries. Bull[177] recommended adding 1.5 to 2% Si, and raising the nickel content to 11 or 12% to prevent intergranular corrosion after welding. The addition of silicon and extra chromium (0.12 C, 21 Cr, 7 Ni, 2 Si) was found by Houdremont and Schafmeister[178] to lead to the same result. Silicon alone up to 6% in plate and electrode assures absence of intergranular corrosion in plate and weld, according to Monypenny.[167,174] Moritz[63] found that an arc weld (V butt) in stabilized 18–8 made with the following electrode:

	C	Mn	Si	Cr	Ni	Ti
Electrode	0.094	0.77	1.96	21.44	8.76	0.15
Deposit	0.06	0.70	1.28	20.30	8.60	nil

showed no intergranular corrosion in the usual corrosion tests (no details) and had a yield strength of 62,500 lb./in.², tensile strength = 99,000 lb./in.², elongation = 35.0%. Fracture occurred in the weld, which was not heat treated and had nearly as fine grain structure as base metal. Using the same type of test described in the section on molybdenum, Whittemore[173] observed a uniform loss of 13.5% (as welded) and 4.6% (heated to 650° C.) by weight from welded plates containing 18.0–22.0 Cr, 9.0–12.0 Ni, 0.10 C, 1.0–3.0 Si (no details). The welds were bent 180° without failure after they had been tested in corrosion and ground flush (private communication, Nov. 1938).

Table 24—Composition of Materials Used by Friedrich[288]

Material	C	Si	Mn	Cr	Ni	Ta	Remarks
Plate, as rolled	0.095	1.17	0.50	18.7	8.15	0.85	.....
Plate, forged	0.095	1.02	0.50	18.2	8.15	0.85	For round bars
All-weld-metal, covered electrodes	0.15*	0.29	0.74	18.3	8.15	0.34	From all-weld-metal deposits
Oxyacetylene weld metal	0.10	0.20	0.65	18.1	8.12	0.35	From the butt welds

* The high carbon content of the arc weld deposit is related to the carbon content of the coating.

The ease of welding 18–8 is increased by silicon, according to Lee.[176,179] Although Monypenny[167,174] found comparatively little loss of silicon in welding (no details), Bull[180] states that silicon may be entirely lost in the arc. Atomic hydrogen welding is performed without difficulty on 18–8 containing 2% Si, Johnson[181] found. In Franks'[296] tests rods for both arc and oxyacetylene welding contained 0.60–0.69 Si to insure high recovery of columbium. Rockwell hardness tests by Phillips[13] on steel containing 0.18 C max., 0.75 Mn max., 2.0–2.5 Si, 8.0–10.5 Ni, 16.5–19.5 Cr showed that base metal was 83–86 B Rockwell, spot welds were 75–80, oxyacetylene welds were 79–83, and arc welds were 82–86 (no details).

Tantalum

Tantalum may be added to 18–8 to prevent intergranular corrosion, as Leitner[15] pointed out. Shoenmaker and Gaymans[182] state that 0.5–1.0 Ta is effective from the welding standpoint. During welding, according to Maurer,[183] Ta does not burn out to so great an extent as titanium.

Static tension and fatigue tests have been reported by Friedrich[288] on the materials shown in Table 24. The all-weld-metal was deposited by covered electrodes (positive pole) 0.16 (140 amp.) and 0.20 (175 amp.) inch diameter, in the V of an angle iron $10^1/_2$ inches long. The length of bead deposited by one electrode was the length of the deposit ($10^1/_2$ inches). Each layer was chipped and the direction of deposition was reversed for every bead. The speed of welding was 5.4 inches per minute. A short arc was held to avoid spatter and the electrode was inclined so that the arc played on the puddle, no slag being trapped. Butt welds (70° single V), were made in tacked plate 0.20 inch thick using an electrode 0.20 inch diameter (130–150 amp.). To counteract shrinkage a thin, high-speed reverse bead was deposited at 180 amps. Single V (70°) butt welds were made with an oxyacetylene torch (tip diameter = 0.08–0.16 inch, neutral flame, no flux, 10.5 ft.^{3} oxygen per hr., 10.2 ft.^{3} acetylene per hr.) using a filler rod of base metal. Left-hand welding was employed but penetration was poor and a thin reverse bead without filler rod had to be

Table 25—Static and Pulsating Tensile Tests on Cr-Ni-Ta Steel. Friedrich[288]

Specimen	Yield Strength, Lb./In.^{2} at 0.003% Set	at 0.02% Set	at 0.2% Set	Tensile Strength, Lb./In.^{2}	Modulus of Elasticity, Lb./In.^{2}	Elongation, % in $1^1/_2$ Inches	in 3 Inches	Reduction of Area, %	Tensile Strength in % of Base Metal	Fatigue Strength,* Lb./In.^{2}
1. Unwelded, as rolled	....	....	48,500	112,000	.....	....	...	...	100	47,000
2. Unwelded, as rolled but with central drill hole	....	....	59,000	101,000	.....	....	...	...	90.7	38,500
3. Unwelded, heated locally to 900° C.	....	....	64,500–68,000	99,500–104,000	.....	....	45–53	54–55	91.0	40,000
4. Unwelded, quenched in water from 1100° C.	....	....	42,500–45,000	89,000–92,500	.....	....	44	55–57	81.2	37,500
5. Butt weld, covered electrodes, unmachined	....	....	59,000	89,500	.....	....	...	...	80.1	22,000
6. Butt weld, covered electrodes, ground flush	....	....	48,500	99,000	.....	....	...	...	88.5	28,500
7. Oxyacetylene butt weld, unmachined	....	....	46,500	105,000	.....	....	...	...	94.0	10,000
8. Oxyacetylene butt weld, reinforcement ground flush	....	....	54,000	74,000	.....	....	...	...	66.5	11,500
9. Forged plate, unnotched	11,000	25,000	35,000	104,000	26,400,000	68.0	44.6	69	...	±33,500 (±28,500)
10. Forged plate, notched	15,500	25,500	34,200	124,000	26,800,000	20.6	18.6	16	...	±23,500
11. All-weld-metal, covered electrodes, unnotched	29,000	41,800	52,700	132,000	23,000,000	45.7	41.8	30	...	±34,000 (±20,500)
12. All-weld-metal covered electrodes, notched	14,400	33,400	46,300	104,000	22,400,000	5.8	...	10	...	±25,000

* Based on 2 million cycles for pulsating tension tests (375 to 700 cycles/min.) in which the lower tensile stress was always 2800 lb./in.^{2} The quoted fatigue strength is the difference between upper and lower stress. ± indicates rotating bend fatigue strength for which the criterion was 50 million cycles in air and 100 million cycles in water, the latter results being in parentheses. Water trickled over the stressed portion of the specimen in the corrosion fatigue tests. All Wöhler curves were horizontal before 10 million cycles.

Description of Specimens

1. Cross section = $2^3/_4 \times 0.20$ inch.
2. Ditto but with hole 0.67 inch diameter.
3. Heated locally in resistance welder, then machined to 0.32 inch thick; or 9 inch thickness was 0.39 inch.
4. Held 15 minutes at 1100° C. after preheating at 800° C., machined to 0.28 inch thick after heat treatment. Original thickness was 0.39 inch.

5–8. Cross section = $2^3/_4 \times 0.20$ inch.

9. 0.28 inch diameter; specimens for corrosion fatigue tests were 0.20 inch diameter.
10. 0.28 inch diameter, 60° V notch 0.020 inch deep, radius at root = 0.0032 inch.
11. 0.24 inch diameter.
12. 0.32 inch diameter, 60° V notch 0.034 inch deep.

deposited. Even so, X-ray examination revealed considerable porosity in the oxyacetylene welds, whereas the arc welds were free from defects.

The main result of Friedrich's investigation, Table 25, was the correlation he discovered between rotating bend fatigue strength and microstructure. The steel he tested has a two-phase (alpha-gamma) structure, and is said to be insensitive to intergranular corrosion. In the weld metal, the ferrite forms a thin dendritic network along which the fatigue crack traveled. In the rolled specimens the ferrite occurs as fibers perpendicular to the direction of the fatigue crack. Hence, the fatigue crack is forced to cross the gamma phase, which seems to be stronger than the ferrite. If the ferrite is globularized (water quenching from 1100° C.), the fatigue fracture passes through the globules. Perhaps the low static strength of the quenched specimens accounts for their low fatigue strength. Consequently, the dendritic structure of the weld metal and the spheroidized structure of the heat-affected zone are the two phenomena that account for the low fatigue strength of the welded joints. To explain the sensitivity of weld metal to corrosion fatigue, Friedrich believed that water was drawn into the pores of the weld metal during the tension cycle, and, not being ejected during the compression cycle, exerted a prying effect.

As was to be expected the arc welds had better static tensile properties than the oxyacetylene. Flux was not used in the oxyacetylene process for reasons of corrosion resistance. The effect of notching in lowering the strength of weld metal was believed to have been chance. A static tensile strength of 97,000 lb./in.^{2} unnotched and 105,000 lb./in.^{2} notched was determined on unfractured fatigue specimens (no details) of weld metal. Two arc welds failed to fracture at stresses above the quoted fatigue strength (no details).

Inconclusive experiments on shrinkage stresses indicated that shrinkage stresses may slightly decrease the fatigue strength. Plates 0.20 inch thick were surfaced with a bead of arc weld metal (covered electrodes, 0.20 inch diameter, 175 amp.). The beads were ground flush and one specimen was slit by cuts 0.08 inch wide on both sides of the bead and 0.04 inch distant. The slits were 7 1/2 inches long, 1/2 inch longer than the beads. At an upper tensile stress of 25,600 lb./in.^{2} (lower tensile stress = 2800 lb./in.^{2} the uncut specimen fractured after 170,000 cycles, whereas the slit specimen fractured after 325,000 cycles. In view of the irregular results on welds throughout Friedrich's tests, further experiments will be necessary before any effect of shrinkage stresses on fatigue limit is established.

The tensile and notch impact properties of welds made with a covered 18-8 electrode in 18-8, 0.20 inch thick containing 0.10 C and some Ta, have been determined by Hougardy,[55] Table 26 (no details). Hoffmann[184] reported good results from welded 18-8 containing 1.3 Ta (no details).

Titanium

Titanium is added to 18-8 to prevent intergranular corrosion. An 18-8 containing titanium, if it is properly made (and also sometimes heat treated), is said to be stabilized. For many industrial welding purposes Mathews[179] believed the steel need not contain so much titanium as the common formula (Ti = 6 (C−0.02)) required. Duma,[185] an enthusiast, has never seen an abnormal or defective condition which could be ascribed to titanium, providing the titanium content was not excessive. Excessive titanium content in a weld or welded plate was said to create ferrite and coarse grain structure. With small amounts, presumably 1% Ti or less, the weld metal is completely austenitic, although base metal may be two-phase. The ferrite islands in titanium 18-8 are subject to carbide precipitation during welding, which leads to failure in severe corroding agents, according to Inglis and Andrews.[186] Despite Duma's statement that titanium increases the ease of welding, Franks,[185] discussing Duma's results, asserted that titanium made the weld metal sluggish, and Moreau[42] observed that it had an adverse effect on the welding qualities of 18-8.

A covered 18-8 electrode containing 1% Ti is described by Schoenmaker and Gaymans.[182] The currents were low and varied from 17-30 amps. with electrodes 0.06 inch diameter to 100-135 amps. with electrodes 0.20 inch diameter. Weld metal contained 15.9 Cr, 9.11 Ni, and presumably 0.04-0.07 C. The tensile properties of butt welds in 18-8, 0.20 inch thick using the titanium 18-8 electrode are summarized in Table 27. The butt welds withstood 180° bending without failure.

Table 26—Tensile and Notch Impact Properties of Arc-Welded 18-8 Containing Some Tantalum, Hougardy[55]

Specimen	Yield Strength, Lb./In.^{2}	Tensile Strength, Lb./In.^{2}	Elongation, % in 0.79 Inch	Notch Impact Value, Mkg./Cm.^{2}
Unwelded base metal	51,200	100,000	57	19.7
As-welded, ground flush	57,500 to 58,000	101,000 to 102,000	44 to 47	14.0* to 15.9†

* Notch in middle of weld. (Type of specimen not stated.)
† Notch in fusion zone.

Table 27—Tensile Tests of Butt Welds Made with Titanium 18-8 Electrode. Schoenmaker and Gaymans[182]

Specimen	Yield Strength, Lb./In.^{2}	Tensile Strength, Lb./In.^{2}	Elongation, %	Reduction of Area, %
Unwelded base metal	48,500 to 51,000	100,000 to 103,000	46-48	49-51
Butt weld 1.38 inches wide perpendicular to axis of stress	49,000 to 50,000	100,000 to 102,000	...	...
Butt weld 1.38 inches wide parallel to axis of stress	46,000 to 47,500	99,000 to 100,000	22-24	...
All-weld-metal, 0.39 inch diameter	38,000 to 42,000	81,500 to 87,500	16-20	38-42

Table 28—Mechanical Properties of Arc-Welded Titanium 18-8, 1 Inch Thick. Hessler and Kautz[91]

Specimen	Yield Strength (0.2% Elongation), Lb./In.^{2}	Tensile Strength, Lb./In.^{2}	Elongation, % in 1.18 Inches	Reduction of Area, %	VGB Notch Impact Value, Mkg./Cm.^{2}	Cold Bend Elongation, % in 0.79 In.	Cold Bend Elongation, % in 6 In.
Unwelded	44,000	89,000	70	62	> 32.5	...	...
Stressed perpendicular to weld	46,000	92,500	40	34.8	24.5	55.5	33.1
Stressed parallel to weld	49,000	86,000	43.9	41	...	42.8	24.5

Tensile, notch impact, and bend tests were made by Hessler and Kautz[91] on heat-treated butt welds in 1-inch plate containing 0.07 C, 0.57 Si, 0.43 Mn, 18.0 Cr, 9.2 Ni, 0.48 Ti, Table 28. The welds were made 90° X, Fig. 2, with 18–8 multiple-dip electrodes. The welding and testing conditions were identical with those described for Table 6. There were no X-ray or microstructural defects.

Oxyacetylene and arc welds had about the same tensile properties, Table 29, in tests made by Zeyen[48] on welded sheet 0.12 inch thick containing 0.11 C, 0.7 Si, 0.5 Mn, 17.6 Cr, 9.1 Ni, 0.5 Ti. The tensile specimens fractured outside the weld. Bend specimens withstood 180° being without failure.

Table 29—Tests of Oxyacetylene and Arc-Welded Titanium 18–8, 0.12 Inch Thick. Zeyen[48]

Specimen	Yield Strength, Lb./In.²	Tensile Strength, Lb./In.²	Elongation, % in 11.3 $\sqrt{A}$
Oxyacetylene, welded from one side	44,000	87,000	41.9
Oxyacetylene welded from both sides	43,000	88,000	43.0
Arc welded manually	43,000	87,000	43.7
Arc welded automatically	43,000	84,500	37.8

A = Cross section

Practically all the titanium is lost in arc welding with an 18–8 electrode containing titanium.[168,169,170] According to Hodge[167] and Monypenny,[298] if the electrode contains 1.0% Ti, the weld may contain 0.1 Ti. *Machinery*[67] estimated the loss of titanium from covered electrodes to be 70 to 80%; Thum's[49] estimate was that $^2/_3$ of the titanium was lost. Krivobok[168] stated up to 80% of the titanium may be oxidized during arc or gas welding with an 18–8 rod containing Ti. Although it is commonly believed that titanium is lost by oxidation, Grossmann[187] pointed out that nitrogen may account for part of the loss. The loss of titanium in oxyacetylene welding must be considerable, especially if excess oxygen[52] is employed. In view of the loss of titanium from welding rods, Bain[66] recommends that a columbium-bearing rod be employed for welding titanium 18–8. The tensile properties of oxyacetylene welds in titanium 18–8 made with columbium-bearing rods are given in Table 18. *Machinery*[2] states that a highly infusible oxide is formed when the columbium 18–8 rod is used for oxyacetylene welding titanium 18–8 sheet. The addition of 20 to 25% cryolite to some fluxes dissolves the oxide.

Intergranular corrosion did not occur in spot-welded exhaust pipes handling burnt doped fuel, according to Ffield,[107] if titanium 18–8 0.050 inch thick was used, and specially heat treated before and after welding. Under similar conditions, failure occurred in pipes made of unalloyed 18–8. Intergranular corrosion occurred in all parts of the pipe and not solely at the welds.

Krivobok[168] observed severe intergranular corrosion in a weld deposited by a rod containing 17.75 Cr, 9.97 Ni, 0.09 C, 0.47 Ti in the zone in which the weld had been heated by an intersecting weld. The corrosion was developed by four 30-min. exposures to a solution containing 10% HNO_3, 4% HF at 60° C. The welds were in plate of the same composition as the rod and $^1/_4$ inch thick. The results indicated loss of Ti during welding.

The A. S. M. E. Boiler Code Committee[90] established an allowable design stress of 15,000 lb./in.² with 90% joint efficiency up to 480° C. (900° F.) for welded, stress relieved 18–8 containing Ti (Ti = 6 × carbon content, not over 0.60 Ti).

Tungsten

Intergranular corrosion after welding is said[14,180] to be prevented by adding 1% W to the 18–8. However, practically all authorities agree that tungsten retards but does not prevent the development of intergranular corrosion in 18–8. There is practically complete recovery of tungsten in welding.[49] Leverick[188] mentioned arc weld metal containing 17.9 Cr, 8.07 Ni, 0.95 W, but gave no details.

In oxyacetylene welding steel containing 0.10 C, 0.3 Mn, 0.6 Si, 18 Cr, 8 Ni, 1.0 W, 1.0 Ti, Raymond[29] recommended rods of the same composition and a neutral flame. Sarjant[189] had no difficulty welding steel containing 0.25 C, 1.29 Si, 0.57 Mn, 21.6 Cr, 7.65 Ni, 3.15 W to itself or to other heat resisting steels by the oxyacetylene or, preferably, metal arc process.

Vanadium

Aside from Bull's[180] vague statement that the addition of 0.6 V to 18–8 prevents intergranular corrosion in welds, nothing has been published.

WELDING OTHER HIGH-ALLOY CORROSION AND HEAT RESISTING STEELS

25 Cr–12 Ni

The procedure for oxyacetylene and metal arc welding of steels of the 25–12 type (0.20 C max.) is the same as for 18–8, according to Priestley[34] and Jennings.[139] The filler rod has the same composition as base metal. In oxyacetylene welding Jones[7] observed that plenty of flux is needed and that plate over $^1/_4$ inch thick is difficult to weld on account of an infusible oxide slag (no details). Intelligent application of flux overcomes the difficulty. Grain coarsening in oxyacetylene welds in 25–12, according to Wilkinson,[283] cannot be removed by annealing. The electrode should be the positive pole except for especially heavy sections, Geiger[190] found. He pointed out that spot welding of 25–12 and higher alloy steels is particularly easy on account of their low thermal and electric conductivity, and low melting point. Although believing that no heat treatment except, perhaps stress relieving was required for welded 25–12, Whitmer[1] states that 25–12 is no less subject to intergranular corrosion than 18–8. It should be pointed out that the main application of 25–12 is in high temperature service up to 1100° C. The Rockwell hardness of welded 25–12 (0.20 C max., 79–82 B Rockwell) was found by Phillips[13] to be 79–82 B for oxyacetylene welds, 79–80 for metal arc, and 70–75 for spot welds.

Occasionally 25–12 electrodes are used for mild steel and stainless irons.[191] The notch impact value of weld and scarf in V butt welds made with a mineral coated 25–12 electrode (no details) in a pipe (S. A. E. X 1020, alumi-

Table 30—Izod Impact Value (Ft.-Lb.) of Low Carbon Steel Welded with 25–12 Electrodes. Kinzel, Crafts and Egan[192]

Temperature of Test, ° C.	As-Welded		Normalized 900° C.		Stress Relieved 600° C.	
	Notch in Weld	Notch in Scarf	Notch in Weld	Notch in Scarf	Notch in Weld	Notch in Scarf
+ 20	73.9	81.5	66.2	77.5	81.0	82.5
− 50	80.0	82.2	72.0	82.8	69.2	48.5
− 78	59.1	88.8	55.0	21.0	61.0	11.0
−120	56.5	13.9	60.0	14.0	60.8	26.0

Table 32—Mechanical Properties of 25–20 Welds in Plain Carbon Steel. Kautz[195]

Thickness of Plate, Inch	Yield Strength, (0.2% Elong.) Lb./In.²	Tensile Strength, Lb./In.²	Elongation, % in 0.79 Inch	VGB Notch Impact Value, Mkg./Cm.² As-Welded		Aged*		300° C.†	
				Plate	Weld	Plate	Weld	Plate	Weld
0.59	44,000	84,000	28	13	24.4	9	19	...	...
1.10	43,500 to 45,500	80,000 to 82,000	29 to 40.5	12.5	35.5	10	29	13.2	32.5

* Compressed 7% and heated 1/2 hr. at 250° C.
† Heated 50 hr. at 300° C.
(See review of literature on Fatigue Strength of Welded Joints, AMERICAN WELDING SOCIETY JOURNAL, **16** (1) Suppl. 20 (1937) for additional information.)

num treated) 6 inches diameter, 1-inch wall was determined by Kinzel, Crafts and Egan,[192] Table 30. There was little change in Izod value of 25–12 weld metal as the temperature was lowered, but the heat-affected zone in the low carbon steel had low Izod value at −120° C. If the joint was heat treated the heat-affected zone had low Izod value at −78° C. The investigators concluded that 25–12 weld metal can develop full utility only with steel having high Izod value below −100° C. U welds made with the 25–12 electrode in S. A. E. X1020, 1 1/2 inches thick had 30 to 50 ft.-lb. Izod down to −120° C., but the scarf fell below 10 ft.-lb. at −50° C.

Table 31—The 25–20 Electrode Described by Kautz[195]

Composition	C	Si	Mn	Ni	Cr	N_2	O_2
Electrode	0.09	0.78	1.31	19.9	25.2	0.019	0.040
All-Weld-Metal	0.20	0.35	1.86	18.4	22.3	0.044	0.050

Diameter of Electrode, Inch	Current Amp.,	Voltage	Rate of Deposit, Minutes per Lb.	Ratio of Cross Section of Coating to Cross Section of Core Rod: For Butt and Fillet Welds	For Reinforcing Surfacing Layers
0.16	160	30	12.5	0.69–0.96	1.49–1.65
0.20	220	30	7.7	.....	.....
0.24	300	30	5.4	0.69–0.96	0.96–1.35

Mechanical Properties of All-Weld-Metal

Specimen	Yield Strength, Lb./In.²	Tensile Strength, Lb./In.²	Elongation, % in 0.79 Inch	Reduction of Area, %	DVMR Notch Impact Value, Mkg./Cm.²
As-welded	61,000	86,500	21.8	27	12.5
Stress relieved at 600° C.	60,000	87,000	28.3	13	13.2
Unwelded; water-quenched from 1050° C.	43,000	86,000	56*	70 to 75	...

* % in 10 diameters. Modulus of Elasticity = 21,500,000 lb./in².

In welding stainless iron (nickel-free) with 25–12 electrodes, nickel rapidly diffused into the base metal, according to Krivobok,[193] and the weld metal contained only 6% Ni. For example, with a rod containing 0.06 C, 23.3 Cr, 12.8 Ni, and sheet containing 0.13 C, 28.9 Cr, the weld metal contained 0.12 C, 25.5 Cr, 6.5 Ni. In other words, the weld metal is diluted with nickel-free metal melted from the scarves.

An automotive exhaust valve has been made[194] consisting of a 21 Cr, 12 Ni steel head fusion welded to a low nickel-chromium steel (S. A. E. 3140) stem (no details). Norwood[171] claimed that a steel containing 25 Cr, 10 Ni, 2 Si, 2 Mn is easy to weld without brittleness or heat treatment even with 0.5 C. A steel with 29 Cr, 9 Ni, is easy to arc weld, according to Thum,[49] but the weld should be reheated to 1180° C. (2150° F.) and cooled rapidly.

25 Cr–20 Ni

Although 25–20 is primarily a heat-resisting steel, it is widely used as a filler rod for welding mild steel. A thorough description of the 25–20 electrode is given by Kautz,[195] Table 31. The coating was sensitive to moisture. The 25–20 electrode deposited a given weight of metal in about 60% of the time required by a covered unalloyed steel electrode. The weld metal had high tensile strength and notch impact value but low ductility.

Used for welding an aging-resistant steel (Izett IV, 0.25 C, 0.6 Mn) 0.59 and 1.10 inches thick, Table 32, the 25–20 welds developed good ductility and notch impact value. The tensile specimen was machined flush and had a parallel section about 1 inch long. Notch impact tests made with the notch at different positions in the fusion and heat-affected zone showed that the lowest values were always obtained in heat-affected base metal. The maximum hardness in the weld was 174 Brinell (2.5 mm. ball, 187.5 kg. load), in the heat-affected zone 150 Brinell, and in base metal 135 Brinell. The martensitic zone in the weld metal was about 0.003 inch wide. There were indications of shallow intergranular penetration of the 25–20 weld metal into the plain carbon steel. In a normalized weld in plate 9/16 inch thick there was a completely decarburized zone about 0.01 inch wide in the

Table 33—Effect of Heat Treatment on Izett IV Welded with 25–20 Electrodes. (Average of Two Specimens). Zeyen[196]

Heat Treatment	Yield Strength, Lb./In.²	Tensile Strength, Lb./In.²	Elongation, % in 0.79 Inch Across Width of Weld	Location of Fracture	Bend Angle, Degrees	Bend Elongation, % in 0.79 Inch	DVMR Notch Impact Value, Mkg./Cm.²: Weld	DVMR Notch Impact Value, Mkg./Cm.²: Heat Affected Zone
Not treated	53,800	75,400	30.0	in weld	> 180	30.0	15.3	18.1
Stress relieved*	50,600	79,200	27.5	in weld	> 180	29.2	18.8	18.6
Normalized†	51,700	74,500	15.0	1 in weld, 1 in base metal	> 180	20.5	12.8	1 = 13.2 1 did not break

* 2 hr. at 600° C., cooled in furnace.
† 1/2 hr. at 920° C., cooled in air.
Tensile specimens were reduced section perpendicular to weld.
Machined roller bend specimen, 4.9 inches between rollers; 2.8 inches roller diameter; 3.0 inches plunger diameter.
Notch impact specimen was notched in face of weld, perpendicular to the direction of rolling.

Table 35—Butt Welds in Plain Carbon Steels, 0.24 and 0.47 Inch Thick, Made with 25–20 Electrodes. Zeyen[198]

Composition of Base Metal			Butt Welds† (average of 3 to 5 tests) Unmachined		Machined Flush		Double Fillet Weld Strength, Lb./In.²	DVMR Notch-Impact Value Mkg./Cm.² 0.47 Inch Plate	Reversed Bend Endurance Limit (10 Million Cycles) Unmachined 0.24 Inch Thick 0.79 Inch Wide Butt Weld, Lb./In.²	Firth Hardness of Butt Weld in 0.47-Inch Plate			
										In Weld		Heat in Affected Zone	
C	Mn	Si	Tensile Strength, Lb./In.²	Bend Angle, Degrees	Tensile Strength, Lb./In.²	Bend Angle, Degrees				0.04 Inch from Surface	0.24 Inch from Surface	0.04 Inch from Surface	0.24 Inch from Surface
0.11	0.47	0.12	77,000	>180	77,000	>180	>50,500*	14.0	18,500	165	168	142	139
			80,000	>180	80,000	>180							
0.17	0.55	0.31	87,000	>180	81,000	>180	51,000	13.3	Not determined	162	168	180	157
			91,000	>180	85,000	>180							
0.30	0.56	0.23	104,000	>180	90,000	>180	54,000	12.5	Not determined	157	180	235	174
			100,000	>190	95,000	180							
0.38	0.64	0.29	114,000	>180	96,000	>180	57,000	15.0	19,900	162	168	250	185
			107,000	180	102,000	180							
0.56	0.63	0.32	128,000	>180	102,000	>180	58,000	14.1	Not determined	168	195	291	240
			114,000	116	107,000	180							
0.60	0.63	0.33	128,000	135	105,000	140	64,000	13.1	Not determined	168	185	291	255
			115,000	92	107,000	90							
0.68	0.69	0.35	127,000	120	84,000	96	69,000	13.1	19,900	185	193	312	261
			107,000	78	101,000	53							

* Failed outside weld.
† Upper row for each composition applies to plate 0.24 inch thick.
Lower " " " " " " " 0.47 " "

Table 34—Tensile Tests at Elevated Temperatures on Plain Carbon Steel Welded with 25–20 Electrodes. Zeyen[196]

Temperature of Test, ° C.	Yield Strength, Lb./In.²	Tensile Strength, Lb./In.²	Elongation, % in 0.59 Inch	Location of Fracture
20	49,700	82,200	31.7	In weld
100	46,000	74,000	36.7	In weld
200	47,000	72,600	33.3	In weld
350	38,100	70,600	35.0	In weld
500	30,300	46,400	(15.8)	In base metal

The velocity of loading was less than 700 lb./in.² per second.

low carbon steel next to the weld. The decarburized zone was not present in unheat treated welds.

Heat treatment, although not advantageous, had no serious effects on the mechanical properties of X butt welds made by Zeyen[196] in Izett IV steel 0.95 inch thick, Table 33. The low tensile and bend ductility of normalized specimens was due to the base metal taking a greater share of the deformation. Although Zeyen concluded that annealing was unnecessary and did no harm, he made no corrosion tests. Tensile tests at elevated temperatures, Table 34, showed that butt welds made with 25–20 electrodes in Izett IV steel, 0.47 inch thick, maintained high strength and ductility up to 350° C. Welds made with 25–20 electrodes in plain carbon steel have high ductility up to 0.56 C, beyond which the ductility decreases, Table 35.

Besides being useful for welding plain carbon steels, particularly up to 0.35 C, the 25–20 electrode is suitable for low-alloy steels, Table 36. A micrograph of a weld containing 0.13 C, 19.96 Cr, 14.00 Ni in a Cr-Mo forging steel is discussed by Rapatz and Hummitzsch,[142] who found a martensitic zone and intergranular penetration similar to 25–20 welds in plain carbon steel. Kalisch[197] had good results, Table 37, with copper-molybdenum boiler steel (0.17 C, yield strength = 40,000 lb./in.² at

Table 38—Recommended Current for 25–20 Electrodes. Zeyen[136]

Diameter of Electrode, Inch	Current, Amp.	
0.12	80 to 90	
0.16	110 to 120	
0.20	135 to 145	Not used overhead
0.24	155 to 170	Only used under exceptional circumstances

Table 36—Butt Welds in Alloy Steels Made with 25–20 Electrodes. Kautz[196]

Steel	Thickness of Plate, Inch	Tensile* Strength, Lb./In.²	Elongation, % in 0.79 Inch	Location of Fracture	Bend Test Angel, Degrees	Bend Test Diameter of Plunger, Inch
0.12 C, 2.3 Mn	0.39	106,000	20	In weld	180	0.79
0.15 C, 0.80 Cr, 0.5 Mo	0.59	92,000	25	In weld, heat-affected zone, and base metal	150	1.18
0.15 C, 3 Ni	0.51	81,000	27.5	In base metal	180	1.02

* Parallel section about 1 inch long, machined flush.

Table 37—Locomotive Boiler Steel Welded with 25–20 Electrodes. Kalisch[197]

Temperature of Test, ° C.	Yield Strength, Lb./In.²	Tensile Strength, Lb./In.²	Elongation, % in 0.79 Inch	DVMR Notch Impact Value Unaged	DVMR Notch Impact Value Aged	Bend Test Angle, Degrees	Bend Test Elongation, % in 0.79 Inch	Bend Test Elongation, % in 1.6 Inch
20	58,000 to 63,000	74,000 to 77,000	37	15–17 mkg./cm.²	10–13 mkg./cm.²	180	50	37.5
225	53,000	70,000	...	.....	.....	180	45	36.5

225° C., tensile strength = 67,000 to 80,000 lb./in.², elongation = 18% in 0.79 inch, DVMR notch impact value = 8 mkg./cm.²) V butt welded with 25–20 electrodes using a reverse run. Zeyen[136] found that a machined butt weld made with 25–20 electrodes in a low-carbon molybdenum steel (no details) withstood 2 million cycles of tensile stress pulsating between 2800 to 38,400 lb./in.² followed by 2 million cycles pulsating between 2800 and 42,700 lb./in.² without fracture. He[199] also cited a chromium-molybdenum steel preheater tube for a high-pressure boiler, which was welded with 25–20 electrodes, was not heat treated, and gave perfect service for at least two years.

The 25–20 electrode, according to Zeyen,[136] is always coated, and is always the positive pole (d.c.). Recommended currents, Table 38, are nearly the same as for 18–8. Ordinarily the 25–20 electrode is 14 or 16 inches long, and is cut in two for vertical or overhead welding, in which cracking is more likely than in downhand. Welding should not be done in cold weather on cold metal. Pipe welding is a field for which the 25–20 electrode is particularly suited.

Oxyacetylene welding with a 25–20 rod can be applied to plates up to 0.39 inch thick, according to Zeyen.[136] A neutral flame is used with a tip one size smaller than for a mild steel rod of the same size. The rod usually is 0.12 to 0.20 inch diameter; 0.24 inch is exceptional. The static tensile and notch impact properties of 25–20 oxyacetylene weld metal are listed in Table 39.

Table 39—Mechanical Properties of Oxyacetylene Weld Metal Deposited by 25–20 Rods. Zeyen[136,199]

Heat Treatment	Yield Strength, Lb./In.²	Tensile Strength, Lb./In.²	Elongation, % in 5 Diameters	Reduction of Area, %	DVMR Notch Impact Value, Mkg./Cm.²
None	51,000	93,000	30.0	28	10.9
2 hr. at 600° C., furnace cooled.	56,500	96,000	23.3	26	10.4
1/2 hr. at 850° C., air cooled.	58,000	96,000	26.8	23	10.9

For welding 25–20 heat resisting steel (tensile strength = 97,000 lb./in.², elongation = 33%) Schoenmaker[289] used a covered 25–20 electrode which deposited metal containing 23.6 Cr, 19.7 Ni and having a tensile strength of 94,000 lb./in.², elongation = 32%. Although Schoenmaker had no difficulty in securing good penetration and in maintaining a smooth arc with little spatter, Willem Smit & Co. (private communication, November 1938) stated that the steel must be preheated, welding must be performed with the utmost care, and cooling must be slow. Nevertheless, in most instances small intergranular cracks appeared in the interior of the deposited metal. The necessity for preheating is confirmed by G. W. Plinke (private communication, November 1938).

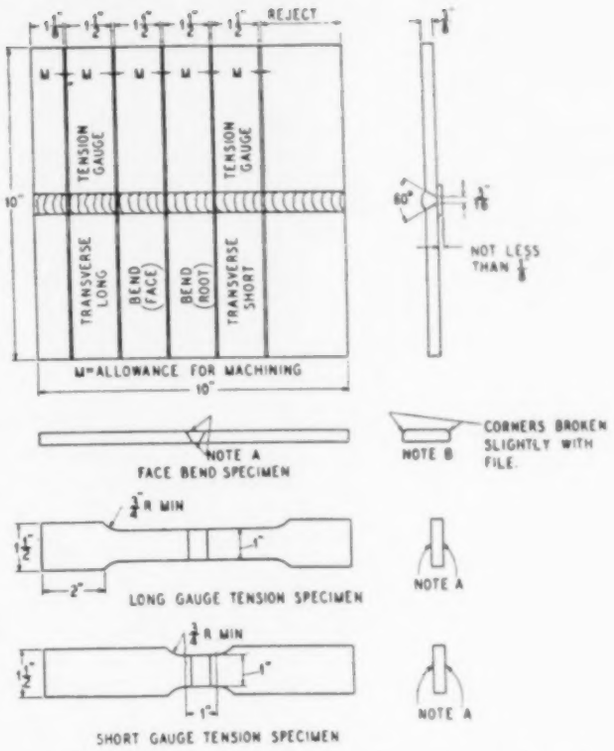


Fig. 7—Test Plate for Scale-Resisting Alloy Electrodes. (U. S. Navy)

A—Machine flush both sides; remove no undercutting
B—Break corners of bottom edges of root bend specimen; bend with root of weld in tension
C—Temperature of previously deposited weld metal shall not be over 100° C. at time of depositing subsequent bead or layer
D—Peening shall not be done unless it is to be used on the job
E—Root of weld shall not be chipped out
For the vertical welding tests, the weld shall be deposited upward
Base metal = 23–27 Cr, 17–21 Ni, 1.50 Si max., 1.10 Mn max., 0.20 C max., 0.05 P max., 0.05 S max. Stewart[60,200]

The test plate, Fig. 7, required by the U. S. Navy[60,200] for approval of welding processes for scale-resisting alloy (25 Cr, 20 Ni) contains root and face bend specimens as well as short-gage and long-gage tension specimens. A jig is used for bending the specimens (see Fig. 8, page 41, April 1938 issue of AMERICAN WELDING SOCIETY JOURNAL). Any crack or opening which exceeds 1/8 inch causes rejection. The minimum tensile strength is 80,000 lb./in.². Weld metal must contain 0.20 C max., 1.5 Si max., 1.10 Mn max., 23–27 Cr, 17–21 Ni, 0.05 S max., 0.05 P max., and must show no greater scaling than base

Table 40—Brinell Hardness of Butt Welds in Three Plain Carbon or Low-Alloy Steels Using a 23 Cr–18 Ni Electrode. Waché[286]

Tensile Strength, of Plate, Lb./In.²	Deposited Metal Minimum	Deposited Metal Maximum	Heat-Affected Zone	Base Metal
57,000	136	307	151	100
71,000	132	181	162	110
85,000	148	295	213	165

Table 41—Materials Used by George and Meikle[204]

	C	Ni	Cr	Si	Mn	Vickers Hardness of Weld: Neutral Flame	Vickers Hardness of Weld: Carburizing Flame
Stainless alloy base metal	0.15 max.	1.0 max.	16–20	0.60 max.	0.60 max.	300	405
18–8 base metal	0.20 max.	6–12	14–20	0.60 max.	0.60 max.	190	405
Welding rod	0.47	27.25	15.5	.....	.....	...	...

metal after 1 hr. at 1200° C. (2200° F.), presumably in air. Warpage, undercut and root condition are inspected, and macro- and microscopic examination is made.

Using a covered electrode containing 0.13 C, 1.35 Si, 1.10 Mn, 17.48 Ni, 23.12 Cr to prepare four-layer, 60° V butt welds in the 0.47-inch steel plates listed in Table 19, Waché[280] determined Pomey sclerometer hardness (converted to Brinell units), Table 40, and micro-shear strength and ductility. Dilution of weld metal by base metal appeared to account for the high maximum hardness in two of the welds. The areas of maximum hardness (martensite) were too narrow to affect the micro-shear ductility, which was determined on specimens 0.06 inch diameter.

The Rockwell hardness of welded 25–20 (0.20 C max., 0.70 Mn max., 2.0 Si 79–83 B Rockwell) was found by Phillips[13] to be 80–81 B for oxyacetylene, 83–84 for metal arc and 75–77 for spot welds. Sarjant[189] mentioned that 25–20 with 0.35 C, 1.7 Si, may be arc and gas welded without difficulty to itself or to other heat resisting steels. In agreement with other investigators Krivobok[110] stated that 25–20 is less subject to intergranular corrosion than 18–8 and need only be stress relieved (no details) after welding. However, the thicker the plate the slower is the cooling after welding and the greater is the danger of damage to corrosion resistance. For example, Krivobok[168] observed no corrosion in the heat-affected zone of welds (no details) in plate $^1/_4$ inch thick containing 0.07 C, 24.5 Cr, 19.5 Ni, but considerable corrosion appeared in the heat affected zone of a welded plate $^7/_8$ inch thick containing 0.09 C, 25.0 Cr, 20.1 Ni. In both tests the welds were made with strips of base metal and were tested for two periods of 4 hr. in a boiling solution containing 15% HNO, 3% HF.

20 Cr–20 Ni

As W. B. Miller[9] points out, every manufacturer has individual instructions for welding 20 Cr–20 Ni, with or without 2 Si and similar alloys. So far as can be ascertained, these instructions do not depart materially from the instructions for 18–8. Krivobok[110] states that welded 20 Cr–10 Ni requires no more drastic heat treatment than stress relieving (no details). Oxyacetylene welded pickling tanks holding hot H_2SO_4, according to Johnson,[181] provide satisfactory service when they are made of steel $^1/_4$ inch thick containing 0.16 C max., 19.5–23.5 Cr, 19.5–23.5 Ni, 1.0–1.4 Mo, 1.0–1.4 Cu, 3.0–3.75 Si. Without supplying details, Zschokke[89] found that arc welds in a steel containing 0.18 C, 18 Cr, 19 Ni (tensile strength = 108,000 lb./in.², 180° bend angle) had tensile strength = 119,000 lb./in.², 156° bend angle, with reinforcement, and tensile strength = 109,000 lb./in.², 180° bend angle with reinforcement removed.

15 Cr–9 Ni

Atomic hydrogen welds made by Meikle[201] in sheet 0.080 inch thick containing 0.11 C, 0.24 Si, 0.13 Mn, 15.17 Cr, 7.65 Ni could be bent over a radius of 0.16 inch without cracks. Sheet and weld were 130–190 Vickers hardness, and were completely austenitic. The Rockwell hardness of V butt oxyacetylene welds in plate 0.15 inch thick containing 0.17 C, 10.4 Ni, 14.9 Cr was 2 C, base metal was 20 C, according to Sasaki.[202] Base metal had a martensitic structure, which seems odd in view of Meikle's statement. A steel 0.71 inch diameter containing 0.45 C, 0.6 Si, 1.0 Mn, 16 Cr, 13 Ni, 2 W, 0.5 V was flash welded by Baumgärtel and Heinecke[203] to two different steels, 0.3 C, 1.2 Cr, 4.3 Ni; and 2.04 C, 14 Cr. At room temperature the static tensile elongation was 17 to 21% in 4 inches, reduction of area = 40%. The tensile strength was 111,000 to 114,000 lb./in.² at 20° C., 46,000 to 48,000 at 600° C., and 11,000 to 13,000 lb./in.² at 900° C. Micro-examination revealed that the diffusion zone was thin in both welds, but intergranular penetration of the Cr-Ni-W steel into the 14 Cr steel was observed. The weld to the low-alloy steel was difficult to produce. Forging the welds from 0.71 to 0.51 inch diameter refined the structure. The torsion fatigue (specimens 0.47 inch diameter) and rotating bend fatigue (specimens 0.24 inch diameter) strengths were high (see review of literature on Fatigue Strength of Welded Joints, AMERICAN WELDING SOCIETY JOURNAL, **16** (1) Suppl., 1–44 (1937)).

23 Ni–8 Cr

Alloys containing 0.3–0.5 C, 21–25 Ni, 7–9 Cr, 1–1.5 Si, 1–1.5 Cu are oxyacetylene welded in the same way as stainless iron, according to Jones,[7] but the weld metal is likely to be weak just after solidification.

25 Ni–15 Cr

Monypenny[19] recommended a rod containing 20–25 Ni, 15–20 Cr for oxyacetylene welding stainless irons. The rod was said to be particularly free flowing. Welds made with this rod using a neutral and a carburizing flame were examined by George and Meikle,[204] Table 41. The neutral flame weld in the stainless iron was dendritic with eutectic intergranular filling. The stainless iron near the weld had typical overheated structure. The junction between sheet and weld was austenitic. The weld made with a carburizing flame had a complex structure, the weld metal containing quantities of coarse hexagonal crystals of Cr_7C_3. The weld metal deposited with a carburizing flame in 18–8 contained masses of similar needles. In both welds the carbide was especially concentrated at the surface of the weld. The neutral flame weld in 18–8 was dendritic, and the sheet was austenitic in the vicinity of the weld. George and Meikle concluded that smooth welds are easier to obtain with the carburizing flame than with the neutral flame, but the carburized welds are hard and brittle. The best flame has slightly excess acetylene sufficient to form a cap on the inner cone but insufficient to carburize the metal.

According to Sarjant,[189] a heat resisting steel containing 0.45 C, 27.9 Ni, 14.5 Cr, 1.5 Si, 1.6 Mn, 4.0 W is readily welded to itself or to other heat resisting steels using arc or gas welding. A Russian writer[147] used electrodes containing 0.2–0.8 C, 25–29 Ni, 16–19 Cr, 3–4 Si, 0.5 Mn with low current for welding Silchrome (0.15–0.6 C, 2–11 Cr, 2–5 Si) and 25 Cr iron.

Table 42—Brinell Hardness of Butt Welds in Three Steels Using Two 35 Ni–12 Cr Electrodes and a 60 Ni–13 Cr Electrode. Waché[280]

Electrode	Tensile Strength of Plate, Lb./In.²	Deposited Metal Minimum	Deposited Metal Maximum	Heat-Affected Zone	Base Metal
B (35–12)	57,000	128	196	127	100
"	71,000	122	286	140	110
"	85,000	117	162	232	165
C (35–12)	57,000	142	232	137	100
"	71,000	143	196	154	110
"	85,000	159	224	224	165
D (60–13)	57,000	98	170	143	100
"	85,000	89	141	213	165

Composition of Electrodes

Electrode	C	Si	Mn	Ni	Cr
B	0.13	0.06	1.24	35.36	11.34
C	0.38	1.12	1.54	34.04	11.92
D	0.09	0.99	1.51	60.14	13.08

Table 43—Mechanical Properties of Welded High-Alloy Cr-Mn Steels

Composition of Base Metal C	Mn	Cr	Si	Ni	Thickness of Base Metal, Inch	Welding Process	Reference	Specimen	Yield Strength, Lb./In.²	Tensile Strength, Lb./In.²	Elongation, %	Bend Angle, Degrees
0.10	8	16	..	2	0.14	Arc	Hougardy[55]	Unwelded	85,000	138,000	34*	...
0.10	8	16	..	2	0.14	Austenitic electrodes	Hougardy[55]	Welded, machined flush	76,500	117,000	19*	...
0.15	14.6	19.9	0.54	...	0.08	Oxyacetylene	Brühl[211]	As-welded, machined flush	59,700	87,700	11.2†	.63‡
0.15	9.26	20.5	0.59	...	0.08	Oxyacetylene	Schafmeister[211]	Unwelded, as rolled	107,000	121,000	15.2†	...
0.15	9.26	20.5	0.59	...	0.08	Oxyacetylene	Schafmeister[211]	As-welded, not machined	.....	100,000–102,000	...	180
0.15	9.26	20.5	0.59	...	0.08	Oxyacetylene	Schafmeister[211]	Weld water-quenched from 1200° C., not machined	.....	83,000–88,000	...	180

* % in 0.79 inch
† % in 5.65 $\sqrt{A}$; A = Cross section
‡ Bend diameter = 0.08 inch

35 Ni–15 Cr

Using the covered electrodes in Table 42 to prepare 4-layer, 60° V butt welds (0.04 inch spacing) in the steel plates 0.47 inch thick listed in Table 19, Waché[230] determined Pomey sclerometer hardness (converted to Brinell units) and micro-shear strength and ductility. With the exception of one of the welds produced by electrode B, the welds were comparatively soft, the high carbon content of electrode C having little effect. The heat-affected zones were not seriously hardened. The martensitic zone was too narrow to affect the ductility in micro-shear tests on specimens 0.06 inch diameter.

A steel containing 0.50 C, 35 Ni, 15 Cr, 1.25 Si, 0.70 Mn (tensile strength = 92,400 lb./in.²) was welded by Hakin[205] with filler rods of the same composition. With coated electrodes, butt welds had a tensile strength of 87,000 lb./in.². Plug welds 1 inch diameter in three plates each 1/4 inch thick had a breaking strength of up to 65,000 lb./in.² (calculated on base metal which was 3 inches wide). The weld was pulled out intact along with the heat-affected part of the middle plate. End fillet welds in plates 1/4 inch thick developed up to 89,000 lb./in.² calculated on the weld. These results confirm Thum's[49] statement that 35 Ni–15 Cr alloy is easily arc welded, but the welds should be reheated to 1180° C. (2150° F.) and rapidly cooled. A heat resisting steel containing 0.43 C, 37 Ni, 11 Cr, 1.6 Mn, 0.2 Si was found by Sarjant[189] to be readily arc and gas welded.

18 Cr–8 Mn

Steel containing 0.12 C max., 17–19 Cr, 8.5–10 Mn, 0.75–1.10 Cu, 0.50 Ni max., 0.2–0.5 Si can be satisfactorily arc or oxyacetylene welded, according to Becket and Franks.[208] Oxyacetylene welding, Critchett[207] and others[208] found, is done with a good chromium flux and a neutral or slightly reducing flame. The steel was said to be easier to weld than 18 Cr–8 Ni steel. Arc welds have a tensile strength of 100,000 lb./in.² or more[206] with over 20% elongation in the bend test (no details). Sometimes columbium, tantalum or titanium is added[209] to prevent intergranular corrosion of welded joints. Since 18 Cr–8 Mn steel is a mixture of austenite and ferrite, intergranular corrosion sometimes occurs in two zones: one close to the fusion line in the coarse ferrite, the other 0.8 to 1.2 inches away from the fusion line, according to Tofaute.[210] The former zone is typical of welded ferritic stainless iron, the latter zone is typical of austenitic alloys, such as 18–8.

The mechanical properties of welded high alloy Cr-Mn steels reported by different investigators are summarized in Table 43.

18 Cr–9 Cu

Welded steel containing 15–18 Cr, 8–10 Cu, 0.05–0.09 C, 0.3–0.4 Mn, 0.3 Si, according to Lippert,[212] does not develop intergranular corrosion after 12 hr. at 70° C. (160° F.) in a solution containing 15% HNO_3, 4% HF, whereas welded, unalloyed 18–8 did. A weld in the 18 Cr–9 Cu steel withstood 2 bends through 180° on a radius of 1/8 inch, without failure (no details). It was claimed that the copper restrained grain growth in the heat-affected zone (no details).

WELDING STAINLESS IRONS

Stainless irons, or ferritic stainless steels as they are sometimes called, may be grouped in three classes: (1) 13 Cr, 0.12 C max. (11–15 Cr), (2) 18 Cr, 0.12 C max. (16–20 Cr), (3) 25 Cr, 0.3 C max. (20–30 Cr). Class 1 is capable of being heat treated (oil or air quenching) to 350–400 Brinell, the grain structure being refined in this way. The other two classes of stainless irons are completely or nearly completely ferritic at all temperatures. These two types are subject to coarse grain growth on being overheated, which cannot be removed by heat treatment and which renders the iron brittle at room temperature, although the hardness is always below 200 Brinell unless the irons have been cold worked. The principal difference between Class 2 and Class 3 irons is the better resistance to scaling and to corrosion by some reagents possessed by Class 3 iron.

The main difficulty[6,212,214] in welding martensitic (Class 1) stainless irons is air hardening and embrittlement of the heat-affected zone together with some loss of corrosion resistance. The remedy is annealing or quenching and tempering after welding. It should be remembered that tempering as-quenched or as-rolled stainless iron of Class 1 (for example, 0.10 C, 12 Cr) at about 525° C. may decrease the notch-impact value and may cause marked susceptibility to corrosion, the remedy for which consists in tempering at 600° C. or higher. Replacement of martensite by troostite with accompanying deposition of intergranular carbides is believed to account for the injurious effects of tempering at about 525° C. In Class 2 and 3 irons, on the other hand, embrittlement (at and below room temperature only; the welded joints are tough above 200° C.) from grain growth in the heat-affected zone is the principal difficulty, and cannot be overcome by heat treatment. As Houdremont[213] found, shrinkage stresses and carbide distribution in welded ferritic stainless iron (16 Cr or more) but not grain size are altered by heat treatment.

Generally it is not desirable to weld together different types[215] of stainless iron. A comprehensive article on welding stainless irons has been prepared by E. C. Chapman and A. J. Moses (*Welding Handbook*, 1938 Edn., 498–524).

13 Cr Irons

In oxyacetylene welding, according to Miller[9] and Jones,[7] a neutral flame is used similar to the flame for 18–8, and flux is spread on top and bottom surfaces. Too large a tip causes boiling and porosity. Flange type welds are made in sheet 0.0625 inch thick (16 gage) or lighter. For thicker material rods of the same composition or of 18 Cr iron are used. Plates thicker than 1/8 inch are beveled. Preheating is required only if double V welds are being made. All welds must be heated 30 minutes at 675–705° C. and cooled in the furnace or in still air. Torch annealing for 3 minutes is acceptable sometimes, but the temperature must not exceed 790° C. (1450° F.) if air hardening is to be avoided. French[216] and English[85] writers give the same instructions, Leverick mentioning that oxide inclusions are a difficulty and that the welds should be normalized at 780° C., which appears a bit high. Booer[217] mentioned special rods containing nickel for 13 Cr iron. Perhaps he refers to 18–8 rods, the use of which for 13 Cr iron is discussed in the section on 18–8 Welding Rods. As early as 1926 Armstrong[218] demonstrated the importance of keeping the heated zone as small as possible in oxyacetylene welding 13 Cr iron. He also recommended reheating the weld with the torch as soon as the weld cooled black.

With a silicon content of 1%, 13 Cr iron is easier to weld, Miller[93] found, than with a silicon content of less than 0.5%. The high-silicon puddle was more sluggish, had a light slag blanket, and did not boil. Jones[7] admitted these advantages, but stated that grain growth in the heat-affected zone increases as the silicon content increases. According to Priestley,[34] chromium in the puddle is readily oxidized to chromium oxide, which has a high melting point (2000° C.). If the oxide is allowed to accumulate, it will form an insulating layer between flame and puddle. A suitable flux dissolves the chromium oxide and forms a thin layer of slag which protects the metal from oxidation. Hence, the use of a flux which is a good solvent for chromium oxide is essential. Puddling, excessive heat, and improper flame control are harmful.

Arc welds, as well as gas welds, in 13 Cr iron must be annealed as Jennings[58,139] points out. An electrode (positive pole) of the same composition as base metal is used with 10% lower current than for mild steel. The welds must be heated to 790 to 820° C. for a long time, cooled in the furnace to 600° C., and cooled in air thereafter. The necessity for annealing reduces the slight advantage to be gained by using austenitic electrodes, the deposit from which is adversely affected by annealing. The electrodes are always flux coated.[217] A satisfactory coating[63] consists of a spiral wrap of white or blue asbestos yarn coated perhaps with cryolite, lime, fluorspar, ferroalloys and reducing agents. If the carbon content is not over 0.08%, Moritz[63] states, the joint need only be torch annealed at 650–700° C. to remove the hard zone. To refine the grain structure either normalize at 900° C., or quench from 950° C. and temper.

In 1926 Armstrong[218] found that bare or thin coated electrodes are unsatisfactory on account of nitrogen pick up and loss of alloying elements. He suggested that the electrode for 13 Cr iron (10–14 Cr) contain at least 16 Cr, 0.10 C max., with 1 Si or 1 Al or both. The beads should not exceed a width of 3/8 inch. The A. S. T. M. Tentative Specifications for 12% Cr Steel Castings (A 168–35T) state that good repairs in the castings may be made with a rod containing 16–18 Cr, 0.15 C, the rod being coated for arc welding and bare for oxyacetylene and atomic hydrogen welding. According to Krivobok,[110] an iron containing 0.10 C, 12–14 Cr, 0.2–0.3 Al does not air harden during welding; an 18–8 or 25–12 electrode was suggested. Deposits of arc weld metal containing 12–15 Cr, 0.5–2 Ni developed cracks in tests by Hess[153] in which the electrode was deposited on eroded cast steel turbine runners.

Atomic hydrogen welding is entirely satisfactory for 13 Cr iron, according to Moritz,[63] particularly because the temperature gradient is less severe than in arc welding and the hard zone is likely to crack. Fillet welds can be made in sheet as thin as 0.04 inch, butt welds as thin as 0.030 inch. The procedure consists of sweating the base metal followed by feeding the filler wire at the edge of the flame. The flame is moved steadily along the seam without undue oscillation. Carbon and silicon are the only elements lost, the carbon loss being increased by puddling, which, however, causes porosity. Porosity, a rare defect, is also caused by the metal freezing rapidly and trapping evolved hydrogen. Preheating often prevents porosity. Atomic hydrogen welds are molecule tight to pressures as low as 10^{-6} mm. mercury.

The mechanical properties of oxyacetylene and arc-welded 13 Cr iron are summarized in Table 44. Evidently heat treatment is essential for welds. A weld in stainless iron containing 14.1% Cr furnace cooled from 790° C. had 26.0 to 26.5 ft.-lb. Charpy in Hodge's[219] tests. Atomic hydrogen welds made by Meikle[201] in sheet 0.028 inch thick containing 0.11 C, 13.1 Cr, 0.36 Si, 0.31 Ni, 0.11 Mn, without flux or filler rod were 450 Vickers hardness in weld and heat-affected zone and were brittle. Base metal was 180 Vickers. The structure consisted of martensite with a few ferrite grains. There was only a trace of dendritic structure. In Sasaki's[202] V butt oxyacetylene weld in plate 0.15 inch thick containing 0.13 C, 11.21 Cr, 0.25 Ni there was coarse grain structure in the fusion zone and the weld was principally martensitic. The hardness of the weld was 50 Rockwell C; base metal was 13 C.

Table 44—Mechanical Properties of Welded 13 Cr Iron

Composition of Base Metal									
C	Cr	Mn	Si	Reference	Process and Specimen	Heat Treatment	Yield Strength, Lb./In.²	Tensile Strength, Lb./In.²	Elongation, % in 2 In.	Bend Angle, Degrees
0.11	12.3	0.33	0.39	Miller[93]	Oxyacetylene; sheet 0.050 inch thick (18 gage), flange type weld, 1/16-inch flange, specimens 1 1/2 inches wide, neutral flame and flux	Unwelded base metal	53,600	78,600	34	180
0.11	12.3	0.33	0.39	Miller[93]		As-welded	32,400	43,700	4.0	33
0.11	12.3	0.33	0.39	Miller[93]		Torch annealed	55,350	76,400	6.5	85
0.11	12.3	0.33	0.39	Miller[93]		Heated to 750° C., cooled in furnace	45,000	70,800	7.0	140
0.10	13.2	0.30	0.39	Jennings[139]	No details	Unwelded base metal	53,000	78,600	34	...
0.10	13.2	0.30	0.39	Jennings[139]	No details	As-welded	32,400	43,700	4	...
0.10	13.2	0.30	0.39	Jennings[139]	No details	Annealed 790° C.	45,000	70,800	20	...

18 Cr Irons

Oxyacetylene welding is carried out in the same way as for 13 Cr iron using a rod of the same composition as base metal and a flux, according to Miller[9] and Jones,[7] Alloys with 16¼ to 17¼ Cr are simpler to weld than alloys with higher chromium. The presence of 1% Si facilitates welding. An 18–8 rod may be used but exaggerated grain growth occurs in the heat-affected zone nevertheless. Ductility is improved by furnace annealing at 650 to 750° C. It was found that annealed oxyacetylene welded tubing can be cold drawn considerably. If electrolytic effects create difficulty, the side of the weld not subject to corrosion may be welded with austenitic rods, stainless iron rods of the same type as base metal being used on the active side, as Krivobok[193] suggests. If the chromium content is relatively low (16 Cr, 0.14 C or less), Thum[49] shows that heat treatment (4 hr. at 760° C., furnace cool to 590° C. followed by air cooling) refines the grain structure. Peening may also be effective. The addition of 2% Ni to 18 Cr iron (0.09 C) restricts grain growth but the welds require heat treatment, according to Hatfield,[220] which consists of air hardening from 950° C. followed by tempering.[19] Preheating to 120° C.[49] (250° F.) or 200–430° C.[173] (400–800° F.) is desirable for welding 18 Cr iron.

Welds of any type in 18 Cr iron are unavoidably rather brittle, Monypenny[19] points out and the welded vessel must be tempered at 700° C. Torch annealing with a soft flame is permissible[208] if the tempering temperature is not unduly exceeded. According to the specifications of a leading chemical firm (Sept. 1938), welded equipment in 15–16 Cr iron (0.08 C max., 15.0–16.0 Cr, 1.0 Mn max., 0.50 Si max., 0.025 max., 0.025 P max., 0.40 Ni max.) must be soaked (4 hr. per inch of thickness) at 760–790° C. (1400–1450° F.) and cooled slowly (56° C. per hr.) to at least 540° C. (1000° F.). To alleviate the brittleness in arc welding Jennings[139] recommends preheating to 100–200° C. (200–400° F.). Further, the welds must be heated to 790–820° C. (1400–1450° F.) and slowly cooled to spheroidize the carbides and so to restore ductility. Puddling must be avoided. Hess[153] found hairline cracks in a ground deposit (350 Brinell) made with an electrode containing 18% Cr (no details) on an eroded cast steel turbine runner. An 18 Cr iron electrode should be used, according to Whitmer,[1] even for arc welding 18 Cr iron to mild steel. The same procedure as for 18–8 is adopted, there being less warpage with 18 Cr iron on account of its low coefficient of expansion. In Holmberg's[221] experience the electrode for 15–18 Cr iron should contain 1.25% more chromium than base metal. Heat treatment is essential: heat slowly to 800–860° C. (1475–1575° F.), hold 8 hr., cool slowly to at least 600° C. In this way the carbides are partially spheroidized. Preheating is advisable particularly in cold weather to minimize the thermal shock of the arc. An electrode containing 19.5 Cr is used by Moritz[63] to weld 18 Cr iron. Arc welds in 18 Cr iron are likely to be porous in his opinion. Consequently, Moritz preferred atomic hydrogen welding, the process being particularly applicable even to 20 Cr iron, according to Thompson.[14] Government permission[222] has been granted in Germany to arc weld stationary boilers of 17 Cr iron.

Although Ragsdale[116] advised against spot welding stainless iron on account of brittleness, Smith[132] cited flash-welded 18 Cr iron strip that could be bent 180° at or near the weld without fracture. Aluminum[77] can be spot welded to stainless iron using Elkaloy (copper-rich alloy) tips at 2.55 volts. If a tungsten tip is used on the stainless the voltage is 1.5.

The mechanical properties of oxyacetylene and arc-welded 18 Cr iron are shown in Table 45. The welds are 15 to 30% weaker than base metal. Annealing raised the strength of welded sheet but had little effect on the strength of welded plate. Unheat treated welds are

Table 45—Mechanical Properties of Welded 15–18 Cr Irons†

Composition of Base Metal C	Cr	Mn	Si	Reference	Process and Specimen	Heat Treatment	Yield Strength, Lb./In.²	Tensile Strength, Lb./In.²	Elongation, %	Bend Angle, Degrees
0.09	15.3	..	..	Zeyen[48] (0.24-inch plate)	Base metal	None	57,500	82,000	20.3*	>180
0.09	15.3	..	..		Oxyacetylene weld	None	44,000	68,000	7.8*	16
0.09	15.3	..	..		Oxyacetylene weld	Annealed (below 800° C.)	40,000	69,000	21.6*	>180
0.09	15.3	..	..		Arc weld	None	45,500	70,000	15.6*	32
0.09	15.3	..	..		Arc weld	Annealed (below 800° C.)	40,000	69,500	25.2*	>180
0.13	17.2	..	..		Base metal	None	52,000	78,500	21.9*	>180
0.13	17.2	..	..		Oxyacetylene weld	None	44,000	69,000	5.1*	14
0.13	17.2	..	..		Oxyacetylene weld	Annealed (below 800° C.)	45,500	72,500	17.8*	>180
0.13	17.2	..	..		Arc weld	None	47,000	71,500	16.0*	20
0.13	17.2	..	..		Arc weld	Annealed (below 800° C.)	44,000	70,000	20.6*	>180
0.08	17.21	0.29	1.05	Miller,[49] Thum[49] and Jennings[139] (Jennings quotes 20% elongation in 2 inches for the annealed weld)	Oxyacetylene, sheet 0.050 inch thick (18 gage), flange type weld 1/16-inch flange specimens 1½ inches wide; neutral flame with flux	Unwelded base metal	63,500	77,100	31.0‡	180
0.08	17.21	0.29	1.05			As-welded	41,500	53,400	1.0‡	25
0.08	17.21	0.29	1.05			Torch annealed	47,600	68,900	6.0‡	110
0.08	17.21	0.29	1.05			Heated to 750° C., cooled in furnace	50,800	73,800	11.6‡	150
0.11	17.8	0.4	0.5	Hengler[223] (plates 0.24 inch thick annealed 2 hr. at 750–780° C. before welding)	Base metal	Annealed	52,500	83,000	22.3	>180
0.11	17.8	0.4	0.5		Oxyacetylene weld	None	45,000	73,000	7.8	28
0.11	17.8	0.4	0.5		Oxyacetylene weld	Annealed 750–780° C.	49,500	76,500	17.4	>180
0.11	17.8	0.4	0.5		Arc weld	None	50,000	77,000	15.0	30
0.11	17.8	0.4	0.5		Arc weld	Annealed 750–780° C.	50,000	79,500	21.2	>180

* % in 5.65 $\sqrt{A}$; A = Cross section
† See Table 46 for Tofaute's results on arc and gas welded unalloyed 18 Cr iron.
‡ % in 2 inches.

Table 46—Charpy Notch-Impact Value of Arc-Welded 16–18 Cr Iron. Hodge[219]

% Chromium (presumably in weld)	Charpy Value, Ft.-Lb. As-Welded	Weld Heated to 790° C. and Cooled in Furnace	Weld Heated to 790° C. and Cooled in Air
17.6	1.0	2.7	15.0
16.3	1.0	16.5	18.0
15.7	1.0	15.5	21.0

brittle; annealing raises the ductility close to that of base metal. The higher ductility of the arc compared with the gas welds probably may be attributed to the smaller heat-affected zone in arc welding. With both processes the heat-affected zone probably has the same low ductility although the grain size was coarser in the gas than in the arc welds examined by Tofaute.[210]

The Charpy notch-impact value at room temperature of arc-welded 16–18 Cr irons also is improved by annealing, as Hodge[219] showed, Table 46. Without heat treatment the welds had 1 ft.-lb. Charpy. Heat treatment in-

Table 47—Alloyed 18 Cr Irons. Tofaute[210]

Composition and Mechanical Properties of Annealed Plates, 0.24 Inch Thick

No.	C	Si	Mn	Cr	Mo	Ti	Ta	Cb	Yield Strength, Lb./In.²	Tensile Strength, Lb./In.²	Elongation, % in $L = 11.3\sqrt{Area}$
1	0.10	0.35	0.47	16.5	..	..	..	..	48,400	72,700	24.2
2	0.11	0.85	0.49	17.9	..	1.24	..	..	51,200	79,700	23.4
3	0.13	0.39	0.42	17.4	2.10	..	..	..	45,500	73,300	23.7
4	0.04	0.44	0.36	18.2	..	..	..	..	34,800	57,800	27.3
5	0.06	0.04	0.41	16.8	..	0.88	..	..	49,800	75,500	22.1
6	0.04	0.42	0.41	17.5	..	..	1.09	0.09	35,600	67,400	23.6
7	0.05	0.55	0.55	18.4	..	..	0.09	0.71	49,800	76,500	19.0
8	0.04	0.35	0.47	19.2	..	..	0.31	0.35	43,500	67,400	17.0
9	0.05	0.30	0.27	17.4	2.08	..	..	..	54,100	78,500	24.1
10	0.05	1.83	0.52	18.1	..	..	..	..	66,200	87,600	14.2
11	0.04	0.67	0.44	24.3	..	..	..	..	49,800	68,400	20.0
12	0.04	0.63	0.46	29.3	..	..	..	..	51,200	73,900	23.9
13	0.03	0.45	0.43	23.5	..	0.60	..	..	44,100	71,800	27.5
14	0.05	1.02	0.43	30.4	..	1.14	..	..	64,000	93,300	18.9

Effect of Titanium and Tantalum on the Mechanical Properties of Gas and Arc-Welded 18 Cr Irons Using Rods of Base Metal as Filler Metal. Plates 0.24 Inch Thick

No.	Yield Strength, Lb./In.²		Tensile Strength, Lb./In.²		Elongation, % in $L = 11.3\sqrt{Area}$		Number and Location of Fractures*		Angle of Bend, Degrees	
	Gas	Arc	Gas	Arc	Gas	Arc	Gas	Arc	Gas	Arc
1	46,900	46,900	69,700	69,800	10.0	19.8	2 *S*, 1 *B*	3 *B*	8, 17, 18	39, 51, 61
2	55,500	59,700	67,500	78,500	3.0	19.2	1 *S*	2 *B*	5, 11, 9	6, 17, 39
4	44,100	55,500	56,900	65,400	4.0	6.4	2 *S*	1 *B*, 2 *S*	20	22, 22, 41
5	....	57,300	....	73,500	...	5.9	...	3 *S*	...	15, 33
6	44,100	46,900	64,500	67,400	8.6	20.9	2 *S*	2 *B*	19, 21, 41	5, 31, 37

* *S* = Fracture in weld; *B* = Fracture in base metal.

Effect of Titanium and Tantalum on Mechanical Properties of Gas-Welded 18 Cr Iron Using Austenitic Welding Rod. Plate 0.24 Inch Thick

No.	Yield Strength, Lb./In.²	Tensile Strength, Lb./In.²	Elongation % in $L = 11.3\sqrt{Area}$	Number and Location of Fractures*	Angle of Bend, Degrees	Depth of Intergranular Corrosion, 200 Hr. in $CuSO_4$–H_2SO_4 Soln. Inch
1	42,700	57,100	9.1	3 *S*	49, 40, 40	0.10
4	42,700	52,600	3.7	1 *S*	10	0.12
5	50,700	71,100	6.0	3 *S*	33, 42, 66	0.00
6	44,100	65,400	16.4	1 *S*, 2 *B*	41, 74, 78	0.12

* *S* = Fracture in weld; *B* = Fracture in base metal.

Effect of Alloys on Mechanical Properties of Arc-Welded Irons with 18, 24 and 30% Cr using Austenitic Electrodes. Plate 0.24 Inch Thick

No.	Yield Strength, Lb./In.²	Tensile Strength, Lb./In.²	Elongation, $L = 11.3\sqrt{Area}$ %	Number and Location of Fractures*	Angle of Bend, Degrees	Depth of Intergranular Corrosion, 200 Hr. in $CuSO_4$–H_2SO_4 Solution, Inch
1	46,900	68,500	21.4	3 *B*	48, 80	0.08
2	54,000	76,900	19.3	3 *B*	85, 130	0.00
3	52,600	75,500	17.2	2 *B*	61, 82	0.10
4	56,900	67,700	10.6	3 *B*	90, 99, 99	0.11
5	56,500	78,200	15.2	3 *B*	132, >180, >180	0.00
6	45,500	66,900	19.8	3 *B*	>180, >180, >180	Trace
7	....	79,900	13.0	1 *S*, 2 *B*	107, >180, 107	0.00
8	48,100	68,300	18.2	2 *B*	75, 115, >180	0.00
9	54,000	71,200	17.4	3 *B*	88, >180, >180	0.08
10	60,000	81,800	17.4	2 *B*	27, >180	0.11
11	61,300	74,100	15.8	2 *B*	50, 99	0.10
12	64,100	78,400	16.0	2 *B*	111, >180	0.08
13	53,100	73,800	14.5	1 *S*, 2 *B*	>180, >180	0.00
14	72,700	91,300	11.7	2 *U*	49, >180	0.00

* *S* = Fracture in weld; *B* = Fracture in base metal; *U* = Fracture in junction of weld with base metal.

creased the Charpy value, the increase being greater for air cooling than for furnace cooling. The lower the chromium content the higher was the Charpy value after heat treatment. Whitmer[224] showed that a butt weld in 18 Cr iron 3/8 inch thick fractured in a brittle manner without bending when it was struck with a sledge at 20° C., but at 200° C. (400° F.) the weld bent 80° without failure.

The gain in ductility obtained through heat treatment of welded 18 Cr iron has not been explained. Although Whitmer believed the absence of toughness was caused by martensite, there is no evidence for martensite, except possibly in small isolated patches, in welded 18 Cr iron (0.12 C max.). Heat treatment does not change the coarse grain size, to which is attributed the brittleness of the welds, but does eliminate shrinkage stresses and alters the carbide distribution. Houdremont[213] and others believe that the effect of heat treatment on carbide distribution explains the increased ductility obtained through heat treatment. If so, the cause of brittleness is not coarse grain size except to the extent that coarse grain may alter carbide distribution. It is unlikely that shrinkage stresses account for the brittleness of welded 18 Cr iron.

Along with the grain coarsening in welding goes a tendency toward intergranular corrosion in reagents to which base metal itself is not completely resistant, according to Hengler[223] and Schiffler.[203] The intergranular corrosion is more pronounced in gas than in arc welds because the heat-affected zone is smaller in the arc welds. By analogy with 18–8, it may be thought that the addition of elements, such as columbium and silicon, may be useful in preventing intergranular corrosion of welded 18 Cr iron.

The effect of additional elements on welded 18 Cr iron was mentioned by Priestley[34] (Cb and Ti to eliminate air hardening) and has been investigated particularly by Tofaute,[210] Table 47, who refers to earlier patents on the effects of carbide forming elements on nickel-free stainless iron. His welds were 60° V butt and were made with rods and coated electrodes of the same composition as base metal or of 18–8. Base metal was annealed at 750 to 900° C. before welding. The welds were not heat treated. All welds were machined flush on both sides.

In general the arc welds had better ductility than the oxyacetylene welds, but the bend angle was not improved merely by lowering the carbon content below 0.10%. However, carbide forming elements and, to a less extent, molybdenum and silicon greatly improved the ductility. Carbide forming elements reduced the grain size in the heat-affected zone by increasing the number of nucleating particles distributed through the iron. Furthermore, the carbide forming elements prevented the formation of small patches of martensite in the heat-affected zone, perhaps because the carbon remained in combined form and did not go into solution during welding. Titanium and columbium were equally effective in both respects, tantalum being less so. Unless at least 1.5% was present, silicon and molybdenum were not effective in preventing the occurrence of martensite.

The corrosion tests showed that increase in Cr, Si or Mo, or reduction in carbon content had little effect in preventing intergranular corrosion, which occurred close to the weld in the zone of coarse grains. The zone of intergranular corrosion was closer to the weld in 18 Cr iron than in 18–8. It was found that to prevent intergranular corrosion a carbide forming element must be present, preferably in excess to offset loss or dilution in welding. Tantalum was not so effective as Ti and Cb, perhaps because the grain size was coarser in the Ta irons than with Ti or Cb. The same extraordinarily low and uniform corrosion exhibited by welded Ti and Cb 18–8 in boiling 50% HCl was shown by the 18 Cr irons containing Ta, Ti, Cb or Ti and Cb. In general, the Ti or Cb irons were more resistant to intergranular corrosion than the irons containing Ta. However, in some severe reagents (no details) the position was reversed. According to J. C. Holmberg (private communication, Sept. 1938), columbium and titanium added to the electrode coating do much to eliminate porosity in the deposited metal.

Arc welds in a steel containing 18 Cr, 2 Si, 0.24 inch thick, had good ductility in Schottky's[151] tests, Table 48, only if austenitic electrodes were used. Although unwelded 18 Cr, 2 Si iron pipe 2.2 inches o.d., 0.10-inch wall could be crushed flat without failure, butt-welded pipe could not.[225] Krivobok[193] recommends the addition of 0.20 Al to 16–20 Cr iron (0.11 C, 16.5 Cr) to decrease air hardening, the welding being done with austenitic electrodes. He too observed the relatively high ductility of welds in 16–20% Cr iron made with austenitic electrodes but offered no explanation.

Table 48—Arc-Welded 18 Cr, 2 Si Iron. Schottky[151]

	Static Tensile Test, Welds Machined Flush				Bend Angle Degrees, Unmachined Specimens
Electrode	Yield Strength, Lb./In.^{2}	Tensile Strength, Lb./In.^{2}	Elongation,* % in $5.65\sqrt{A}$	Location of Fracture	
18 Cr, 2 Si	62,000	73,000	3.5– 3.8	In weld	13 to 28
Austenitic	58,000	80,000	18.2–21.5	Outside weld	30 to 105

* A = Cross section.

Like Tofaute, Hougardy[55] found, Table 49, that oxyacetylene welds in iron containing 0.10 C, 17–18 Cr, 1.8 Mo, with or without Ti, were less ductile in tension and bend tests even after being annealed at 750° C., than arc welds, which could be bent 180° without fracture in the as-welded condition. Annealing had no effect on grain size but improved the ductility if grain growth was not excessive during welding. An austenitic welding rod apparently was used. Atomic hydrogen welds were said to have properties similar to arc welds. Tantalum and columbium were as satisfactory as titanium. These carbide-forming elements restrained grain growth in the heat-affected zone and prevented the appearance of gamma phase at the grain boundaries in the zone. As shown in Table 50, the required content of the carbide forming element must be adequate to prevent both grain growth and the appearance of gamma phase.

The atomic hydrogen welding of a steel containing 0.09 C, 18.1 Cr, 2.19 Ni, 0.33 Si, 0.18 Mn, 0.080 inch thick, has been investigated by Meikle,[201] who used neither flux nor filler rod. The welds cracked at a small angle of bend, unless they were heat treated (air-quenched from 950° C., tempered at 550° C.). The heat-treated welds could be bent 180° over a radius of 0.16 inch without cracking. The unheat-treated weld was 180 Vickers hardness, heat-affected zone was 350 Vickers, and base metal was 280 Vickers. The microstructure of the unheat-treated weld (× 100) consisted of gamma with thick alpha grain outlines. The grain size was coarser than base metal. Air hardening caused the formation of a new constituent, which appeared to be changed to sorbite on tempering at 500° C. Oxyacetylene welds in the 18 Cr–2 Ni iron were made with a rod containing 0.15 C, 13 Cr, 8 Ni[226] as well as with a rod of the same composition as base metal. Both welds had undesirable, dendritic structure and were not resistant to 2% HNO_3. It should be pointed out that the addition of

Table 49—Tensile Properties of Welds in 18 Cr Iron 0.12 Inch Thick Containing Mo with or Without Ti. Hougardy[55]

Specimen	Yield Strength, Lb./In.²	Tensile Strength, Lb./In.²	Elongation, % in 3 Inches	Location of Fracture
Steel with about 0.1 C, 17 to 18 Cr, 1.8 Mo				
Oxyacetylene weld, unmachined	....	63,000	1.6	At the weld
Ditto, annealed 30 min. at 750° C.	....	63,500	3.8	At the weld
Arc weld, unmachined	....	70,500	5.5	At the weld
Ditto annealed 30 min. at 750° C.	59,000	81,500	23.1	Outside the weld
Arc weld, ground flush	....	64,500	4.8	In the weld
Steel with about 0.1 C, 17 to 18 Cr, 1.8 Mo with Ti				
Oxyacetylene weld, unmachined, welded from one side	64,700	75,500	5.5	At the weld
Ditto, annealed 30 min. at 750° C.	59,000	79,500	10.7	At the weld
Arc weld, unmachined, welded from both sides	66,000	85,500	24.2	Outside the weld
Arc weld, welded from both sides, ground flush	66,000	87,500	24.0	Outside the weld

2% Ni to 18 Cr iron introduces austenite and its decomposition products into the microstructure.

25 Cr Irons

Primarily used for high-temperature service, the 25 Cr irons also have better resistance than the lower chromium irons to corrosion by the class of reagents, such as nitric acid, to which the stainless irons are resistant. In contrast with the 13 Cr irons which always become at least partly austenitic at elevated temperatures and with the 18 Cr irons in which there is often some austenite formed and the carbides are small, the 25 Cr irons (0.35 C max.) are ferritic at all temperatures and contain quantities of carbides. There is no possibility of refining coarse grain growth, to which the 25 Cr irons are subject on being overheated, save by mechanical work. Miller[9] explains that the 25 Cr irons are always brittle (1 or 2 ft.-lb. Izod, but static tensile elongation is 25–30% in 2 inches) at room temperature but are tough at 480° C. (900° F.). However, holding at 480° C. for some time, or cooling slowly through the range 550 to 400° C. increases the brittleness to an extreme degree. Consequently, the 25 Cr irons are preheated to 480° C. for welding and are reheated to a red heat and cooled rapidly after welding. It will be seen that there may be three reasons for brittleness in welded 25 Cr irons: (1) the alloy itself in the best condition has extremely low notch impact value at room temperature; (2) if the iron is held at or cooled slowly through the range 550 to 400° C. the brittleness is increased; (3) the coarse grain structure of the heat-affected zone also adds to the brittleness. Krivobok[193] reported 84 to 90 B Rockwell in a weld in 28% Cr iron made with austenitic electrodes.

For oxyacetylene welding, according to Jones,[7] a rod of the same composition as base metal is used. Overheating leads to boiling and porosity. Flux is required on both sides. If flux is not applied to the underside a groove is created by severe oxidation. Like the lower chromium stainless irons the welded 25 Cr irons are subject to intergranular corrosion in the heat-affected zone, according to Tofaute,[210] Table 47.

The brittleness and, to some extent, reduced corrosion resistance of welded 25 Cr iron have prompted the addition of alloying elements. Nitrogen refines the grain structure,[34] and there is no difficulty in welding 25 Cr iron containing nitrogen with rods of the same composition. Franks[227] states that there is no loss of nitrogen in welding. The improvement in properties of the weld on adding nitrogen is exemplified in Table 51. The base metal could be bent 180° without cracking (thickness not stated), whereas the welds cracked at the slightest bend. The higher nitrogen weld was favored by low carbon content. Nitrogen raised the tensile strength and the unnotched impact value of the weld. The nitrogen content of the lower nitrogen weld presumably was picked up in welding.

Both oxyacetylene and arc welding may be used with high nitrogen 25 Cr iron, as Franks[227] showed, Table 52. Standard samples (no details) were machined from welds in plates 1/4 inch thick that had been heated 4 hr. at 875° C. and air cooled before welding. The plates contained usual amounts of Si and Mn (no details). The heat treatment decreased the strength but improved the elongation a little. According to Wright,[228] full strength and ductility (no details) were exhibited by welds in high nitrogen 25 Cr iron welded with coated 18–8 electrodes. Similar good results were obtained with atomic hydrogen welding using a bare filler rod (25 Cr with N_2). There were no coarse crystals in the heat-affected zone with either type of welding.

Columbium and titanium also have been added to 25 Cr iron[34] to improve the properties of welds. As shown by Tofaute,[210] titanium prevents intergranular corrosion, and, if the chromium content is low, improves the bend ductility. With higher chromium (30%) or possibly higher silicon, the effect of titanium on ductility vanishes. The low carbon content of base metal and the use of austenitic electrodes in Tofaute's tests should be borne in mind.

Another element that has been added to 25 Cr iron is molybdenum, an example being the steel described by Kesper,[229] which contained 25 Cr, a few per cent Mo and

Table 50—Effect of Titanium and Columbium on the Intergranular Corrosion of 18 Cr Steel Containing Molybdenum. Hougardy[55]

C	Cr	Mo	Ti	Cb	Ratio of C to Ti or Co to Cb	Intergranular Corrosion (No Details) After Heating to 1200° C., Air Cooled	Welded
0.10	18.56	1.90	..	..	...	Yes, after 50 hr.	...
0.10	18.50	1.94	0.21	..	1:2.1	Yes, after 90 hr.	Yes, after 200 hr.
0.09	17.82	1.89	0.43	..	1:4.8	Yes, after 200 hr.	Ditto
0.10	18.25	1.86	0.65	..	1:6.5	No	No
0.08	18.35	1.93	0.53	..	1:7.4	No	No
0.09	18.78	1.88	..	0.63	1:7	Yes, after 50 hr.	Yes, after 120 hr.
0.10	18.51	1.96	..	1.01	1:10	No	No
0.10	18.12	1.78	..	1.3	1:13	No	No

Table 51—Effect of Nitrogen on Arc-Welded 25 Cr Iron. Hodge[219]

Specimen	Composition C	Cr	Ni	N_2	Tensile Strength, Lb./In.²	Elongation, % (in 2 Inches)	Reduction of Area, %	Charpy Impact Value, Ft.-Lb., Notched	Unnotched
Base metal	0.10	26.0	..	..	75,000	30	60	3.0	30.0
Weld with unalloyed 25 Cr iron electrodes	0.25	28.8	0.56	0.10	53,000	Nil	Nil	1.0	4.0
Weld with 25 Cr iron electrode containing nitrogen	0.11	27.0	0.21	0.33	66,000	Nil	Nil	2.5	18.0

Table 52—Effect of Heat Treatment on Welded High Nitrogen 25 Cr Iron. Franks[227]

Welding Process	Composition of Base Metal C	Cr	N_2	Heat Treatment	Yield Strength, Lb./In.²	Tensile Strength, Lb./In.²	Elongation, % in 1 Inch
Oxyacetylene	0.17	24.19	0.24	As-welded	66,500	92,700	3
Oxyacetylene	0.17	24.19	0.24	Heated to 875° C., air cooled	57,100	80,500	7
Metal arc	0.19	25.30	0.24	As-welded	64,000	85,500	7
Metal arc	0.19	25.30	0.24	Heated to 875° C., air cooled	53,600	82,400	10

low carbon. For oxyacetylene welding, a special rod, step back welding, and slightly reducing flame were recommended. Only a little borax flux was used. The weld should not be reheated and should be lightly peened. As in all Cr iron welds, excess oxygen created porosity while too much acetylene gave the weld a bright, metallic appearance and embrittled it. Arc welding was possible with a slag coated electrode. The welding of wire 0.012 inch diameter containing 30 Cr, 5 Al, 65 Fe by the condenser discharge method to itself or to copper wire was described briefly by Wrana.[230] Welded to itself the 30 Cr, 5 Al wire had 80% of the strength of unwelded wire. The welds to copper were more satisfactory.

WELDING MARTENSITIC STAINLESS STEELS

Stainless steels generally contain 11 to 18 Cr with 0.15 to 1.0 C. Their outstanding application is cutlery. They are not recommended for welding[6] any more than are plain carbon steels containing over 0.35 C whose structure they resemble in some respects. Stainless steel was welded[231] successfully at least as early as 1921. The air hardening difficulty is acute with stainless steels. Annealing and slow cooling are usually essential, Whittemore[173] suggesting cooling from 775° C. (1425° F.) at not over 30° C. per hr. (50° F. per hr.) to 450° C. (850° F.). Cracking increases as the carbon[232] and chromium[15] contents are increased. To prevent welds (0.23–0.30 C, 14–18 Cr) from cracking during cooling Monypenny[19] reheats the weld to a tempering temperature (700° C.) with a soft torch as soon as the metal has cooled sufficiently to become magnetic. Reheating converts martensite to softer, more ductile sorbite. Schäfer's[233] attempt to deduce the hardness of welded stainless steel from Monypenny's[19] charts of hardness *vs.* temperature from which the steel has been cooled is open to the objection that the cooling rate is not considered. Since the maximum cooling rate must exert an important effect on the hardness of the weld, despite the air hardening capacity of the stainless steels, the existing information summarized in the following sections is classified according to welding process.

Oxyacetylene Welding

For steels containing 0.1 to 0.4 C, 12–14 Cr, Boutté[22] recommends a neutral flame (2.3 cu. ft. acetylene per hr. per inch) with flux on the underside. The flux is used to prevent oxidation of chromium, not to dissolve chromium oxides. No flux is needed on the torch side, which is protected by the flame. With low carbon the welds should be reheated 1/2 hr. at 650–850° C.; higher carbon welds require furnace cooling from 900–920° C. Leitner[15,33] preheated stainless steel with 14–18 Cr to 600° C. and used a slightly reducing flame, which keeps the loss of chromium to less than 2%. The blue cone of the flame should not be allowed to touch the puddle, although in this way it is made clearer and more fluid. The smallest possible excess acetylene is necessary for stainless steels (12–13 Cr or 18–20 Cr, 1–2 Ni), according to Dobson and Taylor,[234] who, like Sutton,[235] used a rod containing 0.25–0.55 C, 24–28 Ni, 15.5–19 Cr, 2–3.5 Si, 0.60–1.25 Mn, which has better fluidity than base metal.

Tensile properties of butt welds made with this rod, Table 53, were poor for the lower chromium steel, but were good for the higher chromium steel. It may be presumed that heat treatment was essential. Miller[9] found that oxyacetylene welds in stainless steel (over 0.20 C,

Table 53—Tensile Properties of Stainless Steels Oxyacetylene Welded with Cr–Ni–Si Rod. Sutton[235]

Specimen	Yield Strength, (0.1% Elongation), Lb./In.² Unwelded Base Metal A	T	Tensile Strength, Lb./In.² A Unwelded Base Metal	A Welded Joint	T Unwelded	T Welded	Elongation, % in 2 Inches A Unwelded	A Welded	T Unwelded	T Welded
12 Cr, 0.15 C[1]	54,500	56,500	82,500	61,000–62,000	74,000–76,500	52,000–63,000	30–32	3.0–5.5	29–31	2.5–6.0
16–20 Cr, 1–3 Ni, 0.20 C[2]	59,000	66,000	95,000–97,000	95,000–97,000	92,500	93,000	17–18	12–14	19	7.5–8.0

A = Air hardened from 950° C. and reheated to 700° C.
T = Reheated to 700° C.
[1] Tubing 1 inch o.d., 0.056-inch wall; welded samples failed at weld.
[2] Sheet 0.056 inch thick; welded samples failed outside weld.

12–16½ Cr) were hard and brittle, but could be benefited by hot working and expert heat treatment, for example, heating to 810° C. (1500° F.) and cooling in a closed furnace. Zeyen's[48] results, Table 54, amply confirm these conclusions. His welds had no bend ductility even after heat treatment (no details).

Arc Welding

Since the application of stainless steel seldom involves welding, little has been done toward developing arc welding processes. As might have been expected bare electrodes are hopeless[15] on account of oxides. As with oxyacetylene welding, arc welds should be heat treated.[213] Schimpke and Horn[226] stated that a steel with 12 Cr, 2.0 C is easy to arc weld, whereas a steel with 12 Cr, 0.2 C is difficult (no details). Leverick[85] also found stainless steels easier to arc weld together than stainless iron. Annealing immediately after welding was essential. Although the tensile properties of arc welded stainless may be good, Table 53, there is likely to be no bend ductility despite heat treatment. A steel with 13 Cr 0.2 C welded with a high grade alloy electrode (no details), according to Zschokke,[89] had a short-time tensile strength of 90,400 lb./in.² at 20° C., and 75,200 lb./in.² at 450° C.

Atomic Hydrogen Welding

There was no loss of carbon in Weinman's[237] tests on atomic hydrogen welding die steel containing 13 Cr, 2 C. The steel was preheated to 750–780° C., which would have been unnecessary had the carbon content been below 0.70%. In 1928 Blanch[51] recommended atomic hydrogen welding for steels containing 12 to 40 Cr.

Resistance Welding

Spot welds made by Ffield[107] in steel containing 0.16 C, 13 Cr were brittle, which was Ragsdale's[116] experience with shot welding stainless steels. Static and impact tensile tests on heat-treated flash and resistance butt welds in steel containing 13 Cr, 0.3 C, Table 55, were made by Galibourg and Ballay.[124] The resistance butt-welded specimens had unsatisfactory static tensile properties, perhaps owing to defects. The hardened flash welds had no static ductility, but the annealed welds were ductile. The impact tensile strength and ductility were low compared with those of a plain carbon steel (0.10 C) welded under similar conditions and water-quenched from 900° C.

SOFT SOLDERING

The soft soldering of stainless steels is not advisable if the solder is subject to corrosion in service. In fact, Tay[238] never soft solders stainless restaurant equipment, because the flux corrodes lock seam joints. Steel[239] soft solders the interior of flanged seams of 18–8 restaurant tanks to prevent lodging of food. The soldered tank is filled with water before the outside of the seams is welded. Kriegel[240] found that soft soldered joints in 18–8 lost 10% of their strength after 2 weeks in 20% salt spray.

The soft soldering of stainless steels is similar to unalloyed steel and depends to only a small extent on the composition of the steel, although Monypenny[19] states that austenitic chromium-nickel steels tin better than the plain chromium stainless alloys. If the solder does not adhere, the surface should be etched for 5 or 10 minutes with a solution containing 63% by weight HCl, 35% ferric chloride, 2% HNO_3. The soldering flux is a saturated solution of zinc chloride in a mixture of equal parts of water and conc. HCl. The difficulty of obtaining adherence was overcome by Kantorowicz[241] by pressing a tinned brass shim by means of a soldering iron on the warmed stainless steel covered with the usual flux. While it is floating on the layer of solder, the shim is moved to and fro during cooling to allow dirt and superfluous solder to escape. The other part is soldered to the brass shim in the customary way. Mechanical cleaning

Table 54—Properties of Welded Stainless Steels 0.16 Inch Thick. Zeyen[48]

Composition of Base Metal				Yield Strength, Lb./In.²		Tensile Strength, Lb./In.²		Elongation, % in $5.65\sqrt{\text{Area}}$		Angle of Bend to First Crack, Degrees	
C	Cr	Ni	Specimen	As-Welded	Heat Treated	As-Welded	Heat Treated	As-Welded	Heat Treated	As-Welded	Heat Treated
0.15	13.5	1.7	Unwelded	....	98,000	...	119,000	..	22.5	...	180
0.15	13.5	1.7	Oxyacetylene weld	B	99,500	B	114,000	B	11.6	0	0
0.15	13.5	1.7	Arc weld	105,000	101,000	125,000	120,000	14.3	16.6	0	0
0.23	14.3	0.7	Unwelded	...	80,000	...	114,000	..	23.0	...	155
0.23	14.3	0.7	Oxyacetylene weld	B	81,000	B	105,000	B	10.4	0	0
0.23	14.3	0.7	Arc weld	B	85,000	B	115,000	B	20.4	0	0
0.40	13.7	0.5	Unwelded	...	109,000	...	129,000	..	16.7	...	75
0.40	13.7	0.5	Oxyacetylene weld	B	...	B	135,000	B	..	0	0
0.40	13.7	0.5	Arc weld	B	...	B	126,000	B	..	0	0

0 = Not measurable.
B = All specimens cracked during machining.

Table 55—Static and Impact Tensile Tests of Flash and Resistance Butt-Welded Steel Containing 0.3 C, 13 Cr. Galibourg and Ballay[124]

Heat Treatment	Welding Process	Brinell Hardness	Static Tensile Test: Yield Strength, Lb./In.²	Static Tensile Test: Tensile Strength, Lb./In.²	Static Tensile Test: Elongation, % in 4¾ Inches	Static Tensile Test: Reduction of Area, %	Impact Tensile Test: Fracture Energy, Ft.-Lb.	Impact Tensile Test: Elongation, % in 1.3 Inches
A	Flash	407	....	84,000 to 206,000	0	0	42	6.3
A	Resistance butt	407	188,000	188,000	0	0	31	4.5
B	Flash	208	58,000	98,000	15.3	62	32	11
B	Resistance butt	208	....	60,000	0	0	31	7.5

A = Oil quenched from 900° C. (413 Brinell), reheated to 400° C., air cooled.
B = Heated 30 min. at 650° C., cooled in furnace.
Details of welding and testing are supplied in Tables 16 and 17.

of the stainless steel seems to be unnecessary. A similar solution to the problem of adherence in soft or silver soldering and brazing was offered by Reed.[242] The stainless steel is electrolytically etched in 5% HNO_3 and is flash plated with copper, which provides adherence for solder.

Whitmer[243] and Taylor[5] found that the surfaces may be roughened with an emery wheel instead of with acid. A 50–50 Pb-Sn solder is used for tinning the edges. The iron should be large, on account of the low thermal conductivity of stainless alloys, but the iron should not be heated above the usual soldering temperature otherwise the buckling will be increased, especially with 18–8 and the temperature of carbide precipitation may be reached. After the edges are tinned the joint is either riveted or sweated with more solder, and washed with ammonia water[300] or soap and 5–10% NaOH to neutralize the flux. The zinc chloride flux is favored by the majority of writers.[8,17,43,244] For soft soldering 25–12, Geiger[190] suggests a flux consisting of equal parts of zinc chloride and ammonium chloride. A 50% solution of orthophosphoric acid is recommended by Boutté[22] and Hatfield[6] for all stainless alloys, if for any reason zinc chloride is not suitable.

A flux consisting of 41% by volume commercial aniline, 27% phosphoric acid (sp. gr. = 1.75), rem. ethylene glycol was found by Willstrop, Sidery and Sutton[245] to be free from undesirable odor and corrosive effects yet to exert a satisfactory fluxing effect (see review of literature on Corrosion Resistance of Welded Joints, American Welding Society Journal, **16** (8) 39 (1937)) when used as a flux with lead-tin solder on 18–8, 18 Cr–2 Ni and 13 Cr stainless steels. The phosphate of which the flux is composed forms phosphoric acid on being heated. Aniline is evolved as gas and the acid etches or cleans the steel of films. Plücker's[4] flux consists of 4 parts borax and 1 part $FeCl_3$ in HCl saturated with $ZnCl_2$. It was said that borax alone did not dissolve the chromium oxides. Zig-zag seams were favored. To remove the black film that prevents tinning of stainless steel, a welding magazine[295] recommends aqueous ammonia, which is flowed on the edges to be soldered a moment before they are tinned. Kikkawa[244] stated that a torch flame may be used instead of a soldering iron. A number of solders (no details) were used on 18–8 dental sheet by Zehbe,[109] the soldering temperature being 600° C. for all. Microscopic examination revealed carbide precipitation in all specimens. The specimens were not resistant to 1% lactic acid solution. A special solder in the form of an amalgam with unusually low melting point was used by Hostettler[300] without flux.

SILVER SOLDERING

On account of the good corrosion resistance and color match of silver solder it is used extensively for joining stainless steels. Although the manipulation of wire and torch is the same for stainless steels as for mild steel, the stainless surface requires a different flux with sometimes a different solder, and the flow point of the solder may be in the range of temperatures of air hardening (for stainless steels) or of carbide precipitation and intergranular corrosion (unalloyed 18–8 and some stainless irons and steels). For example, Miller[17] recommends a silver solder of high melting point for unalloyed 18–8, for silver solders having a flow point below the carbide precipitation range are not ordinarily suitable. On the other hand, Whitmer[1] prefers a silver solder of low melting point to reduce warpage and scale. Hostettler[300] silver soldered 12–14 Cr steel below 820° C., 16–18 Cr alloys below 870° C.

The silver solder used by Monypenny[19] contains 44 Cu, 33 Zn, 23 Ag (melting point = 700° C.). Stainless iron strips 2 inches wide, $^1/_{16}$ inch thick have been joined with the solder and bent 180° close to the unheat treated joint with no sign of failure. A more expensive solder: 27 Cu, 11 Zn, 62 Ag provides a closer color match. Richards[246] uses silver solders with 50 Cu and either 40 Zn, 10 Ag or 35 Zn, 15 Ag for 18–8 with a flux of 75% borax, 25% boric acid. The silver solders with high melting point often contain nickel. For example, equal parts of Cu and Zn with 12–15 Ag and 2–3 Ni is suggested by Herrmann[86] and Plücker,[4] who praised its adherence as well as its corrosion resistance, which was said to be enhanced by nickel. The nickel was said also to prevent coarse structure in the silver solder. Soldering is done at 900–920° C. Besides increasing adhesion and corrosion resistance, the nickel in Miller's[17] solder (50–55 Cu, 27 Zn, 20–15 Ag, 3 Ni) increased the strength and the melting point. A silver solder whose essential constituent is manganese is mentioned by Boutté.[247] The melting point was not over 750° C. and the manganese appeared to play a part in removing the film of chromium oxide although a special flux was said to be necessary. For hard soldering 18–8 dental parts Steininger[124] had fair success with a solder containing 49 Ag, 1 Au, 5 Ni, 25 Cu, 20 Mn and a borax-boric acid paste as flux. The color match was excellent. The parts had to be in close contact to make a good joint, which was not ductile at best. The small gold content may explain Steininger's statement that the solder is useful for joining gold to 18–8, for which spot welding is not recommended. A less satisfactory solder contained 50 Cu, 40 Mn, 10 Ni.

The tensile strength of butt joints in cold-rolled 18–8 (tensile strength = 160,000 lb./in.^{2}) made in a jig, the silver solder being flowed into the joint, depends on the joint thickness, Table 56, and is maximum at 0.0015 inch, according to Leach.[248] Somewhat more than the optimum joint thickness (0.002–0.003 inch) should be allowed for pipe joints to assure penetration of the silver solder. Difficulty in fitting the edges was believed to account for the decrease in strength of joints thinner than 0.0015 inch. Silver soldered 18–8 tanks[247] (no details) withstood an internal pressure of 4300 lb./in^2. Kriegel[240] found that 18–8 joined with silver solder (melting point = 640° C.) broke $^1/_8$ inch from the joint in a tensile test. After two weeks in 20% salt spray failure occurred 1 inch from the joint in a tensile test. In Sasaki's[61] corrosion tests of silver soldered 18–8 and 12 Cr iron in 3% NaCl (100 hr.), 5 *N* HNO_3, 5 *N* HCl and 5 *N* H_2SO_4 (all 1 hr.), the loss in weight was the same as for welds

Table 56—Tensile Strength of Silver Soldered Butt Joints in 18–8.* Leach[248]

Joint Thickness, Inch	Tensile Strength, Lb./In.^{2}
0.0005	100,000
0.0010	113,000
0.0015	134,000
0.003	117,000
0.004	111,400
0.006	87,000
0.019	63,800
0.025	45,600

* The specimens were strips $^5/_8$ inch wide, 0.040 inch thick. The thickness of the joint was measured with a microscope on a polished section. All specimens were filed to strip thickness to remove excess solder. A flux was used consisting of borax, boric acid and an alkali fluoride. The silver solder contained 50% Ag, 15.5 Cu, 16.5 Zn, 18 Cd and flowed at 635° C. (1175° F.). All joints failed in the solder, which had a tensile strength of 65,000 lb./in^2.

made with rods of the same composition as base metals (no details of silver solder).

Many fluxes have been used besides borax. A well-known flux for dissolving the oxide on all stainless steels is the mixture of equal parts of powdered borax or boric acid and potassium fluoride mentioned by Hall,[8] Galibourg[276] and Kikkawa,[244] which may be used dry or as a paste. A paste of 1 part calcined borax and 1 part boric acid with conc. $ZnCl_2$ solution was suggested by Eyles,[43] who stated that the flux flowed freely at 780° C. and soldering was done at 840° C. The function of the zinc chloride, according to Herrmann[86] was to remove the film of chromium oxide. A similar flux was recommended by Francis[249] for sweating 18–8 tubing into fittings. Geiger[190] used a mixture of borax and boric acid to which sodium silicate was added for 25–12. A small high-pressure steam jet may be used to remove flux after silver soldering.[300]

There is no exceptional difficulty in silver soldering corrosion-resisting Cr and Cr-Ni steels and irons to many other metals. Stainless steel turbine blades have been silver soldered successfully to Monel metal wires[250] and to high-speed steel strips.[252] Special silver solders are recommended by Snelling[252] for joining stainless steel to nickel or Monel metal. According to Monypenny,[251] silver soldering at 700° C. does not harden 12–14 Cr stainless steel blading. Francis[253] used a flux (36% $FeCl_3$, 62% HCl, 2 parts HNO_3, with or without Zn chips) to silver solder nickel silver to forged stainless steel. Borax was the best flux known to Barclay[254] for silver soldering silver handles to stainless steel cutlery. High vacuum apparatus involving 25 Cr iron-glass seals often rely on silver soldering for a vacuum tight joint between the 25 Cr iron and copper parts.[255]

BRAZING

On account of intergranular penetration, comparatively poor corrosion resistance of the deposit, absence of color match and the high temperature, brazing or bronze welding is not good practice for Cr and Cr-Ni corrosion resisting steels. Monypenny[19,251] states that if brazing of 12–14 Cr turbine blading is done at 1000° C., the hardness may reach 300 Brinell with low carbon content, or 450–500 Brinell, if 0.3 C is present. The hardness was 300–350 Brinell in steel containing not over 0.25 C, 16–20 Cr, 2 Ni. Parts heated to 850° C. during brazing were less resistant to corrosion than base metal. The conclusion drawn was that 12–14 Cr steel or 16–20 Cr, 2 Ni steel is not suited for bronze or cast-iron cast-in blades unless the carbon content is below 0.15%. Alloyed 18–8 was satisfactory for either brazing or "casting-in" provided it was free from intergranular corrosion effects, the suggested test for which consisted in heating the steel 30 minutes at 650° C. followed by immersing it for 72 hr. in boiling acid copper sulfate solution. The test specimen was required to withstand bending through 90°.

The only difficulty in brazing 18–8 is intergranular penetration.[214] Raymond[256] found that brazing often created large cracks in 18–8 on account of intergranular penetration of the bronze. Mild steel was less sensitive to penetration by bronze than 18–8. Without supplying details, Portevin[257] stated that he knew of a process for brazing 18–8 below 760° C. A silver solder or copper-phosphorus alloy may have been used, because the customary brazing alloys have higher melting points. Fluxes for brazing 18–8 may be waterglass[258] (to prevent oxidation during preheating), boric acid[244] (using slow heating to prevent boiling but brazing at a higher temperature than mild steel), ground borax glass[6,47] (in dip brazing, if scum collects on flux, apply $ZnCl_2$ to parts before dipping), or a mixture of borax, boric acid and sodium silicate.[190] Galibourg[276] recommended a brazing rod containing 30–70 Mn, 10–60 Cu, 10–50 Ni (melting points = 850–1050° C.), preferably 40 Mn, 50 Cu, 10 Ni, because it has the same color as 18–8. A boric acid-borax flux was used. This rod was regarded unfavorably by Steininger in the section on Silver Soldering. A nickel brass (no details) has been recommended,[294] borax being applied on both sides of the joint.

Bronze welds in stainless iron or steel (12–15 Cr), according to Jones,[7] are made with a mixture of proprietary fluxes and have a tensile strength of 55,000 lb./in.^{2}. Since heat treating temperatures are above the flow point of bronze, Jones cautioned against attempting heat treatment after brazing. His belief that the brazed joints have good corrosion resistance is not common. Norlin,[259] for instance, stated that brazed joints are inferior to welded joints, particularly if the welds are heat treated. To prevent oxide formation during heating for brazing, Reed[242] made the stainless steel the anode in a pickling bath of 5% HNO_3 at 0.1 amp. per cm.^{2}, following which a "flash" coating of copper was electrolytically deposited.

Instructions for brazing stainless steels given by Eyles[43] consist of cleaning the steel mechanically or with 50% HCl. Flux may be powdered borax (no frothing) or 2 parts calcined borax and 1 part boric acid as thin paste. Since it is said that tin and lead embrittle the joint, the brazing rod should not contain them. A lap or scarf joint was preferred to the dovetail, thin-edge joint. Preheating for dip brazing was considered essential, parts not to be brazed being coated with a mixture of molasses and graphite. The preheating for dip brazing may be done in the layer of borax floated on top of the brazing bath.[6] The dipped parts may be cleaned in 5% caustic soda solution. To braze cutlery stainless steel Parmiter[301] uses a 50–50 Cu-Zn alloy at 870° C. The flux consists of 4 parts by weight borax, 1 part $FeCl_3$ in HCl made to a thin paste with $ZnCl_2$.

Turnbull[260] had considerable trouble dip brazing stainless iron (13½ Cr, 0.12 C) turbine blades to mild steel segments using 70–30 brass. Temperatures below 1000°C. were too low, and the best spacing was 0.025 inch.

FORGE WELDING

Forge welding is impossible[19,35,48,210,225,299] for the Cr and Cr-Ni corrosion and heat resisting steels. The film of chromium oxide is said to prevent welding. So far as can be ascertained satisfactory fluxes and atmospheres have not yet been found.

PICKLING AND POLISHING

On the completion of welding it is often desirable to remove scale by pickling and to grind and polish the weld. Pickling and finishing improve the appearance and materially increase the corrosion resistance. Two pickles are suggested by Taylor.[5] (1) Solution containing 10 wt. % H_2SO_4 (60° Be) and 10 wt. % rock salt at 65° C. Rinse in hot water and immerse ½ hr. in solution of 10–20% HNO_3 (38° Be commercial) at 50–60° C. Wash in hot water. (2) 50% HCl at 50–65° C., followed by rinsing in water and pickling ½ hr. in 30% HNO_3 at 50–60° C. Wash in hot water. Several authorities[8,10,53,215,261] recommend pickling 15 min. in a solution containing 50% HCl, 5% HNO_3 and 0.25% restrainer at 50–60° C. For

aircraft purposes welds in stainless iron and steel should be varnished, according to Pollard;[215] welds in 18–8 need not be varnished. Eyles[43] suggests pickling in 20 wt. % HCl, followed by 20% HNO_3. Whittemore[173] suggests 50% HCl (75° C.) followed by 33% HNO_3.

There are several complete sets[262] of instructions for grinding and polishing. Three principles are involved: (1) avoid abrasives which embed themselves in the weld, particularly 18–8, (2) do not grind across a weld; grind in the direction of the weld to prevent scouring in the zone close to the weld, and (3) do not overheat (burnish) during grinding or polishing.

FLAME CUTTING

Despite Bignier's[33] conviction that 18–8 cannot be oxyacetylene flame cut, reliable methods have been developed. A method for cutting scrap up to 13 inches thick, described by Judelsohn,[263] makes use of a neutral flame. The tip is held perpendicular to the surface and is moved forward $^1/_{16}$ inch, backward $^3/_{64}$ inch, then forward another $^1/_{16}$ inch and so on, at the rate of 60 cycles per minute. The kerf is $^1/_2$ inch wide. Using an orifice 0.1100 inch diameter, 8 lb./in.^{2} acetylene, 75 lb./in.^{2} oxygen, Judelsohn cut a slab of 18–8, 30 inches wide, 5 inches thick into two pieces in 10 minutes. For flame cutting risers in steels with 18–30 Cr, 8–12 Ni Priestley[34] preheated the casting and used an injector type blowpipe with extra heat and a slightly carburizing flame. The saw-like motion described by Judelsohn was used. For plates the flux method was adopted. Either mild steel plates were clamped on either side of the cut to provide enough molten iron and iron oxide to flux the infusible chromium and nickel oxides, or else an iron rod was held in the cut.

The flame cutting of risers on 18–8 castings is described by Aitchison.[269] The tip should be one size larger than for plain carbon steel and should have 6 preheating jets rather than 4. Oxygen pressure should be increased 15 to 20%. The surface to be cut must be free from sand or dirt and preheated to a dark red by means of another torch. The neutral flame is held horizontal and the cut is made from top to bottom, the burner being held 2 inches from the casting and moved back and forth, not sideways, in the line of the cut. The time of cutting is longer, the spatter on the torch side greater, the cut 30% broader, and the edges rougher than for mild steel. A riser 6 x 6 inches can be cut in 3 to $3^1/_2$ minutes by this method, according to Dwyer,[275] who emphasized the importance of large oxygen hose capacity ($^3/_8$ inch diameter).

A steel rod is only used in the flame cutting of small 18–8 risers, according to Tupholme.[264] For large risers the operator does not have a free hand to hold the rod. Instead of using a steel rod, the operator preheats the top surface to be cut, brings the starting point to a white heat, turns on the oxygen and holds the torch horizontal while moving it up and down, not from side to side. The flame is neutral and the inner cones of the preheat flame are $^3/_{16}$ inch from the face to be cut. The oxygen pressure is 15–20% higher than for mild steel. The slag is more incandescent than with mild steel and sparks violently. A riser 6 inches square was cut in $3–3^1/_2$ minutes, the cut being made without difficulty to within $^1/_2$ inch of the casting.

The flux method is advocated by a French writer[265] for risers (18–8 or 14–24 Cr iron). A mild steel rod 0.16–0.24 inch diameter is held at the starting point and is heated by the torch which is held so that the axis of the jet is parallel to the rod and the flames are 1 inch from the end of the rod. The oxygen (120 lb./in.^{2} for thicknesses up to 3 inches, 150 lb./in.^{2} for thicker parts) valve is opened and oxide from the rod burns the 18–8, the cut being 0.39 to 0.59 inch wide. The tip orifice is 0.06–0.08 inch for thicknesses up to 3 inches, 0.10–0.12 for thicker parts. Without preheat a bar about 8 inches diameter was cut in 18 minutes; 18 ft. of mild steel rod was used. The average oxygen consumption was 0.7 cu. ft. per sq. in. of cut cross section.

In 1924 Atkins[266] observed that stainless steel could not be oxidized but could be cut by melting. From the appearance of the cut surfaces made by present methods the main action in flame cutting 18–8 and similar steels is melting. In 1929 Saunders[281] referred to the difficulty of flame cutting stainless steels, observed the rough cuts, and recommended the oxyhydrogen cutting torch. He was impressed by experiments then being conducted on the arc cutting of stainless steel risers. Wiss[267] found that stainless steels with 10 to 20 Cr were not easy to cut with or without preheat. Green[268] preferred the carbon arc process for cutting 18–8, Holslag[272] recommending the carbon arc for cutting stainless steel risers (no details). Schuster[162] observed carbide precipitation adjacent to a flame cut surface in an austenitic chromium-nickel steel containing 0.5 W.

ACKNOWLEDGMENTS

Contributions to the review have been received from:

R. H. Aborn, United States Steel Corporation.

Director of Scientific Research, Air Ministry, London, England.

J. C. Holmberg, Struthers Wells-Titusville Corporation.

R. H. Leach, Handy & Harman.

J. H. G. Monypenny, Brown, Bayley's Steel Works, Ltd., Sheffield, England.

G. W. Plinke, Henry Vogt Machine Company.

W. J. Priestley, Electro Metallurgical Company.

Willem Smit & Co.'s Transformatorenfabriek N.V., Nymegen, Holland.

C. R. Whittemore, Dominion Bridge Company Ltd., Lachine, Quebec.

W. D. Wilkinson, Jr., Electro Metallurgical Company.

J. H. Zimmerman, Linde Air Products Company.

BIBLIOGRAPHY

1. Whitmer, V. W., American Welding Society Journal, **16** (9) 8–19, Sept. 1937.
2. *Machinery*, (N. Y.) **44** (7) 427–429, March 1938.
3. Meslier, R., *Rev. Soud. Autog.*, **30** (287) 324–325, Feb. 1938.
4. Plücker, R., *Korrosion u. Metallschutz*, **13**, 394–395 (1937).
5. Taylor, A. E., *Product Engg.*, **8**, 399–401 (1937).
6. Hatfield, W. H., *Chem. Engg. Cong., World Power Conf.*, **1**, 33–65 (1936); *Trans. Instn. Chem. Engrs.*, **7**, 49–64 (1929).
7. Jones, O. C., American Welding Society Journal, **13** (4) 9–13 (1934).
8. Hall, C. C., *Weld. Jnl.* (London), **27**, 358–364 (1930); *Ind. Gases*, **18**, 17–23 (1937).
9. Miller, W. B., *Book of Stainless Steels, Second Edn.*, 167–176 (1935).
10. Boutté, A., *Revue du Nickel*, **3**, 98–101 (1932).
11. Leroy, A., *Metaux*, **9**, 460–470 (1934).
12. Holler, H., *Autog. Metallb.*, **22**, 182–189 (1929).
13. Phillips, S. H., *West. Machy. & Steel World*, **24**, 44–47 (1933).
14. Thompson, W., *Weld. Jnl.* (London), **29**, 77–79 (1932).
15. Leitner, F., *Schmelzschwg.*, **11**, 188–191, 212–216 (1932); *Autogen Schweisser*, **5**, 245–249 (1932); **6**, 6–9, 24–27, 36–37 (1933).
16. *Sheet Metal Worker*, **28** (5) 39–41 (1937).
17. Miller, P. H., *Stainless Steels, Oxford Univ. Press*, London, 95 pp. (1937).
18. *Ind. & Weld.*, **10** (8) 47–49 (1937).
19. Monypenny, J. H. G., *Stainless Iron and Steel, John Wiley & Son, N. Y., Second Edn.*, 485–492, 511–514 (1931).
20. *Pratique Ind. et Mèc.*, **10**, 247 (1927).
21. Carmichael, C. M., and Shaw, G. S., *Iron & Steel of Canada*, **16**, 23–28 (1933).
22. Boutté, A., *Bull. Soc. Ing. Soud.*, **8** (48) 2906–2912 (1937).
23. Wachowitz, W. J., *Welding*, **4**, 68–70 (1933).
24. Pratt, D. R., American Welding Society Journal, **14** (12) 16–19 (1935).
25. Hostettler, L. W., American Welding Society Journal, **11** (1) 25–26 (1932).

26. Van Dyke, G., *Weld. Engr.*, **16** (7) 29–30 (1931).
27. Van Dyke, G., *Oil & Gas Jnl.*, **30**, 77–78, July 2, 1931.
28. *Sheet Metal Worker*, **28** (8) 23 (1937).
29. Raymond, E. J., *Modern Engr.*, **7**, 378–380, 388 (1933).
30. Franche, G., *Prat. Ind. Méc.*, **16**, 169–170 (1933).
31. Gezov, B. B., *Katchestvennaia Stal* (10) 45–46 (1936).
32. Bainbridge, C. G., AMERICAN WELDING SOCIETY JOURNAL, **16** (4) 14–17 (1937).
33. *Autogen Schweisser*, **10**, Special Issue, 29–32, May 1937.
34. Priestley, W. J., AMERICAN WELDING SOCIETY JOURNAL, **15** (1) 14–17 (1936).
35. Smith, E. C., Snyder, C. C., Johnson, E. R. and Whitmer, V. W., *Refiner*, **12**, 140–147 (1933).
36. Gordon, F. D., *Sheet Metal Inds.*, **12** (132) 464–466 (1938).
37. Norris, J. G., *Weld Engr.*, **16** (9) 46–47 (1931).
38. Lansing, H. S., *Weld. Engr.*, **15** (2) 53 (1930).
39. Ward, N. F., *Structural Design of Metal Airplanes*, by J. E. Younger, 297–312 (1935).
40. Duncan, W. A., *Iron & Steel of Canada*, **19** (6) 5–11 (1936).
41. Hvamb, O., *Teknisk Ukeblad*, **53**, 116 (1935).
42. Moreau, L., *Bull. Soc. Ing. Soud.*, **4** (27) 1084–1095 (1933).
43. Eyles, A., *Mech. World & Engg. Record*, **93**, 306–308 (1933).
44. Spraragen, W., *Welding*, **1**, 897–900, 979–980 (1930).
45. Green, J. B., *Weld. Engr.*, **13** (12) 34–36 (1927).
46. Trunschitz, *Autogen Schweisser*, **10**, 58–59 (1937).
47. Johnson, J. B., AMERICAN WELDING SOCIETY JOURNAL, **15** (9) 2–11 (1936).
48. Zeyen, K. L., *Autog. Metallb.*, **28**, 33–41 (1935).
49. Thum, E. E., Iron & Steel Inst. Weld. Symposium, **II**, 603–616 (1935).
50. Clark, F., *Weld. Industry*, **4**, 413–417 (1936).
51. Van Dyke, G., *Steel*, **89** (1) 52–53 (1931).
52. Hignett, H. W. G., *Proc. Twelfth Intnl. Acetylene Cong.*, 1081–1085 (1936).
53. Bignier, A., *Bull. Soc. Ing. Soud.*, **8** (48) 2913–2919 (1937).
54. Dobson, R. H., and Taylor, R. F., *Weld. Ind.*, **4**, 99–104 (1936).
55. Hougardy, H., *Elektroschwg.*, **9** (1) 5–9, Jan. 1938; *Maschinenbau*, **17** (15/16) 411–412, Aug. 1938.
56. Johnston, A. P., *Weld. Engr.*, **20** (8) 19–22 (1935).
57. *Machinery* (N. Y.), **44**, 383–385, Feb. 1938.
58. Jennings, C. H., AMERICAN WELDING SOCIETY JOURNAL, **13** (4) 13–15 (1934).
59. Bibber, L. C., *Book of Stainless Steels, Second Edn.*, 177–194 (1935).
60. *U. S. Navy Dept. Tentative Spec. SPS* **941**, Oct. 19, 1933.
61. Sasaki, S., *Japan Nickel Rev.*, **5**, 172–197 (1937).
62. Prentiss, F. L., *Iron Age*, **138** (23) 40–42, 120–121, Dec. 3, 1936.
63. Moritz, M., Iron & Steel Inst. Weld. Symposium, **II**, 445–452 (1935).
64. Diatlov, V. I., and Slutska, T. M., *Academy of Sciences, Kiev, Russia, Report on Development of Chromium-Nickel Stainless Steel Electrode Coatings*, 24 pp. (1937).
65. Bull, H., and Johnson, I., *Ind. Gases*, March and June 1928; *Metallurgist*, **5**, 147–149 (1929).
66. Harris, H., *Metallic Arc Welding*, Longmans, N. Y., 1935.
67. Spooner, H. E., *Modern Engr.*, **10**, 542–543 (1936).
68. Hougardy, H., *Autog. Metallb.*, **30**, 223–227 (1937).
69. *Procedure Handbook of Arc Welding*, Lincoln Electric Co., Cleveland, Fourth Edn., 1936.
70. Alexander, S. C., *Welding*, **2**, 514–517, 684–687, 692 (1931).
71. Maurath, G. A., *Weld. Engr.*, **14** (3) 49–50 (1929).
72. Spraragen, W., *Elec. World*, **99**, 180–181 (1932).
73. Helsby, C., Hamann, C. W., and Samuely, F. J., *Welding and Cutting Year Book*, Temsbank Publ. Co., London, 111–117 (1936/37).
74. Boutté, A., *Revue du Nickel*, **8** (2) 35–38 (1937).
75. Macfarlane, J. W., *Sheet Metal Inds.*, **11**, 831–833 (1937).
76. Staggs, R. W., *Factory Mngmt. & Mntce.*, **95** (4) Part 1, 64–65, 96 (1937).
77. Ferguson, L., *Weld. Engr.*, **18** (8) 20–23 (1933).
78. Nielson, K. L., *Weld. Ind.*, **3**, 15–17 (1935).
79. Stephenson, *Weld. Jnl.* (London), **28**, 7–8 (1931).
80. Holmberg, J. C., AMERICAN WELDING SOCIETY JOURNAL, **16** (10) 44–49 (1937).
81. Blanch, F. D., *Chem. Met. Engg.*, **35**, 495–496 (1928).
82. Miller, L., *Manchester Assn. Engrs. Trans.*, 81–111 (1931–32).
83. Catlett, J. T., *Metals & Alloys*, **2**, 272–276 (1931).
84. *Welding Engineer*, **19** (8) 32(1934).
85. Leverick, F., *Weld. Jnl.* (London) **31**, 237–243 (1934); *Inst. Weld. Engrs. Proc.*, **7**, 17–35 (1933).
86. Hesrmann, H., *Feuerungstech.*, **22**, 23–24 (1934).
87. Rice, H. B., *Weld. Engr.*, **21** (1) 34–42 (1936).
88. Henry, O. H., AMERICAN WELDING SOCIETY JOURNAL, **15** (10) Suppl., 2–9 (1936); **16** (10) Suppl., 41–46 (1937).
89. Zschokke, *Brown-Boveri Review*, **24**, 311–322 (1937).
90. Chevenard, P. A., and Portevin, A. M., Iron & Steel Inst. Welding Symposium, **II**, 333–352 (1935).
91. Hessler, R., and Kautz, K., *Elektroschwg.*, **5**, 81–86 (1934).
92. Harvey, W. E., Ciastkewicz, A. J., and Whitney, F. J., Jr., AMERICAN WELDING SOCIETY JOURNAL, **14** (1) 18–23 (1935).
93. Miller, W. B., AMERICAN WELDING SOCIETY JOURNAL, **7** (5) 49–59 (1928).
94. *Autogen Schweisser*, **10**, 7–8 (1937).
95. Hodge, J. C., AMERICAN WELDING SOCIETY JOURNAL, **15** (1) 6–14 (1936); *Book of Stainless Steels, Second Edn.*, 194–197L (1935).
96. Mathews, J. A., AMERICAN WELDING SOCIETY JOURNAL, **11** (4) 23–27 (1932).
97. Houdremont, E., *Kruppsche Monatshefte*, **11**, 270–286 (1930).
98. Thum, E. E., A. S. T. M. Symposium on High-Strength Constructional Metals, 31–71 (1936).
99. Case No. 834, AMERICAN WELDING SOCIETY JOURNAL, **16** (4) 18 (1937); *Mech. Engg.*, **60**, 639 (1938).
100. Specifications for Welding, U. S. Navy Dept., 63 pp., March 1937.
101. Holmberg, J. C., *Ind. & Weld.*, **11** (3) 42–44, March 1938.
102. Prever, V. S., *Industria Meccanica*, **19**, 780–787 (1937).
103. Poste, E. P., *Ind. & Engg. Chem.*, **27**, 128–134 (1935).
104. McEvoy, P. F., *Book of Stainless Steels, Second Edn.*, 122–127 (1935).
105. Leroy, A., *Proc. Twelfth Intnl. Acetylene Cong.*, 595–599 (1936).
106. Daubois, *Revue du Nickel*, **9** (3) 82–85, May 1938.
107. Ffield, P. D., *Book of Stainless Steels, Second Edn.*, 679–686 (1935).
108. Loiseau, C., *Bull. Soc. Ing. Soud.*, **8** (48) 2920–2936 (1937).
109. Zehbe, H., *Untersuchungen über die Veränderungen des rostfreien Stahls durch Schweissen. Inaug.-Diss. Friedrich-Wilhelms-Univ. Berlin*, G. Neuenhahn G. m. b. H., Jena (1935).
110. Krivobok, V. N., *Amer. Iron & Steel Inst. Year Book*, 129–160 (1937).
111. *Sheet Metal Worker*, **28** (1) 53–54 (1937).
112. Whitmer, V. W., *Welding*, **3**, 529–530 (1932); *Steel*, **92** (8) 23–25 (1933); *Iron Age*, **130**, 498 (1932).
113. *Machinery* (London), **50**, 257–259 (1937).
114. *Pratique Ind. Méc.*, **16**, 235 (1933).
115. Souder (Sciaky), *Bull.* No. **3**, 19–35, Jan. 1937.
116. Ragsdale, E. J. W., *Book of Stainless Steels, Second Edn.*, 198–203 (1935).
117. Larsen, E. I., AMERICAN WELDING SOCIETY JOURNAL, **15** (12) 9–16 (1936).
118. Thomason, H., *Canadian Machy.*, **48** (5) 50–51 (1937).
119. Spraragen, W., *Iron Age*, **134**, 18–21, Sept. 27, 1934.
120. Hensel, F. R., *Weld. Industry*, **5**, 394–406 (1937); *Iron & Coal Trades Rev.*, **135**, 706–707 (1937).
121. Zimmerman, J. H., AMERICAN WELDING SOCIETY JOURNAL, **14** (12) 20–25 (1935).
122. *Aircraft Engg.*, **6**, 54 (1934).
123. Anderson, E. C., *Trans. A. S. M. E.*, **56**, 659–666 (1934); *Product Engg.*, **5**, 242–245, 293–295 (1934); Geschelin, J., *Automotive Inds.*, **67**, 4–6 (1932); Ragsdale, E. J. W., *Aviation*, **31**, 177–179 (1932); MacKinney, J. J., *Radio Engg.*, **17** (4) 10–12 (1937).
124. Steininger, P., *Les Aciers inoxydables et leur application à la prothèse dentaire; Thesis* No. **89**, Faculty of Medicine, Institut Dentaire, Geneva, Switzerland (1934).
125. Friel, J., *Amer. Dental Assn. Jnl.*, **23**, 1471–1480 (1936).
126. Charlier, M., *Schweiz. Monatsschrift Zahnheilkunde*, **41**, 254–266 (1931).
127. Salelles, R., *Rev. Soud. Autog.*, **27** (260) 2–7 (1935).
128. Dussourd, E. J. L., *Rev. Métallurgie*, **32**, 171–181, 210–214 (1935).
129. Hibert, C. L., *Air Corps Tech. Rep., Matériel Branch Report* **3907**; *Air Corps Information Circular*, **7**, 697, 7 pp., July 10, 1934.
130. Eksergian, R., *Trans. A. S. M. E.*, **56**, 667–701 (1934).
131. Gillette, R. T., AMERICAN WELDING SOCIETY JOURNAL, **15** (6) 6–8 (1936).
132. Smith, W. E., *Book of Stainless Steels, Second Edn.*, 204–208 (1935).
133. Clark, M., *Weld. Engr.*, **16** (11) 33–34 (1931).
134. Galibourg, J., and Ballay, M., *Revue du Nickel*, **7**, 109–114 (1936).
135. *Product Engg.*, **8**, 302–304 (1937).
136. Zeyen, K. L., *Techn. Mitt. Krupp*, **5**, 89–102 (1937).
137. AMERICAN WELDING SOCIETY JOURNAL, **15** (11) Suppl., 19 (1936).
138. Stieler, C., *Stahl u. Eisen*, **58** (13) 346–350 (1938).
139. Jennings, C. H., *How to Weld 29 Metals*, Westinghouse E. & M. Co., 100 pp. (1937).
140. Portevin, A., *Metal Progress*, **33** (2) 169–170, Feb. 1938.
141. Dorey, S. F., *Jnl. Instn. Civil Engrs.*, No. **6**, 621–666, April 1937.
142. Rapatz, F., and Hummitzsch, W., *Arch. Eisenhüttenwes.*, **8**, 555–556 (1934–35).
143. Schöne, O., *Braunkohle*, **35**, 256–257 (1936).
144. Johnson, G. W., *Natl. Petrol. News*, **24**, 29–37, Nov. 16, 1932.
145. Czyrski, W., *Przeglad Mechaniczny*, **1**, 402–407 (1935).
146. Mitchell, W. M., *Book of Stainless Steels, Second Edn.*, 304–323 (1935).
147. Kreshtchanovsky, N. S., *Vestnik Metalloprom.*, **15** (8) 20–40 (1935).
148. Portevin, A., and Séférian, D., *Paper at Eleventh Intnl. Acetylene Cong.*, 23 pp. (1934).
149. Dawson, J. R., AMERICAN WELDING SOCIETY JOURNAL, **15** (6) 18–23 (1936).
150. Hoffmann, W., *Autog. Metallb.*, **20**, 337–345 (1927).
151. Schottky, H., *VDI Sonderheft Schweisstechnik*, **2**, 16–21 (1936).
152. Stabley, T. C., *NELA Bulletin*, **19**, 539–545 (1932).
153. Hess, P. M., *Power*, **81**, 138–140, 258–260, 314–315 (1937).
154. Torkildsen, E. J., *Arcos*, **10**, 971–974 (1933); **9**, 727–728 (1932).
155. Seewer, P. W., *Engg.*, **142**, 692–695 (1936).
156. Reid, C. R., *Electric Welding*, **7** (39) 74–94, Feb. 1938.
157. Hoz, J., *Arcos*, **10**, 961–964 (1933).
158. Johnson, W. C., *Weld. Engr.*, **11** (10) 35–38 (1926).
159. Hodge, J. C., *Refiner*, **12**, 189–197 (1933).
160. Hopkins, R. K., *Chem. Engg. Cong., World Power Conf.*, **3**, 527–531 (1936); *Ind. & Engg. Chem.*, **28**, 1386–1387 (1936).
161. Goldmann, F., *Elektroschwg.*, **2**, 41–47, 81–86 (1931).
162. *British Engine, Boiler & Elec. Ins. Co. Tech. Rep.*, 32–43 (1935).
163. Schwarz, A., *Ind. & Weld.*, **10** (11) 30–32, 39, Nov. 1937.
164. *Nickel Steel Topics*, **6** (5) 9, Oct. 1937.
165. Hoffman, R. S., *Second Lincoln Prize Papers*, 404–416 (1933).
166. Child, J. H., *Weld. Industry*, **4**, 94–95 (1936).
167. Monypenny, J. H. G., *Industrial Chemist*, **13**, 96–98 (1937); quotes J. C. Hodge.
168. Krivobok, V. N., *Metal Progress*, **34** (2) 135–139, 160, Aug. 1938.
169. Mathews, J. A., *Book of Stainless Steels, Second Edn.* 431–444 (1935).
170. Scharschu, C. A., *Book of Stainless Steels, Second Edn.*, 369–387 (1935).
171. Norwood, S. M., *Weld. Engr.*, **11** (11) 25–26 (1926); *Trans. Electrochem. Soc.*, **50**, 91–94 (1929).
172. Houdremont, E., *Sonderstahlkunde, Springer, Berlin*, 355 (1935).
173. Whittemore, C. R., *Canadian Min. & Met. Bull.*, **296**, 782–823 (1936).
174. Monypenny, J. H. G., *Metal Progress*, **29**, 68–69, June 1936.
175. Andrews, W., and Welsh, W. C., Iron & Steel Inst. Welding Symposium, **II**, 307–316 (1935).
176. Lee, J. A., *Chem. Engg. Cong., World Power Conf.*, **1**, 459–488 (1936).
177. Bull, H., *Weld. Industry*, **4**, 20–23 (1936).
178. Houdremont, E., and Schafmeister, P., *Chim. et Ind., Special Issue*, 536–546, April 1934.
179. Lee, J. A., *Trans. A. I. Ch. E.*, 169–192 (1936).
180. Bull, H., *Aircraft Engg.*, **8**, 51–54 (1936).
181. Johnson, C. M., *Book of Stainless Steels, Second Edn.*, 476–484 (1935).
182. Schoenmaker, P., and Gaymans, L. W. C., *Polytechnisch Weekblad*, **29**, 97–101, 113–115 (1935).
183. Maurer, E., *Metal Progress*, **31** (5) 535–536, May 1937.
184. Hoffmann, W., *Z. VDI*, **79**, 1145–1148, 1486 (1935).
185. Duma, J. A., *Trans. Amer. Soc. Metals*, **25**, 788–819 (1937).
186. Inglis, N. P., and Andrews, W., Iron & Steel Inst. Welding Symposium, **I**, 259–265 (1935).
187. Grossmann, M., *Year Book. Amer. Iron & Steel Inst.*, 164–167 (1937).
188. Leverick, F., *Jnl. Soc. Chem. Ind.*, **8**, 472–473 (1930).
189. Sarjant, R. J., *Trans. Ceramic Soc.*, **32**, 113–135 (1933).
190. Geiger, G. F., *Book of Stainless Steels, Second Edn.*, 461–465 (1935).
191. Mitchell, W. M., *Book of Stainless Steels, Second Edn.*, 304–323 (1935).
192. Kinzel, A. B., Crafts, W., and Egan, J. J., *Trans. A. I. M. E.*, **125**, 560–583 (1937).
193. Krivobok, V. N., *Metal Progress*, **34** (1) 47–52, July 1938.
194. *Heat Treating & Forging*, **21**, 441 (1935).
195. Kautz, K., *Techn. Mitt. Krupp*, **3**, 143–173 (1935); *Wärme*, **58**, 511–519 (1935).
196. Zeyen, K. L., *Techn. Mitt. Krupp*, **4**, 162–164 (1936).
197. Kalisch, E., *Techn. Mitt. Krupp*, **4**, 168–171 (1936).
198. Zeyen, K. L., *Techn. Mitt. Krupp*, **4**, 83–89 (1936); *Stahl u. Eisen*, **56**, 654–657 (1936).
199. Zeyen, K. L., *Autog. Metallb.*, **30**, 130–138 (1937).
200. Stewart, W. C., *Jnl. Amer. Soc. Naval Engrs.*, **48**, 553–574 (1936).
201. Meikle, G., *Royal Aircraft Establ. Report* No. 06/9068, 4 pp., Feb. 1929.
202. Sasaki, S., *Jnl. Zosen Kiokai*, **56**, 47–64, June 1935.
203. Baumgärtel, K., and Heinecke, F., *Elektroschwg.*, **4**, 228–232 (1933).
204. Meikle, G., *Royal Aircraft Establ. Report* No. 06/9212, 4 pp., March 1929.
205. Hakin, W. A., *Welding*, **1**, 910–912 (1930).

206. Becket, F. M., and Franks, R., *Book of Stainless Steels, Second Edn.*, 497–501 (1935).
207. Critchett, J. H., *Metals & Alloys*, **2**, 253–256 (1931).
208. *Welding*, **2**, 373–376 (1931); Tull, R., American Welding Society Journal, **10** (6) 35–39 (1931).
209. Hougardy, H., *Chem. Fabrik*, **10**, 281–283 (1937).
210. Tofaute, W., *Techn. Mitt. Krupp*, **6** (2) 17–24, April 1938; *Z. VDI*, **81**, 1117–1122 (1937).
211. Brühl, F., *Arch. Eisenhüttenwes.*, **10**, 243–255 (1936/37).
212. Lippert, T. W., *Iron Age*, **140** (16) 54–63, Oct. 14, 1937.
213. Houdremont, E., *Stahl u. Eisen*, **50**, 1517–1528 (1930).
214. Strauss, J., *Proc. A. S. T. M.*, **30**, Part 2, 42–47 (1930).
215. Pollard, H. J., *Flight, Suppl.*, **24**, 85–87 (1152e–1152g) (1932).
216. *Rev. Soud. Autog.*, **25**, 2922–2924 (1933).
217. Booer, J. R., *Chem. Trade Jnl.*, **86**, 53–55 (1930).
218. Armstrong, P. A. E., *Trans. A. I. M. E.*, **74a**, 586–588 (1926).
219. Hodge, J. C., *Metal Progress*, **27**, 33–38, April 1935.
220. Hatfield, W. H., Iron & Steel Inst. Weld. Symposium, **II**, 363–371 (1935).
221. Holmberg, J. C., *Ind. & Weld.*, **11** (2) 28–29, Feb. 1938.
222. *Wärme*, **60**, 560–562 (1932).
223. Hengler, E., *Z. VDI*, **78**, 1163–1167 (1934).
224. Whitmer, V. W., *Welding*, **2**, 457–464 (1931).
225. Schottky, H., *Z. VDI*, **79**, 41–46 (1935).
226. *Royal Aircraft Establ. Report* No. 06/8655, 5 pp., June 1928.
227. Franks, R., *Trans. Amer. Soc. Metals*, **23**, 968–987 (1935).
228. Wright, E. C., *Trans. Amer. Soc. Metals*, **23**, 987–988 (1935).
229. Kesper, J. F., *Maschinenbau*, **12**, 436 (1933).
230. Wrana, J., *ETZ*, **56**, 991–993 (1935).
231. *Iron & Coal Trades Rev.*, **116**, 941 (1928).
232. Houdremont, E., *Sonderstahlkunde*, Springer, Berlin, 290 (1935).
233. Schäfer, R., *Korrosion u. Metallschutz*, **13**, 337–353 (1937).
234. Dobson, R. H., and Taylor, R. F., *Jnl. Royal Aeronautical Soc.*, **40**, 647–657 (1936).
235. Sutton, H., Iron & Steel Inst. Weld. Symposium, **I**, 449–458 (1935).
236. Schimpke, P., and Horn, H., *Praktisches Handbuch der gesamten Schweisstechnik*, **2**, Second Edn., Springer, Berlin, 203–208 (1935).
237. Weinman, R. A., *General Electric Rev.*, **32**, 532–537 (1929).
238. Tay, G. H., *Ind. & Weld.*, **10** (7) 53–56, 64, July 1937.
239. *Steel*, **90**, 34, Mar. 7, 1932.
240. Kriegel, P., *Weld. Engr.*, **21** (8) 32–36 (1936).
241. Kantorowicz, O., *Jnl. Sci. Inst.*, **15** (1) 28, Jan. 1938.
242. Reed, J. P., *Jnl. Sci. Inst.*, **12**, 364 (1935).
243. Whitmer, V. W., *Iron Age*, **129**, 833 (1932).
244. Kikkawa, H., *Japan Nickel Rev.*, **4**, 592–602 (1936).
245. Willstrop, J. W., Sidery, A. J. and Sutton, H., *Jnl. Inst. Metals*, **59**, 53–58 (1936).
246. Richards, E. T., *Metallbörse*, **24**, 1627 (1934); *Röhren u. Armaturen Ztg.*, (11) 18–19 (1936).
247. Boutté, A., *Rev. Soud. Autog.*, **26** (241) 9, March 1934.
248. Leach, R. H., American Welding Society Journal, **15** (10) 41–44 (1936).
249. Francis, W. B., *Metal Ind.* (N. Y.), **32**, 128 (1934).
250. Kraft, E. A., *Stahl und Eisen als Werkstoff*, **III**, 21–27 (1928).
251. Monypenny, J. H. G., *Korrosion u. Metallschutz*, **13**, 373–374 (1937).
252. Snelling, R., *Illust. Zeit. Blechindustrie*, **66**, 94–95, 120–122 (1937).
253. Francis, W. B., *Metal Ind.* (N. Y.), **35**, 558 (1937).
254. Barclay, W. R., *Metal Ind.* (London), **25**, 550 (1924).
255. Meerkamp van Embden, H. J., *Philips Tech. Rev.*, **2**, 312 (1937).
256. Raymond, E. J., *Mech. & Weld. Engr.*, **8**, 220–224 (1931); *Modern Engr.*, **11**, 514–515, 576–578 (1937).
257. Portevin, A., *Metal Progress*, **29**, 70, June 1936.
258. Biernacki, J., *Spawanie i ciecie Met.*, **8**, 67–71 (1935).
259. Norlin, E., *Chem. Engg. Cong., World Power Conf.*, **1**, 422–443 (1936).
260. Turnbull, J. M. G., *Engr.*, 162, 622–625, 648–650 (1936).
261. Holder, E., *Weld. Industry*, **5**, 231–233 (1937).
262. Snyder, C. C., *Book of Stainless Steels, Second Edn.*, 150–160 (1935); *Abrasives*, **18** (10) 25–27, 64, Oct. 1937; *Sheet Metal Worker*, **28** (11) 31–33, Nov. 1937; Wilkinson, W. D., Jr., *Ind. & Weld.*, **10** (10) 62–65, Oct. 1937; *Metal Clean. & Finish.*, **9**, 952–957 (1937); Tupholme, C. H. S., *Sheet Metal Inds.*, **12** (129) 77–78, 80, Jan. 1938; Grismer, E. P., *Abrasive Ind.*, **16** (2) 9–10 (1935); Curry, R. R., *Amer. Machinist*, **81**, 908–910 (1937); *Amer. Machinist*, **81**, 799 (1937); *Amer. Artisan*, **106** (7) 26–27, 101–102 (1937).
263. Judelsohn, F., American Welding Society Journal, **16** (3) 14–15 (1937).
264. Tupholme, C. H. S., *Weld. Industry*, **5**, 160–161 (1937).
265. *Soudeur-Coupeur*, **12** (8) 1–5, Oct. 1937.
266. Atkins, E. A., *Iron & Coal Trades Rev.*, **109**, 713 (1924).
267. Wiss, E., *Autog. Metallb.*, **32**, 46–53, 58–66, 74–79, 90–96 (1929).
268. Green, J. B., *Weld. Engr.*, **13** (7) 40–43 (1928).
269. Aitchison, R. B., *Proc. Intnl. Acet. Assn.*, 140–144 (1932); *Ind. Gases*, **13**, 212–213 (1932).
270. Vertecchio, D., *L'Aerotecnica*, **18** (5) 538–549, May 1938.
271. Krivobok, V. N., *Metal Progress*, **34** (3) 223–229, Sept. 1938.
272. *Elec. Rwy. Traction, Suppl. Rwy. Gazette*, **68**, 872 (1938).
273. Holslag, C. J., *Weld. Engr.*, **13** (10) 48 (1927).
274. Monroe, L. C., *Weld. Engr.*, **19** (1) 26 (1934).
275. Dwyer, P., *Gates and Risers for Castings*, Penton Publ. Co., Cleveland, 293–294 (1935).
276. Galibourg, J., *Cong. Intnl. des Mines*, **6** (3) I, 173–179 (1930).
277. *Design of Welded Piping*, Linde Air Products Co., N. Y., 71–73 (1936).
278. *Industrial Gases*, **17**, 13–14 (1936).
279. Garriott, F. E., The Welding Journal, **17** (3) 12–16 (1938).
280. Waché, X., *Congrès de Chimie Industrielle*, **17** (2) 1083–1105 (1937).
281. Saunders, H. E., *Trans. Soc. Naval Arch. Marine Engrs.*, **37**, 133–153 (1929).
282. Pavlik, R., *Power*, **81**, 461 (1937).
283. Wilkinson, W. D., Jr., *Chem. Industries*, **42**, 153–156 (1938).
284. *Nickel Bulletin*, **11** (4) 76–78, April 1938.
285. Janovsky, F., *Trans. South African Inst. Elec. Engrs.*, **23**, 320–325 (1932).
286. Hansen, K. L., *Iron & Steel Engr.*, **7**, 113–116 (1930).
287. Ullmann, E., *Glückauf*, **72**, 407–408 (1936).
288. Friedrich, W., *Dauerfestigkeitsuntersuchungen an ungeschweisstem und geschweisstem rostsicheren 18-8 Cr-Ni-Ta-Stahl, Dr.-Ing. Diss. T. H.* Berlin, Druck Studentwerk, Charlottenburg, 1937; Hoffmann, W., and Friedrich W., *Elektroschweg.*, **9**, 91–95 (1938).
289. Schoenmaker, P., *Smit Laschtijdschrift*, **1** (6) 256–262 (1937).
290. Sakally, M., *Sudoztrojenye*, **7**, 663–666 (1937).
291. Duma, J. A., *Blast Furnace & Steel Plant*, **26**, 823–828, 846 (1938).
292. Atsuki, K., *Japan Nickel Rev.*, **6**, 247–261 (1938).
293. Schiffler, H. J., and Baerlecken, E., *Chem. Fabrik*, **11**, 385–390 (1938).
294. *Z. Schweisstech.*, **29** (1) 20–21, Jan. 1939.
295. *Z. Schweisstech.*, **29** (1) 23–24, Jan. 1939.
296. Franks, R., *Effects of Special Alloy Additions to Stainless Steels*; Amer. Soc. Metals, 1938 Preprint **6**, 17 pp.
297. Hess, W. F., and Ringer, R. L., The Welding Journal, **17** (10) Suppl., 39–48 (1938).
298. Monypenny, J. H. G., "Corrosion Resisting Steels for Chemical Plant." Unrevised Proof, Society of Chemical Industry, March 23, 1938.
299. Raymond, E. J., *Australian Chem. Inst., Jnl. & Proc.*, **5**, 265–267 (1938).
300. Hostettler, L. W., *Book of Stainless Steels, Second Edn.*, 135–145 (1935).
301. Parmiter, O. K., *Book of Stainless Steels, Second Edn.*, 278–292 (1935).
302. Wallace, R. M., American Welding Society Journal, **18** (1) 17–26, Jan. 1939.

SUGGESTED RESEARCH PROBLEMS

1. The recovery of alloying elements in stainless rods during different welding processes should be studied from the viewpoint of origin of loss. Slag metal reactions, about which nothing appears to be known, probably determine the loss more than any other factor. The quantitative aspects of recovery have been neglected.

2. What function is fulfilled by manganese in 18–8 welding rods?

3. There is scanty information about the corrosion fatigue strength of welded 18–8 and other stainless steels and irons in the corrosive conditions to which they often are subjected in service. Corrosion fatigue tests should be conducted on polished weld metal and unmachined welded joints.

4. It has been said that columbium and titanium reduce porosity in welding stainless iron. Do these elements exert a deoxidizing effect or do they affect gas solubility, arc temperature, rate of deposition or other factors?

5. There is evidence to show that the modulus of elasticity of weld metal deposited by austenitic chromium-nickel stainless steel rods, for example, 25 Cr, 20 Ni, 1.5 Mn, 0.10 C, is 20% lower than that of the rod itself. It has been said that the low modulus of the weld metal is due to its dendritic structure and strained condition. Whether low Young's Modulus is a characteristic of austenitic steel weld metal has not been determined nor has the effect of heat treatment on the modulus been studied.

6. There is no information on the effect of copper on the welding of stainless steels. For example, a steel with 0.30 C, 20 Cr, 1 Cu is said to be free from rapid grain growth and embrittlement on prolonged exposure above 650° C. Addition of 1% Cu and 1% Si to 12–16 Cr iron is said to result in unusually sound castings and to increase the static and impact strength of wrought products.

7. What effect does sulphur and selenium exert upon the welding of stainless steels?

8. Corrosion tests should be made on welded 18 Cr–8 Mn alloy.

9. A study should be made of the factors affecting porosity in stainless alloy deposits. Some of the factors appear to be arc length, speed of welding (puddling), fluxes, position of rod in flame and slag from preceding bead. The method of depositing a bead on the surface of a plate and examining the deposit for quantity and type of blow-hole and inclusion might be applied.

10. Practically nothing is known about the magnitude of shrinkage stresses in welded stainless steels or about the effect of shrinkage stresses on service behavior of welded stainless steel parts.

11. Besides existing as an impurity up to as high as 0.30% in stainless steel weld metal, nitrogen often is added as an alloying element. A detailed metallurgical study of nitrogen in welded 18–8 (heat-affected zone and weld metal) is needed.

12. The gain in ductility through heat treatment of welded 18 Cr iron has not been explained.

13. The corrosion characteristics, composition, ductility, and structure 18–8 welded by the carbon arc process under favorable and unfavorable circumstances should be investigated.

PHYSICAL PROPERTIES OF THE Iron-Nickel Alloys Formed in the Welding of Nickel-Clad Steel*

By W. G. THEISINGER† and F. G. FLOCKE‡

THIS paper is presented by subcommittee VII, Nickel Alloys section of the Engineering Foundation, Industrial Welding Research Division, being a result of certain research work carried out by a group within the subcommittee.

Before the advent of nickel-clad steel plate, the chemical and allied industries, wishing to obtain a vessel with a nickel interior, had two alternatives open to them: either to build the entire equipment of solid plate nickel or to build it of steel and line it with thin sheet nickel. The first plan was sometimes ruled out on the grounds of economy and, while the second had certain serious economic and structural shortcomings, due primarily to the use of very thin sheet, it was used nevertheless.

The matter was brought to a head in 1930 when the caustic industry inquired into the practicability of a nickel-lined railroad tank car. Since loose linings would obviously be outlawed, attention was given to the production of a bimetal in which one surface of a steel plate was clad with pure nickel and which, when fabricated into a vessel or container would in effect be a steel shell with a lining of nickel, firmly and uniformly bonded to the steel base plate.

The first nickel-clad plate, produced on a commercial scale in the summer of 1930, was used in the fabrication of an 8000 gallon riveted and caulk-welded railroad tank car. Since then its production and adaptability has increased steadily and nickel-clad steel has become a commercial material for welded fabrication.

The Nickel Alloy Committee has sought to gather the current technical data on nickel-clad steel in this paper and to present new information. Of recent interest has been the result of tests made to show the strength of the bond between the cladding and the base plate and a study on the effects of proper and improper butt joint set-up.

The process employed in the making of nickel-clad steel involves the preparation of nickel and steel plates to make up a composite slab which is heated and rolled

Fig. 1—Four High Plate Arrangement for Nickel-Clad Steel Prior to Heating and Hot Rolling

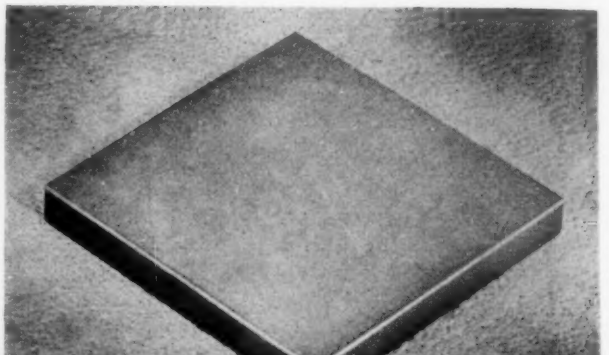


Fig. 2—The Nickel-Clad Steel Plate Cross Section

in a conventional plate mill. A thick steel slab has laid upon its cleaned top surface a plate of clean hot-rolled nickel which is held in position by welding. Two such slabs are prepared and joined, as shown in Fig. 1. They are charged in the heating furnace in this "sandwich" form. The high pressure and temperature used in rolling the built-up slab to plate form creates a bond between the nickel and the steel. The nickel-clad steel cross section illustrated in Fig. 2, shows a uniform cladding of pure nickel bonded to a flange quality steel base plate.

The actual strength of the bond between nickel and steel, an iron-nickel alloy shown in the photomicrograph, Fig. 3, has only recently been investigated. Tensile tests of the bimetallic plate made in the usual way in which the direction of pull was parallel to the thickness and the bonded area showed that the cladding did increase the strength over the base steel plate approximately 10%. However, this did not fully answer the questions regarding the tensile strength of the bond itself when the stress would be applied normal to the plate surface in an attempt to pull the cladding from the steel.

Figure 4 shows the method used to carry out such a strength test. A bar of nickel was arc welded with a nickel electrode to the nickel cladding and in like manner a steel bar was joined to the steel side of the plate of the 1 inch thick, 20% nickel-clad steel sample using a steel electrode. Several specimens so prepared disclosed strengths of the 0.505-inch diameter specimens shown at the bottom of Fig. 4 as between 50,000 and 52,000 psi with fractures in the steel weld. Neither the nickel weld, nor the bond showed any signs of failure. Another similar specimen was made, but the nickel and steel

* Presented at the Annual Meeting A. W. S., Detroit, October. Contribution to the Industrial Research Division, Welding Research Committee.

† Lukens Steel Co.

‡ International Nickel Co.

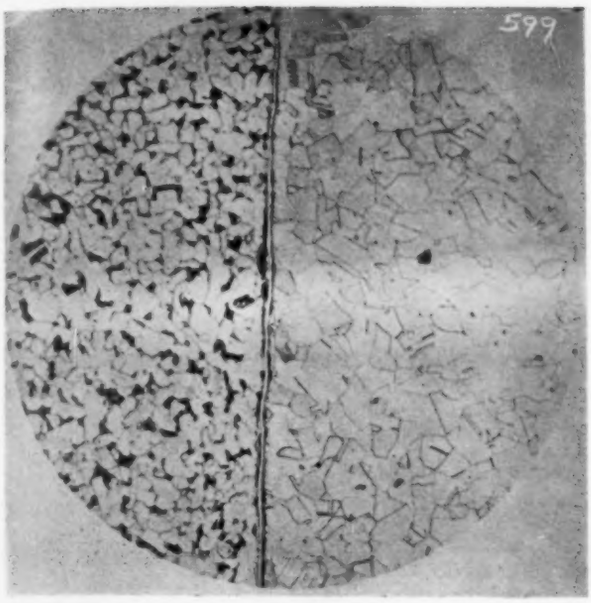


Fig. 3—The Nickel-Clad Steel Cross Section at the Bond at 100 Magnifications

welds joining the bars to the nickel-clad steel were heavily reinforced. This specimen broke in the steel base plate at 55,500 psi without any separation of the nickel cladding from the steel.

Confirmation of this bond strength was obtained by a large user of nickel-clad steel in a series of elaborate tests in which the nickel-clad steel bond was subjected to a direct tensile stress at right angles to the plane of the bond. Again, as in the previous specimens, rods were welded to the nickel and steel surfaces for gripping the specimen in the tensile machine, but the welded specimens were not machined to a 0.505-inch diameter. Weld cross sections were so designed that failure was to occur in the bimetal plate. In all cases, the fractures were through the steel base plate, remote from the nickel-steel interface and not in any of the welds which held the grip rods to the clad specimens. The computed strengths in all cases fell within the range of the physical requirements for flange quality steel plate, viz.: 55,000 to 65,000 psi, demonstrating that the strength of the bond between the nickel cladding and the steel plate is greater than the strength of the steel itself.

The second part of this paper deals with the fit-up of butt welds in nickel-clad steel and the effect of amperage on the several types of fit-up. The procedure used in butt-welded fabrication of this material is illustrated in the four steps shown in Fig. 6.

Briefly, it involves a procedure which simulates that used in paragraph U-68, A. S. M. E. Code fabrication by welding from one side and back chipping, followed by the deposition of one or more sealing passes. In the case of nickel-clad, the steel weld is made first, as shown in (1) and (2) of Fig. 6. Back chipping is done with a round nose gouge and not a diamond point chisel, care being taken to remove all unwelded material. Unnecessarily deep chipping from the nickel side of the joint should be avoided in order to keep the nickel bead as shallow as possible. In depositing the sealing passes nickel electrodes of course are substituted for the steel, as seen in (3) and (4) of Fig. 6.

In the authors' opinion, the quality of the first steel pass depends upon the exactness and care with which it has been deposited. The desired result is reached when the bottom of the first steel pass is just contacting the nickel-steel interface. If the steel bead is incompletely penetrated, an excessively deep groove must of necessity be chipped with the result that an excess of iron is picked up during the deposition of the nickel weld. If, on the contrary, too much penetration is obtained so that the steel weld metal actually penetrates beyond the outer nickel surface then some difficulty may be experienced because the iron-nickel alloy thus formed may be as hard as the chipping tool. That an approach to the optimum is, of course, commercially practical is attested to by the satisfactory fabrication and service record for the many tons of equipment in use.

When viewed by a chemical engineer, the contamination of the nickel weld by iron is uppermost, since interest is centered in a low iron content in the nickel weld and the strength of the steel weld assumed as satisfactory. When viewed by the steel producer, fabricator or user, the possible effects of the nickel cladding on the quality of the steel weld becomes a matter of first importance, with the making of the nickel weld secondary.

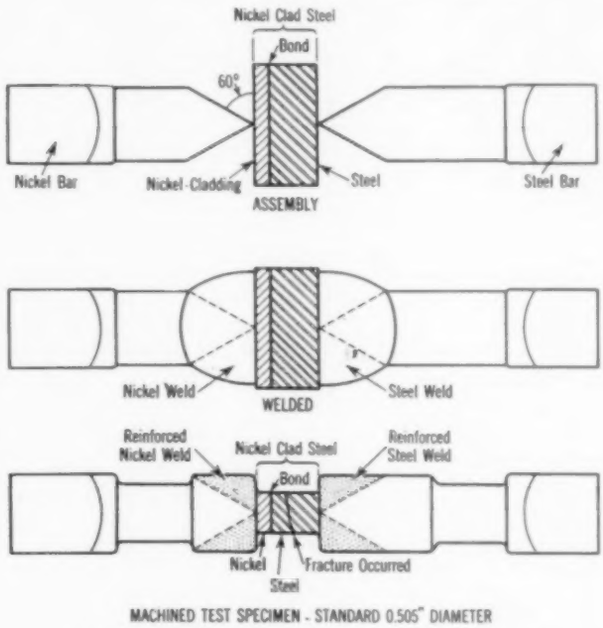


Fig. 4—Method Employed in Making the Bond Test

Since the deposition of the first steel pass is so significant, the variables such as bevel, heat, joint offset, cladding and plate thicknesses were studied and their effects determined by a hardness survey of the cross section of the weld.

A preliminary survey was made by making joints 12 inches long—welding two pieces of 6 x 12-inch nickel-clad steel plate in an effort to lay the groundwork for a more extended test program. It was found, for example, that the first steel pass would be harder if nickel had been picked up. This pick-up in turn was dependent upon the heat of welding and the degree of offset of the two plates at the joint. Figure 7 is a photograph of the preliminary set in which the plates were offset a prede-

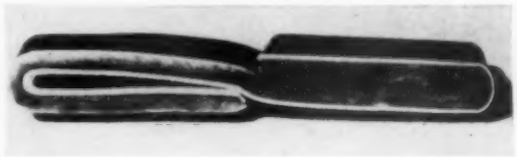


Fig. 5—Bend Test to Show the Completeness and Uniformity of the Bond

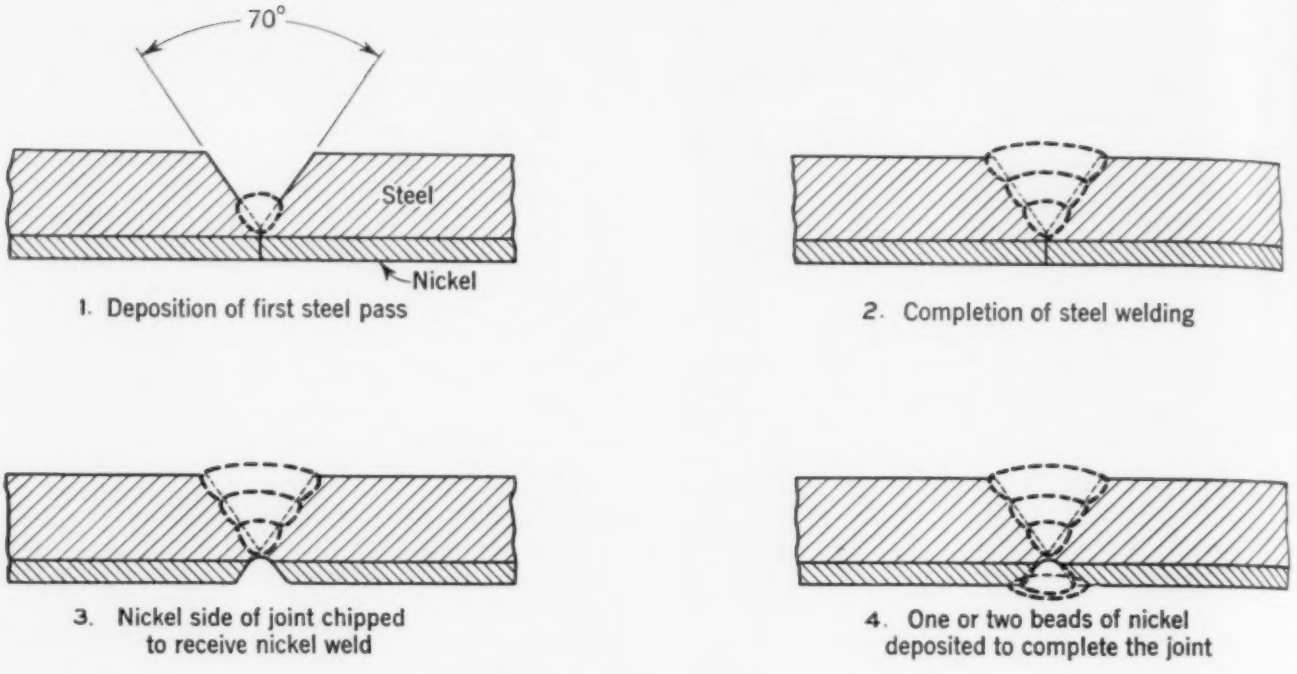

70°
Steel
Nickel
1. Deposition of first steel pass
2. Completion of steel welding
3. Nickel side of joint chipped to receive nickel weld
4. One or two beads of nickel deposited to complete the joint

Fig. 6—Welding Sequence for Nickel-Clad Steel

termined amount to exaggerate conditions sometimes found in vessels where joint fit-up is not of the best. The pick-up of nickel under extreme conditions of poor fit-up can be seen in the specimens in the right column, third and fourth from the bottom, and is recognized easily by the crescent-shaped area where the steel weld has penetrated into the nickel-cladding.

As a result of this survey, two types of single-vee bevel were used, varying only in the straight portion or lip at the base of the vee. In the one case the plate was beveled not quite to a feather edge, but with only the cladding thickness left unbeveled. In the second case, the lip included the cladding thickness plus $^{1}/_{16}$ inch of the steel. A total included angle of 90° on the steel side was used.

A good quality shielded-arc steel electrode $^{5}/_{32}$ inch diameter was employed for all steel welding under two definite sets of conditions, namely, a low current of 135 amp. and a high amperage of 185 amp. Both were extremes of the current range for this $^{5}/_{32}$-inch electrode.

Although the preferred method of joint fit-up is to have the surfaces in line or flush, there are times when conditions do not permit this, and the plates comprising a joint are offset, an obviously undesirable practice, yet sometimes unavoidable. Therefore, both flush and offset joints were included in this research in order to study their effect on the completed welds.

Feeling that perhaps the cladding thickness would have a bearing on the results, two percentages of cladding were used. Nickel-clad steel is described in terms of total plate thickness and the cladding given as a percentage of this total thickness. The material included in both preliminary and final tests was:

$^{3}/_{8}$ in. 10% cladding—nickel thickness 0.037 in.
$^{3}/_{8}$ in. 20% cladding—nickel thickness 0.075 in.

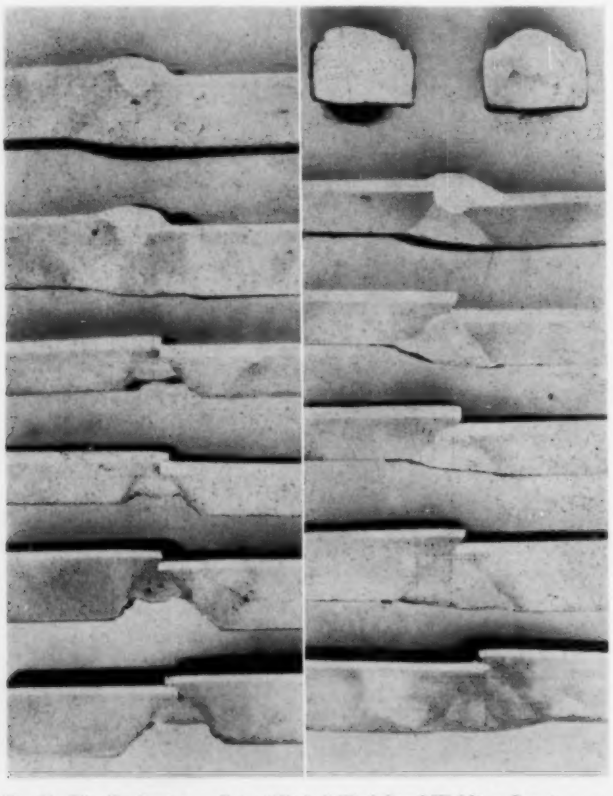


Fig. 7—The Preliminary Set of Nickel-Clad Steel Welding Specimens

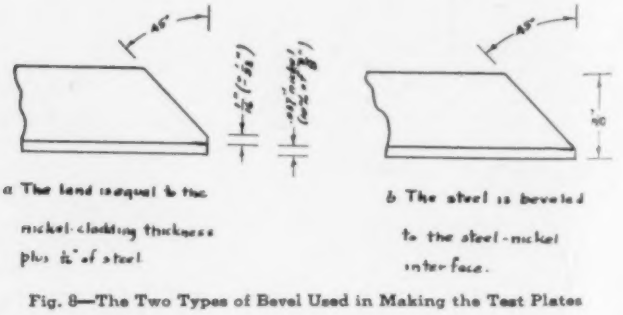

45°
45°
a The land is equal to the nickel-cladding thickness plus $^{1}/_{16}$" of steel
b The steel is beveled to the steel-nickel interface.

Fig. 8—The Two Types of Bevel Used in Making the Test Plates

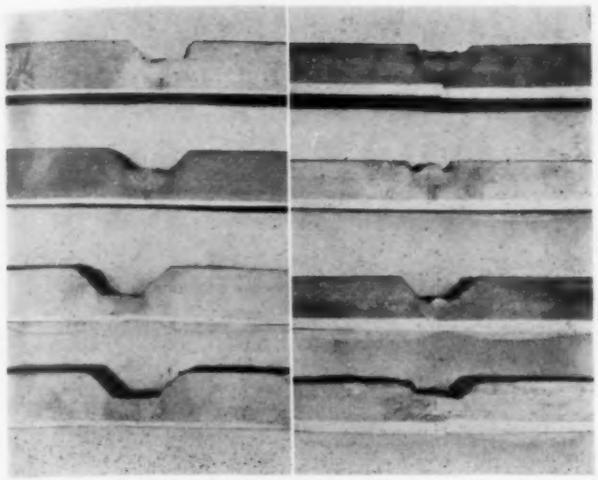


Fig. 9—The Test Specimens Set Up So That the Nickel Surfaces Were Flush Specimens 1–8

Specimens were made in duplicate and examined, but in order to avoid duplication and perhaps confusion in presentation, a single specimen, representative of each condition is submitted in the several figures which follow.

All welds were sectioned, ground, polished and lightly etched with 2% Nital to bring out the weld structure and the contour of the weld in relation to the nickel cladding. The hardness survey was made on a Vickers machine with a 10 kilogram load and a diamond penetrator.

The steel base plate hardness was of the order of 138 Vickers (an average of 30 readings), whereas the hardness of the nickel cladding averaged 151 for the same number of impressions. The hardness of the steel weld deposits free of nickel pick-up was 200 Vickers, but in those welds were the nickel was taken into solution locally, the hardness jumped to over 300 Vickers. It is self-evident that there was no question of judgment involved in deciding whether or not there was dilution in the steel weld.

To avoid an incorrect impression being gained regarding the increases in hardness effected by the dilution of steel with nickel, it should be noted that this condition is peculiar to the present experiment only, and in no way reflects upon the uniformity of nickel steels or nickel alloys in general.

In Fig. 9 will be seen the beginning of the second series, in which all abutting plates are flush. Although all eight

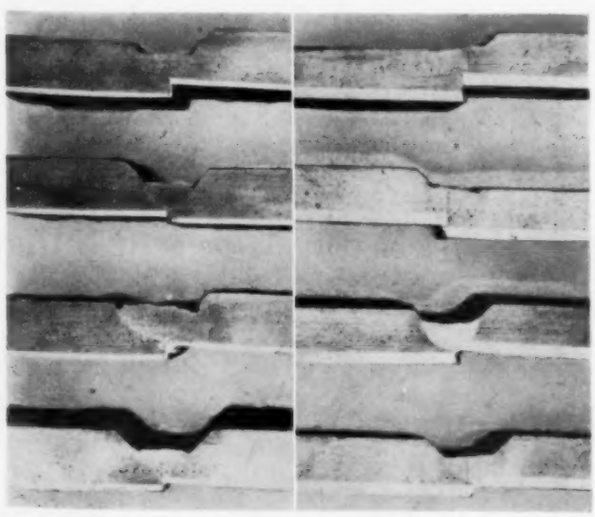


Fig. 10—Specimens Set Up So That the Two Plates Comprising the Butt Joint Were Offset. Specimens 9–16

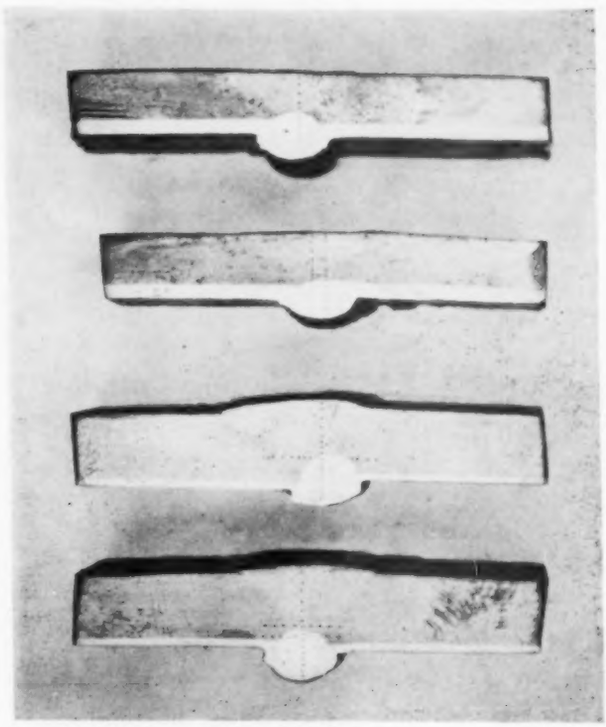


Fig. 11—Completely Welded Nickel-Clad Steel Specimens. Specimens 17–20

specimens have an over-all thickness of $^3/_8$ inch the difference in cladding thickness, i.e., between the 10% and 20% clad, is immediately recognizable. Those specimens in the lower half of the figure had the steel bevelled as shown in Fig. 8*b*, whereas the four specimens in the upper half can be seen to have a definite lip, as shown in Fig. 8*a*.

Specimens 1 and 2 were welded with low current (135 amp.) and high current (185 amp.), respectively, and neither the visual examination nor the hardness survey revealed any increase in hardness of the steel weld. In the case of specimen No. 1, the Vickers hardnesses of the steel plate were 134, 139 and of the nickel cladding 140, 143. Following the horizontal line from left to right the Vickers values are 153, 164, 174, 193, 190, 166, 155, 155, while on the vertical line from top to bottom

Table 1—Hardness Values Obtained on Specimens Shown in Figs. 9, 10 and 11

Spec. No.	Vickers	Amp.	Bevel	Clad
		Flush Fit		
1	200	135	..	10%
2	203	185	..	"
3	215	135	lip	"
4	215	185	lip	"
5	193	135	..	20%
6	203	185	..	"
7	212	135	lip	"
8	185	185	lip	"
		Offset Fit		
9	227	135	..	10%
10	390	185	..	"
11	212	135	lip	"
12	209	185	lip	"
13	376	135	..	20%
14	376	185	..	"
15	193	135	lip	"
16	181	185	lip	"

the determinations were 190, 190, 190, 200, 187, 187, 120, the latter reading being in the cladding.

Again in specimens 5 and 6, with the steel bevelled as in 8*b*, that is, without any steel lip, neither high nor low amperage welds penetrated into the nickel cladding.

Where the lip section includes some steel in addition to the thickness of the nickel cladding, neither high nor low amperages penetrated to the nickel-steel interface; in fact, a bit of the original machined edge is still visible. The hardness survey of course revealed no wide variations from normal values for either the steel plate, weld or cladding.

Where the joints were flush, as in Fig. 9, neither 135 nor 185 amp. penetrated beyond the nickel-steel interface and the conclusion can be drawn that either type of bevel (Fig. 8) may be used on plate of $^3/_8$ inch thickness with 10 or 20% cladding without any hardening effect of the deposited metal.

Occasionally butt joints in tanks are found to be offset so that the inside surfaces of the tank are not absolutely flush. In the following series the plate offset was exaggerated in the extreme, in an endeavor to obtain the worst possible condition.

Figure 10 illustrates the type of specimen prepared and the amount of offset on both the 10 and 20% clad materials. Those specimens in the lower half of the figure have a lip of nickel-cladding only, Fig. 8*b*, whereas those

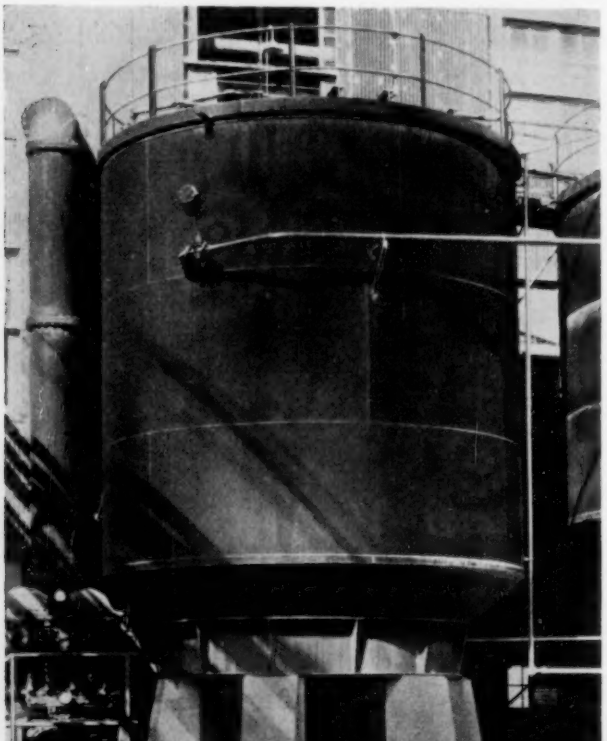


Fig. 12—Nickel-Clad Steel Caustic Settling Tank, 18 Ft. Diam. x 25 Ft. High, Built in 1934

Fig. 13—Nickel-Clad Steel Railroad Tank Car for the Shipping of Phenol. Nickel Coils in the Bottom of the Tank Are Used to Preheat the Liquid Cargo

specimens in the upper half have the $^1/_{16}$ to $^3/_{32}$ inch of steel in addition to the thickness of the nickel. Single pass steel welds were deposited at the high or low-current values as described earlier (135 and 185 amperes).

Where the nickel cladding is exposed in the vee so that the steel weld has an opportunity of penetrating to the nickel and so infuse it into the steel deposit, an increase in the hardness of this weld metal was found. For example in the four specimens with the nickel edge bevel (Nos. 9, 10, 13, 14) both high and low amperages permitted the steel weld to penetrate to the cladding, in fact, in No. 14, the entire steel weld has a hardness of over 300 Vickers.

Where sufficient steel lip was provided, and the steel deposit made at low amperage (No. 11), there was not enough heat to melt down to the nickel. However, with specimen No. 12, the high, 185-amp. current coupled with an excessive offset was sufficient to penetrate into the nickel locally, with resultant high hardness at a definite point in the bottom only.

Even though there was a 20% offset, the cladding thickness was the measure of the offset, neither low nor high amperage penetrated down to the cladding in specimens 15 or 16.

The findings of the original set of specimens and the 16 duplicate sets just described confirmed field practice. On the basis of these, four further plates were completely welded up in which the joint preparation embodied the facts brought out by the earlier portion of investigation. The specimens in Fig. 11, representative of the last set of completely welded plates had no localized hard spots.

The conclusion which is drawn from the hardness surveys made is that with proper joint design—a single-vee bevel with a straight lip of approximately $^1/_{16}$ inch plus the cladding thickness—good joints should result if a good welding technique is used. Although this is based on work on $^3/_8$-inch clad plate, the fundamental principle of providing a lip of steel in addition to the cladding will also hold for thicker plate.

THE ENGINEERING FOUNDATION

Welding Research Committee

Sponsored by the American Welding Society « « » » and American Institute of Electrical Engineers

Supplement to the Journal of the American Welding Society, April 1939

DATA ON THE SPOT WELDING OF Low Carbon Steel

By WENDELL F. HESS, D.Eng. and ROBERT L. RINGER, JR., E. E.,

INTRODUCTION

THIS paper is a further report on the program of resistance welding research sponsored by the Welding Research Committee[1] of Engineering Foundation at the Rensselaer Polytechnic Institute welding laboratory. The previous paper[2] presented contact resistance and strength data for stainless steel. Similar data is here presented for 0.029-inch hot rolled, annealed and pickled mild steel. Methods of test and microscopic examination of this material were included in the previous paper.

STRENGTH AND INDENTATION

A point was made in our previous paper that the best welds, made with just sufficient energy, do not have serious indentation even with high pressure. In fact, it seems apparent that the indentation produced is a good indication as to the proper value of welding current to be used with particular values of electrode pressure and time. This result is well illustrated in Fig. 1, which is plotted for the optimum conditions of pressure, time and curtent for 0.029-inch hot rolled, annealed and pickled mild steel. The electrode tips were carefully aligned for flat, uniform contact over their entire surfaces, as in the previous work. With mild steel the electrode pressures may properly be of the order of 15,000 psi as contrasted with 50,000 psi or more which was found desirable in the case of stainless steel. Reference to the tensile strength curve in Fig. 1 shows that the tensile strength falls off only slightly when the current is excessive and expulsion of metal occurs. This decrease in tensile strength is thus not so sharp a criterion of an excessive current. Reference to the indentation curve on the other hand shows a very marked increase as soon as the current becomes excessive.

It will be recalled that in the earlier paper the excessive current gave rise to marked differences in the microstructure of the weld such as grain coarsening, cavitation, shrinkage cracks, distortion and too great penetration of the fused zone. In the mild steel welds the tension-on-a-lap-weld strength falls off a little and then may rise slightly, beyond the optimum value of current and thus is not a clear indication of the quality of the weld. The microstructure and even more simply the indentation are clearly indicative of the optimum value of current. Thus, for about 13,500 amperes we have an indentation of only about 2 per cent whereas at 14,000 amperes the indentation has jumped to 10 per cent. The optimum current thus becomes 13,500 amperes for $^1/_4$-inch spot welds in this material, and the strength of a single spot lap weld is slightly under 1000 pounds.

All of the welds in this series were carefully cross-sectioned across their diameters. These were polished, etched and their diameters measured by means of an optical comparator. It will be noticed that at the optimum value of current, the full size of the tip diameter, in this case $^1/_4$ inch, is just reached, after which a further increase in current does not increase the size of the spot, but seriously injures its metallurgical structure and markedly increases the indentation. Thus, the size of the best welds in this thickness of material is the same as the size of the electrode tip.

Figure 2 illustrates a typical series of strength curves as a function of current for different values of pressure and time. It will be seen that at 15,000 psi it is possible to obtain a very slightly higher strength in 12 cycles using about 12,000 amperes. These conditions represent almost equally satisfactory welding conditions to those shown in Fig. 1. However, this requires a longer weld cycle, which may or may not be undesirable. Times longer than 12 cycles are definitely less satisfactory since they show no improvement or an injury to the properties of the weld. A time of 4 cycles or less gives lower values of strength than obtainable at 6 cycles.

Pressures lower than 15,000 psi permit metal to be expelled at lower values of current and thus give rise to lower strength welds. Figure 3 shows a series of curves for the same conditions illustrated in Fig. 2 except that per cent indentation is here plotted as a function of current. This figure strikingly illustrates the effect of raising the pressure from 15,000 psi to 30,000 psi. Here the indentation jumps from about 6 per cent at 14,000 amperes to approximately 25 per cent at 14,500 amperes, as compared to an increase from 2 per cent to 10 per cent at the lower pressure and for the same $3^1/_2$ per cent change in weld current. Reference to Fig. 2 shows that 30,000 psi prevents expulsion of metal until more than 14,000 amperes is used and gives a weld that is 5 per cent

*Associate Professor of Metallurgical Engineering and Head of the Welding Laboratory, Rensselaer Polytechnic Institute, Troy, New York.

** Research Fellow, Department of Metallurgical Engineering, Rensselaer Polytechnic Institute, Troy, New York.

[1] Under joint auspices of the Fundamental and Industrial Research Divisions.

[2] "Studies of the Spot Welding of Low Carbon and Stainless Steels," W. F. Hess and R. L. Ringer, Jr., Welding Research Supplement, October 1938, pp. 39–48.

Fig. 1—Data from Optimum Welds, Made in 6 Cycles with 1/4-Inch Diameter Tips and 15,000 Psi Electrode Pressure

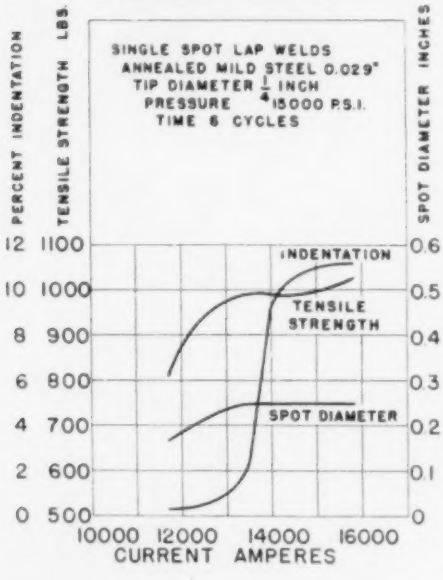


Fig. 2—Tensile Strength of Single Spot Lap Welds as a Function of Current for 1/4-Inch Tip Diameter Spot Welds

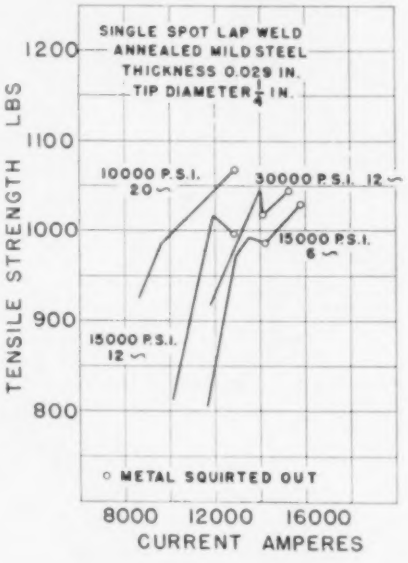
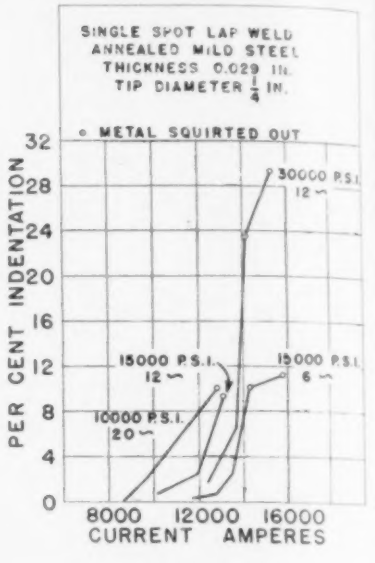


Fig. 3—Indentation as a Function of Current for 1/4-Inch Tip Diameter Spot Welds



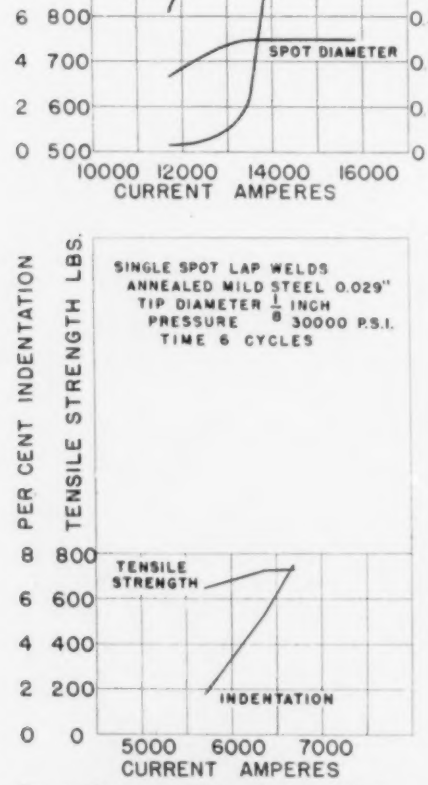


Fig. 4—Data from Welds Made in 6 Cycles with 1/8-Inch Diameter Tips and 30,000 Psi Electrode Pressure

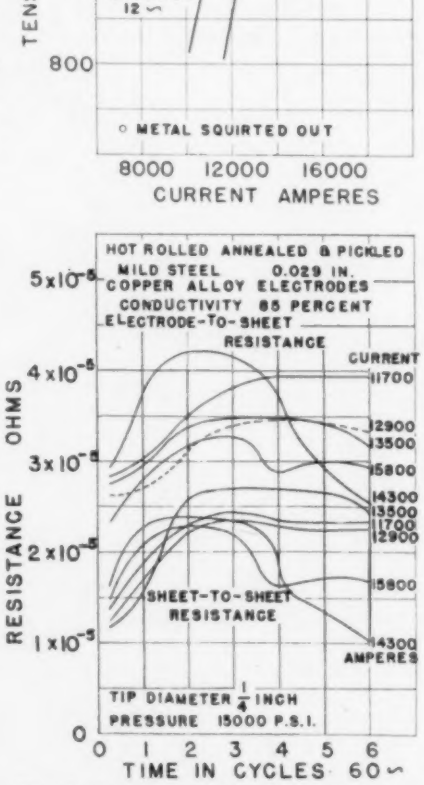


Fig. 5—Contact Resistances for Optimum Spot Welds, Made in 6 Cycles with 1/4-Inch Diameter Tips and 15,000 Psi Electrode Pressure

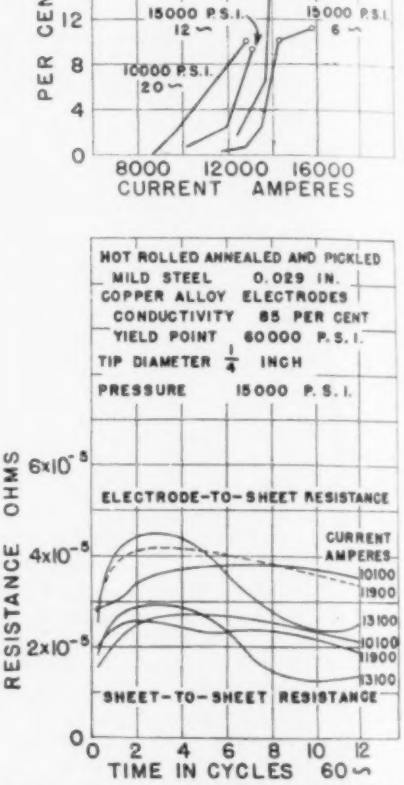


Fig. 6—Contact Resistances for Spot Welds Made in 12 Cycles, with 1/4-Inch Diameter Tips and 15,000 Psi Electrode Pressure

stronger than the best welds made at 15,000 psi using 13,500 amperes. This slight increase in strength is not warranted in view of the fact that a slight excess over the maximum value of current gives rise to much more serious indentation with the higher pressure.

Figure 4 illustrates the tendency for marked increase in indentation with current as compared to the change in tensile strength over the same range of current, for welds made with 1/8-inch flat tips. Note that the optimum currents are about half those for quarter inch spots which agrees with the results for stainless steel, that the current is proportional to the diameter.

CONTACT RESISTANCES

A series of measurements of contact resistance as a function of time during the making of actual welds was made for a variety of conditions discussed in the above paragraphs. There is a very marked difference in the results obtained with the mild steel as compared to the contact resistances obtained in the earlier paper for stainless steel. In the case of stainless steel the contact resistances from sheet-to-sheet are approximately three times as high as those from electrode-to-sheet. In the case of mild steel, the reverse is true, namely that the electrode-to-sheet resistances are one and one-half or two times higher than the sheet-to-sheet resistances. The sheet-to-sheet resistances of mild and stainless steel compare very closely, when contact resistances are considered to be a function of the resistivity of the material and proper allowance is made for the difference in pressure.

Figures 5 to 9 show contact resistances measured under the same conditions of time, pressure and current as used in discussing strength and indentation. These families of curves show similar characteristics to those obtained for stainless steel. The best welds under these various conditions were made with current values of 13,500, 11,900, 9700, 6700 and 14,000 amperes, respectively. Note that with these values of current the resistances first rose with temperature and then fell off *gradually* with increased fusion and intimacy of contact.

With lower values of current the resistance rose less rapidly and fell off less than for the best welds. With excessive currents causing expulsion of metal, the rise in resistance was generally steeper and at some time during the weld cycle there was a sudden decrease in resistance accompanying the expulsion of metal. The sharpness of the decline was less for slightly excessive currents than for more excessive values. Note also that the cycle during which expulsion of metal took place is indicated on these graphs and, as is to be expected, is earlier for more excessive than for less excessive currents. In Fig. 5 expulsion took place during the latter half of the fourth cycle with 15,800 amperes and commenced during the first half of the fifth cycle with 14,300 amperes.

In Fig. 7, the very excessive current of 12,800 amperes caused a double expulsion of metal, after $1^1/_2$ and 4 cycles. Metal was not expelled from any of the welds for which data are shown in Fig. 8. In Fig. 9, 15,300 amperes produced an expulsion of metal after 8 cycles. It is of interest to compare this curve taken at an electrode pressure of 30,000 psi with the 15,800 ampere curve of Fig. 5 taken at 15,000 psi where expulsion took place after $3^1/_2$ cycles.

Figures 5 and 6 show that the best welds made in 6 and 12 cycles with a tip diameter of $^1/_4$ inch and an electrode pressure of 15,000 psi should be made with currents of 13,500 and 11,900 amperes, respectively. They also enable us to predict time values which might have been used for best results with certain other values of current for which curves of contact resistance are shown. For example, in Fig. 5, 14,300 amperes would have made a better weld in $3^1/_2$ cycles. By extending the 12,900 ampere curve it would appear that this value of current would make its best weld in about $7^1/_2$ cycles. Reference to Fig. 6 shows that 11,900 amperes made an optimum weld in 12 cycles. This figure also indicates that 13,100 amperes would have made an optimum weld in a little less than 7 cycles. Plotting the above values of time in cycles to make the best weld with various currents at constant pressure will result in a smooth curve, relating time and current for a definite pressure. It should be possible to make a family of these curves for different pressures. The curve suggested above is not here reproduced since it is desired to do more work of this character and to check results before presenting data in this form of graph.

RESULTS AND CONCLUSIONS

1. The optimum pressure for welding 0.029-inch hot rolled annealed and pickled mild steel is 15,000 psi.

2. The optimum time for the above thickness of material is from 6 to 12 cycles and the currents corresponding with these are 13,500 and 12,000 amperes, respectively.

3. With flat electrodes the indentation produced in welding is a clear indication of the dividing line between proper and excessive values of current, there being a marked increase in indentation immediately above the optimum value of current.

4. For this thickness of material, the diameter of the optimum weld and of those made with excessive currents is the same as the diameter of the flat electrode tip.

5. The shape of the contact resistance curves is an indication of the quality of the weld. For the optimum values of current there is always the same general character of curve, while excessive values of current are clearly indicated.

6. The electrode-to-sheet resistances are one and one-half or two times higher than the sheet-to-sheet resistances in contrast to the case of polished stainless steel where the ratio is approximately doubled and reversed.

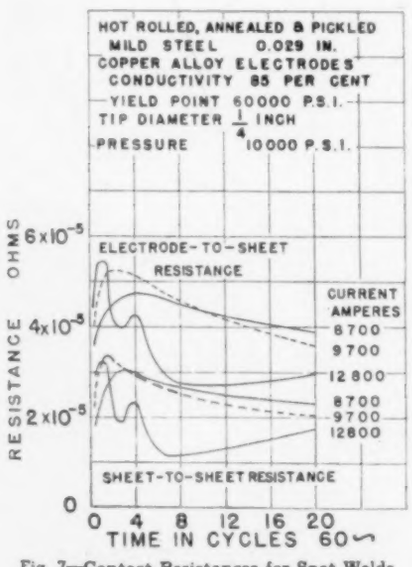


Fig. 7—Contact Resistances for Spot Welds Made in 20 Cycles with $^1/_4$-Inch Diameter Tips and 10,000 Psi Electrode Pressure

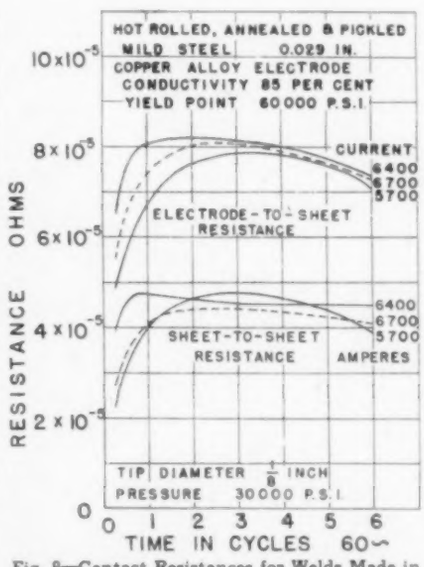


Fig. 8—Contact Resistances for Welds Made in 6 Cycles, with $^1/_4$-Inch Diameter Tips and 30,000 Psi Electrode Pressure

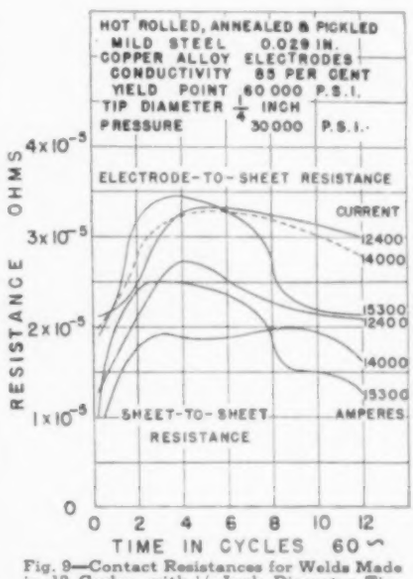


Fig. 9—Contact Resistances for Welds Made in 12 Cycles, with $^1/_4$-Inch Diameter Tips and 30,000 Psi Electrode Pressure

Report on the Activities of

RESISTANCE WELDING COMMITTEE

of Industrial Research Division

THE Resistance Welding Committee was organized in the Spring of 1938 and has since met twice; once in June in Atlantic City, and once in Detroit in October. In addition there have been several meetings of the subcommittees to which the Committee has assigned some of its duties.

The efforts of the Committee have to date been largely confined to the study of the fundamentals involved in Resistance Welding. It has been considered necessary, before embarking on any advanced research, to define, standardize and determine methods of procedure as well as all factors, variables and requisites involved, in order that comparable results may be obtained in future by independent researchers. A great deal of the existing data and past research has been found to be useless because of lack of uniformity or completness in recording conditions or methods of procedure and of the resulting performance.

It has been decided that the first fundamental to be considered is the method of determining the quality and performance characteristics of spotwelds. A subcommittee has been assigned to this task and after a preliminary investigation submitted the following report:

REPORT OF SUBCOMMITTEE ON SPOT WELD TESTING*

The subcommittee made a careful analysis of various methods of spot weld testing. The importance of agreeing upon methods of testing spot welds is based upon:

1. The demand for data on the strength of spot welds by designers and users of welding.
2. The need for placing the results of research programs upon a common basis for comparison.
3. The fact that the results of tests is to a greater or less degree dependent upon the design of the test specimen.

In arriving at its recommendations the committee was guided by the following considerations, with respect to an acceptable standard test for spot welds:

1. Make it simple—so that people will use it.
2. Make it sensitive—to small changes in welding conditions.
3. Make it conform to standard test equipment, if possible.
4. Check it with more complicated tests for special circumstances.

A careful analysis was followed by a preliminary program of testing of spot welds, using several specimen designs, in tension on a lap weld. As a result of this study, it was decided that the best design of specimen for this type of test is a single spot lap weld. The committee feels that this test should properly be called "tension on a lap weld," instead of "shear test" as it is commonly called. Of all the tests used by investigators in this field, the single spot tension on a lap weld test best fulfils the principles stated above.

It is realized that the above test will not be sufficient for all types of material especially those of the air-hardening type. For these cases some sort of peel test will be necessary. An analysis of two-directional *vs.* four-directional peel tests indicates that the latter is likely to exaggerate the tendency of a material with high yield strength to fail in a brittle manner, because of the three-dimensional stress condition which is present. In a material with a low flow strength, the yielding of the metal will relieve the stress concentration, and thus give rise to a ductile failure.

The subcommittee feels that further study of the two peel tests above mentioned should be made to determine the sensitivity of these tests to slight errors in the form of the test specimen. Any slack in the fit of the sheet metal to the supporting test blocks, or any slack in the clamping arrangement should give rise to serious inconsistencies in results.

Referring again to the single spot tension on a lap weld test, it will be evident that for any given thickness of sheet, welded with the largest practicable diameter of spot, the width of the test specimen must be sufficient to insure failure in the zone around the weld rather than through the sheet. In other words, the sheet strength must exceed by a fair margin the strength of the spot weld. On the other hand, it will be desirable to keep the width of the specimen down to one inch wherever possible, to make the specimen conform to standard sheet metal testing grips which will handle specimens up to one inch in width. The above considerations make it obvious that a further experimental program will be necessary to develop the proper width of specimen for each sheet thickness.

Investigation of available tests shows that no test is at present available which simulates the most important service condition to which spot welds may be subject. This condition is the result of the tendency to buckle adjacent to or between individual spots in a row. It gives rise to a combined shear parallel to the sheets with tension at the edge of the weld, resulting in diagonal shear in the sheet at this point. The occurrence of this stress condition in transportation or aircraft structures is likely

* WENDELL F. HESS, *Chairman*, M. B. GATHMAN, M. F. SAYRE and A. M. UNGER.

to be frequently repeated, so that a fatigue test may be necessary.

To summarize, the subcommittee makes the following recommendations: 1. That a single spot tension on a lap weld test be accepted as a standard method of testing spot welds, the exact dimensions of test specimen to await the results of further investigation.

2. That further study be made for the purpose of comparing two-directional with four-directional peel tests, before making definite recommendations for a test of this type.

3. That an invention or development of a method of test, which simulates the most important service condition to which spot welds may be subject, be sought.

This report has been discussed to some length by the Committee and its recommendations considered satisfactory for preliminary work. It is realized that the tests proposed can be used only for determination of the properties and performances of individual spot welds and that special test procedure may have to be devised later to take into account special conditions encountered in fabrication processes such as proximity effect in multilayer welding. It is considered, however, that for the time being the important thing is to devise a fundamental test for the simplest condition possible, to be used as a standard in terms of which all the more complex conditions can be evaluated.

It will be seen from the subcommittee's report that considerable research is required to establish a comprehensive method of testing spot welds. The Committee has recommended that it be authorized to conduct this research and was granted this authority at the last meeting of the Executive Committee of the Industrial Division.

The Committee has next considered the matter of the relationship between the factors affecting resistance welds and the quality and performance of the resulting welds. It was decided that an investigation of the fundamental relationship, considering the most important factors only and using the most common materials, is first needed, to provide scientific and exact backing to what is generally believed to be true throughout the industry. A program of research necessary for such an investigation has been prepared as follows:

A—SPOTWELDING

Material

I. Full cold rolled, deep drawing auto-body steel, full anneal, mill finish, with carbon content not over 0.10%, thoroughly cleaned and degreased, chemical analysis and physical properties to be recorded. Five thicknesses to cover, in approximately equal steps, from 0.020 inch to 0.109 inch at the discretion of the investigator. Combination of two similar thicknesses to be considered first, with possibly some additional combinations of dissimilar thickness at the discretion of the investigator.

The investigator to determine:

1.—Optimum current density or range of densities for spots of given diameter, the range of diameters considered to cover the range practical for the thickness being investigated. Three or four weld time periods to be chosen by the investigator, to cover the range practical for the thickness being investigated, to be considered. 1 inch to $1^1/_4$ inch diameter, water-cooled electrodes with 4-inch radius dome to be used, if practical. The investigator may, however, at his discretion, use flat electrodes with point diameter equal to the desired spot diameter.

NOTE: By optimum is considered a value or range of values producing a spotweld possessing maximum strength for a given diameter and satisfactory appearance, as determined by standard test or tests.

2.—Optimum pressure density or range of densities for conditions used in 1.

3.—Effect on weld properties of different weld time periods, welds being made with optimum current and pressure densities as obtained or interpreted from the results obtained in 1 and 2.

4.—Effect of abnormally short dwell period on 1 and 2. Two dwell periods chosen at the discretion of the investigator to be considered.

NOTE: A "Dwell period" shall be considered the time elapsing between the cessation of the welding current and the cessation of the welding pressure. A normal dwell period shall be considered any dwell period in excess of 15 electrical cycles.

5.—Effect of inadequate electrode mass, or shape, or water cooling, on 1 and 2. For this purpose the following electrodes shall be used:

(*a*) Diameter = Spot weld diameter plus $^1/_{16}$ inch
Length at above Diameter = 2 × Diameter
End = Flat or 4-inch radius
Water-cooling = Up to $^3/_8$ inch–$^1/_2$ inch of tip, or as close as practical

NOTE: Five (5) welds should be made within three (3) seconds, the fifth weld only to be considered.

II. Similar to (I) except using hot-rolled pickled steel. The extent and details of this investigation to be left to the discretion of the investigator, depending on the extent of difference between the two grades

III. Similar to (I) except 18-8 stainless steel 90 to 100,000 psi tensile and 140,000 to 175,000 psi tensile.

IV. Similar to (I) except 52-S and 53-S aluminum. (This investigation to be coordinated with the work being done by the Aluminum Company of America. In so far as a great deal of the desired data is already available, this investigation will be partially in nature of a check.)

V. Similar to (I) except copper and brass alloys. (This investigation to be coordinated with the work being done by Dr. Crampton of the Chase Brass and Copper Company.)

VI. Similar to (I) except Monel metal.

In all cases the investigator is to keep a record of the weld strengths obtained for given weld diameter at every optimum condition.

B—PROJECTION WELDING

Material

I. Mild steel, cold or hot rolled pickled, and in similar thicknesses only; otherwise similar to (A-I). Either one simple semispherical projection or three equi-distantly spaced projections shall be used at the discretion of the investigator. However, if single projection is used results should be checked with three projections and the difference, if any, recorded

The investigator to determine:

1.—Relation between projection diameter and diameter resulting weld, using what appears to be optimum current, pressure, time.

2.—Relation between height of projection and projection diameter in so far as resulting diameter of weld is concerned, using what appears to be optimum current, pressure, time.

3.—Using projections of minimum dimensions for

diameter of weld desired, the optimum current density or range of densities for given weld diameter, the range of weld diameters considered to cover the range practical for the thickness being investigated. Three or four weld time periods to be used to cover range practical for diameter and thickness being used.

4.—Optimum pressure density or range of densities for conditions used in (3).

5.—Optimum weld time periods for conditions used in (3 and 4).

II. Same as (I) except one sheet to be approximately five (5) times the thickness of the other sheet, making the ranges of the sheet thickness 0.020 to 0.109 and 0.109 to 0.500, respectively.

III. Check results of II with medium carbon steel (up to 0.25 C maximum) of heavier sheet.

IV. Check effect of placing projections on thinner sheet in II.

The Committee has been authorized by the Executive Committee of the Industrial Division to proceed with this program as soon, and as fast, as sufficient funds are made available for this work.

The Committee plans to conduct this research as much as practical at the Rensselaer Polytechnic Institute under the immediate guidance of Dr. Wendell Hess, as it feels that considerable advantage can be obtained by the uniformity of methods possible when work is conducted in one place and also because it has high regard for the facilities available there.

It is the Committee's understanding that an attempt is being made by NEMA to standardize the ratings of the welding contactors and transformers and, wishing to be of assistance in this matter, it has investigated, through a subcommittee, the question of welding contactors primarily from the user's standpoint and has come to the following conclusions:

REPORT OF SUBCOMMITTEE ON STANDARDIZATION OF WELDING CONTACTORS AND TRANSFORMER RATINGS*

A. *Contactor Ratings*

1. Contactors are to be rated on a constant temperature rise basis.

(*a*) A 150° C. rise was selected. Although a 120° C. rise was originally favored, correspondence with the equivalent NEMA committee lead us to believe that such a rating would be far too conservative.

Laboratory tests have proved that temperatures in the neighborhood of 250° C. as measured by means of a thermocouple embedded in the copper tip as near the surface as possible, result in "sticking" of contacts. By limiting the maximum temperature to a slightly less than 200° C., no difficulty should be experienced. Likewise, it is the opinion of the group that sufficient contact tip life will be obtained at this temperature rise.

Standardization in measurement of temperature rise should be a function of the NEMA engineering committee. Obviously, the true surface temperatures of the contacts cannot be recorded. Instead the thermocouple should be inserted in the tip from the rear, by means of rifle drilling extending to within about $^1/_{32}$ inch of the surface. Fish spline insulators proved satisfactory in preventing the short-circuiting of the thermocouple loop.

2. Contactor Identification. To avoid questions ordinarily raised by Underwriters' inspectors when contactors are intermittently applied, beyond their nominal ratings, it is recommended that size designations be used. The listing below is in accordance with NEMA committee recommendations.

Equivalent Nominal Capacity	Size Number
50 amperes	0
100 amperes	1
150 amperes	2
300 amperes	3
600 amperes	4
900 amperes	5

3. Contactor Rating Curves

(*a*) A separate set of curves for each contactor size is recommended. This step is taken to avoid the necessity for having the customer interpolate between actual load and a percentage of contactor ratings. In addition, the procedure follows that which has been established in connection with the rating of electronic devices.

(*b*) Individual curves for 220 and 440 volts, 60 cycle applications should be published. Correction factors are necessary for 550 volts and 25 cycle applications. These factors would compensate for the additional energy liberated in the higher voltage arcs or the prolonged arcing characteristic of the lowered frequencies. The values of these factors should be left up to the NEMA engineering committee.

(*c*) The curves should be plotted using primary amperes as the ordinant and average operations per minute as the abscissa. The family of curves indicating weld-periods of 2, 5, 10, 15, 20, 25, 30, 50, 75 and 100 cycles each, is suggested. It was agreed that the number of operations should be averaged over a five minute operating period before applied to the curve.

Primary amperes and average number of operations per minute have been selected as ordinant and abscissa, respectively, because these two factors have the greatest influence in the rating of the contactor. If two factors contributing to this rating were known and the third to be read from the curve, the unknown quantity should, without question, be plotted as the ordinant. Such procedure is standard practice. Since all three factors are known and it becomes necessary to simply fit these factors to one of a group of existing curves, the procedure first recommended is considered best.

(*d*) The cut-off points of the curves should be determined by a 400% nominal rating at low frequency operation. Temperature rise limitations of the operating coil of the contactor itself will be the determining factor at the higher operating speeds. It is considered an object of the NEMA engineering committee to establish a maximum operating speed above which the magnetic contactors under consideration should not be applied.

(*e*) Single pole contactors are to be rated at 50% of the rating permitted on the hottest pole of a double pole unit.

It is assumed that double pole contactors will be used to break both sides of the line. If such is the case, the poles of these contactors must be connected in series. Then the advantage of the 2-pole unit consists chiefly of providing two points of break at which energy can be dissipated. Because of the impossibility of keeping the individual contacts in perfect alignment, the heat is not evenly distributed. A 60–40% distri-

* Submitted by F. H. Roby, *Chairman*, C. E. Heitman, S. M. Humphrey and W. C. Hutchins.

bution is expected. The single pole contactor would then be rated at 50% of the hottest pole of the double pole device.

B. *Transformer Ratings*

1. The Resistance Welder Manufacturers' Association is now considering the A.I.E.E. proposal for standardization of welding transformer ratings. A joint meeting was held recently to adopt a final standard, acceptable to all concerned. Since the Welding Research Subcommittee is represented on both organization committees already dealing with the problem, there was no need for separate work.

The Committee has transmitted the above report to NEMA in hopes that it may be of assistance to it.

THE RESISTANCE WELDING COMMITTEE
G. S. Mikhalapov, *Chairman*

THE USE OF LIGHT STEEL TRUSSED Joists and Studs in Welded Frames*

By LESLIE W. GRAHAM, CLARKSON H. OGLESBY and EARLE S. SLOAN

INTRODUCTION

When Stanford University was planning to build Lagunita Court in 1934, it was decided to make use of a new form of steel frame, made up of light welded steel trusses, in place of the conventional type of wood studs and joists. This new type of construction, developed by the Soulé Steel Company, was about 10 per cent more costly than a standard timber design, but its many advantages definitely outweighed such an increase in cost.

During the construction of Lagunita Court, several load-deflection tests were made upon a bent, with the idea that the data obtained would be of use in checking any future theoretical work done in analyzing this structure. These tests were made by Richard Hattrup and Howard Russell, who subsequently incorporated them in their thesis, "Deflection Tests on the Welded Steel Frame of Lagunita Court," Stanford, 1935.

This work has been carried on, checked, and expanded by the writers of this thesis, who undertook to investigate the behavior of several of the trusses used in Lagunita Court by experimental means; to determine the stresses due to rigid joints and due to eccentric connections; and to arrive at some easily applied method of analytically determining the "Hardy Cross characteristics" required for analysis of the bent.

EXPERIMENTAL DETERMINATION OF JOISTS AND STUD CHARACTERISTICS

Three measurements were required to determine the characteristics of the trusses. These were: (1) rotations at the simply supported ends due to an external load applied at any location along the length of the truss, (2) rotation at one end due to moment applied at that end, and (3) rotation at one end induced by moment applied at the other end. In addition, deflections under the loads applied at the panel points, and deflections at these points due to end moment alone, were measured in order to check the analytical work (Fig. 1).

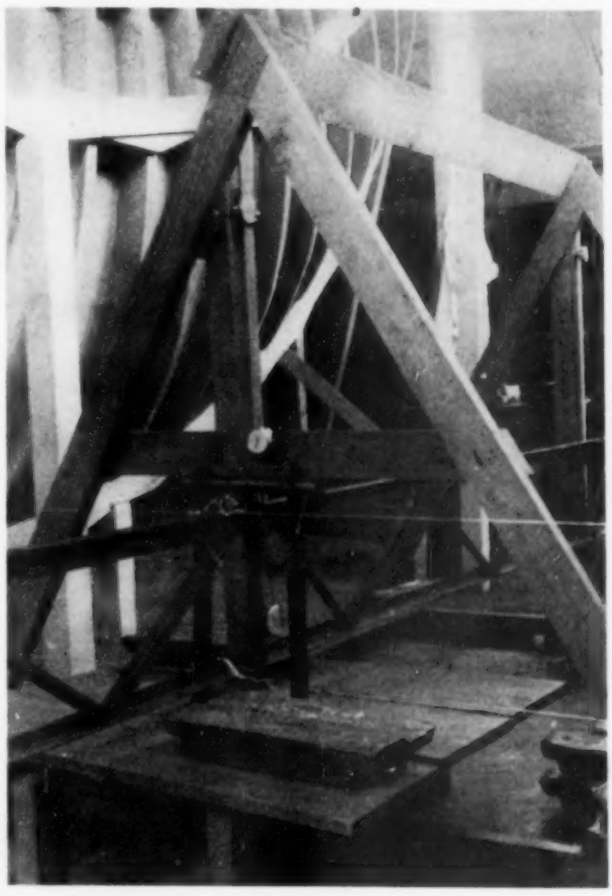


Fig. 1—Measuring End Rotations Due to Applied Moment

* Résumé of a thesis, supervised by Prof. A. S. Niles, submitted to the Dept. of Civil Engineering of Stanford University in partial fulfilment of the requirements for the Degree of Engineer, June 1936. Contribution to the Fundamental Research Division, Welding Research Committee.

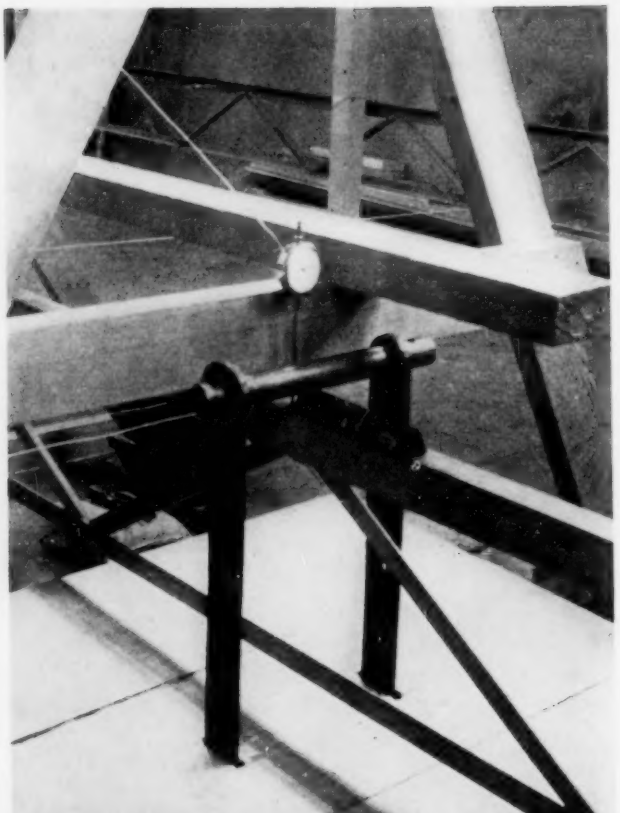


Fig. 2—Loading Platform Detail

Apparatus Used

Basic requirements determining the form of the apparatus were: a method of loading the truss, a method of supporting it to give simple beam action, and facilities for measuring the deflections and rotations obtained.

Loads were applied by means of lead pigs and shot-bags placed on platforms suspended from the upper chord of the truss being tested. The platforms (Fig. 2) were made of three 1 in. x 12 in. x 3 ft. boards secured to two horizontal steel angle sections by means of ordinary wood screws. The angles were in turn arc-welded to two vertical pieces of strap steel which formed the hangers, and were drilled at the top to take the removable pin which rested directly on the top chord.

Suitable end reactions were secured by using hollow, cast-steel prisms which formed part of the auxiliary equipment of a 200,000 lb. Riehle testing machine. Freedom from any form of end restraint was obtained by resting the ends of the trusses on steel pins (Fig. 3) one of which rolled, and one of which rested in a groove, on the flat upper face of steel billets resting on the truncated upper edges of the steel prisms (Fig. 4). The cast-steel prisms were brought to a firm level bearing by resting their bases on the machined upper faces of special horseshoe-shaped base plates, which were in turn set in a rich grout placed directly on the concrete floor.

Lateral stability of the trusses during the tests was secured by means of guy-wires attached at intervals to the upper and lower chords and run to convenient fixed points in the room. These wires were tightened by means of large turnbuckles.

The movements caused by end rotations were transferred to the dials, and at the same time magnified, for more accurate results, by means of vertical steel bars secured to the upper and lower chords of the trusses at the points where the measurement was required.

Description of Members Tested

Four members were tested, and their characteristics determined by substituting the experimental values in suitable derived equations. The members consisted of: (1) a long first floor joist, part of the length of which formed a room span, and part of which formed a corridor span, thus allowing both parts to be tested together; (2) a short floor joist, containing the same size members and truss pattern as the long floor joist, but containing only enough panels to form a room span; (3) an exterior wall stud, of the length used in Lagunita Court; and (4) an extra member, shorter and shallower than the regular floor joists, and discussed in this thesis under the name "shallow floor joist." All the connections in these members were electric arc-welded.

Members (1), (2) and (4) had their upper and lower chords formed of angle sections continuous over the length of the truss, while the web members were also formed of angles, with a section of the angle leg cut out to permit them to be bent at the panel points, and thus be formed from one continuous piece.

The stud, member (3) above, was of rather unusual construction. The upper and lower chords were formed from a steel sash bar, of the cross section shown in Fig. 5. The section was satisfactory as its shape allowed for

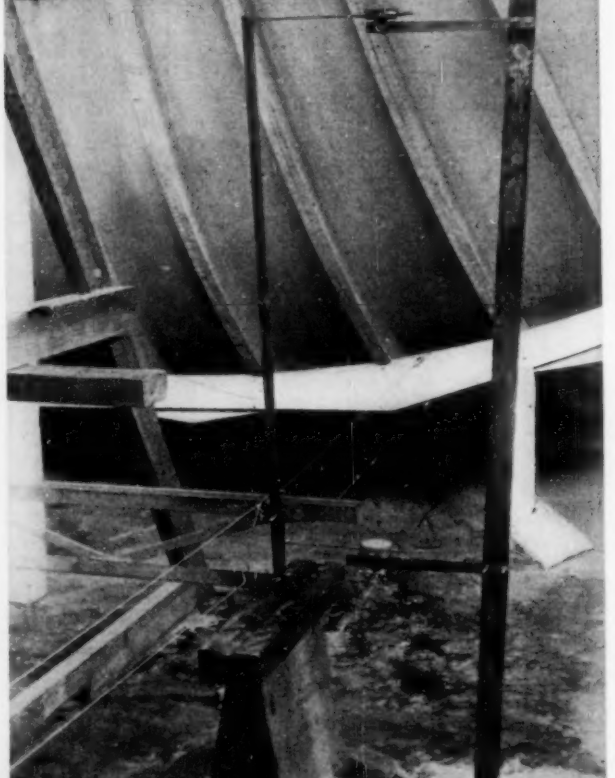


Fig. 3—Measuring Wall Stud South End Rotation

Table 1—Empirical Factors

Member	Note	Carry-Over A Exp.	Carry-Over A Comp.	Stiffness B Exp.	Stiffness B Comp.	Fixed-End Moment C Exp.	Fixed-End Moment C Comp.
First-floor joist, room span		0.436	0.453[D]	11.00	11.50[D]	44.80	44.89[D]
First-floor joist, hall span		0.174	0.280[E] 0.275	21.7	20.2[E] 19.8	...	...
Shallow floor joist		0.439	0.393	15.17	12.07	18.80	14.38
Exterior wall stud		0.223	0.372	4.55	3.19	19.46	12.25

Notes

A. Carry-Over Factor is the moment required to prevent rotation at the far end when the near end is subjected to a unit couple. It will vary from +0.5 for a solid web beam in which deformation due to shear is negligible to −1.0 for a "truss" without resistance to shear, i.e., an imaginary truss consisting of two parallel members functioning as chords and no web members.

B. The Stiffness Factor is the ratio of a couple applied to one end to the resulting rotation of that end in radians, the far end being assumed fixed. The figures in the table are the actual stiffness factors divided by 1,000,000.

C. The Tabulated Fixed-End Moments are those corresponding to loads of one pound at each third point. This factor was not determined for the short hall span.

D. The factors for the room span were also computed with allowance for the effect of the rigidity of the welded joints. The results were Carry-Over Factor, 0.427; Stiffness Factor, 10.90; and Fixed-End Moment, 43.70.

E. Two values given due to lack of symmetry of the span.

convenient attachment of the web members, and its section gave satisfactory column qualities. The web members were formed from a continuous furring channel, bent to the pattern required, and welded in place with the outstanding legs perpendicular to the plane of the truss.

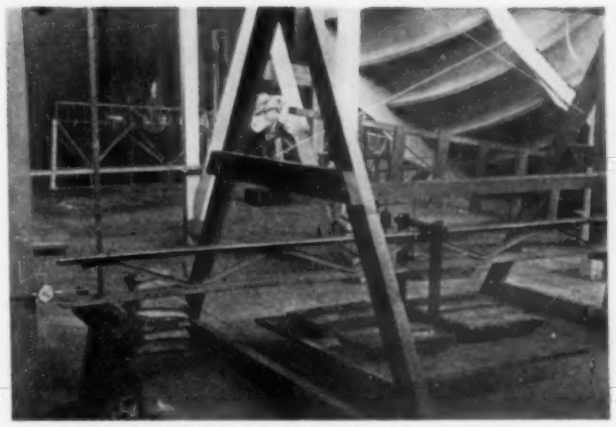


Fig. 4—Wall Stud Fixed-End-Moment Test—North End Rotation

All the members tested had eccentric connections. In the case of the floor joists these were confined to the upper chords, but in the studs the unusual shape of the sash sections used caused eccentricity at both the upper and the lower chords. The sizes of the structural shapes used in the members tested, as well as the rest of the members used in Lagunita Court, are presented in tabular form in Fig. 5.

Experimental Results

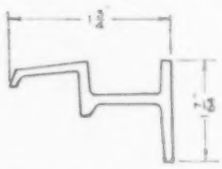
On completion of the tests the experimental data were utilized to compute empirical values of the carry-over factor, stiffness factor and fixed end moment for the

ROOM SPAN

CORRIDOR SPAN

FLOOR JOIST

EXTERIOR & INTERIOR STUDS



Section of Stud chord

	TRUSS	DIMENSIONS LENGTH	TOTAL DEPTH	EFFECT DEPTH	TOP CHORD MEMBERS	LENGTH	AREA	I/A	I	I/L*K	BOTTOM CHORD MEMBERS	LENGTH	AREA	I/A	I	I/L*K	WEB MEMBERS MEMBERS	LENGTH	AREA	I/A	I	I/L*K
ROOM SPAN	FIRST FLOOR	202"	12"	©11.03 ⊕10.40	1-2"×1½"×⅜"∠ 2" legs down	33.67	4.22	©79.8 ⊕79.8	.1731	©.00484 ⊕.00484	1-1½"×1½"×⅜"∠	33.67	2.97	113.4	.0440	©.00131	1-1"×1"×⅜"∠	©20.12 ⊕19.78	2.34	©85.99 ⊕84.50	.0216	©.00175 ⊕.00169
	SECOND FLOOR	202"	12"	10.40	1-2"×1½"×⅜"∠ 2" legs down	33.67	4.22	79.8			1-1½"×1½"×⅜"∠	33.67	.360	93.7			1-1"×1"×⅜"∠	19.78	2.34	84.5	.0216	
	CEILING	202"	12"	10.40	1-2"×2"×⅜"∠	33.67	.475	70.9			1-2"×1½"×⅜"∠ 2" leg up	33.67	.422	79.8			1-1"×1"×⅜"∠	19.78	2.34	84.5	.0216	
CORRIDOR SPAN	FIRST FLOOR	87"	12"	10.40	1-2"×1½"×⅜"∠ 2" leg down	29.0	.422	68.72			1-1¼"×1¼"×⅜"∠	29.0	.297	97.64			1-1"×1"×⅜"∠	19.78	2.34	76.34	.0216	
	SECOND FLOOR	87"	12"	10.40	1-2"×1½"×⅜"∠ 2" leg down	29.0	.422	68.72			1-1½"×1½"×⅜"∠	29.0	.360	80.67			1-1"×1"×⅜"∠	19.78	2.34	76.24	.0216	
	CEILING	87"	12"	10.40	1-2"×2"×⅜"∠	29.0	.475	61.05			1-2"×1½"×⅜"∠ 2" leg up	29.0	.422	68.72			1-1"×1"×⅜"∠	19.78	2.34	76.24	.0216	
WALL STUDS	EXTERIOR	122.5"	6"	4.25"	Sash Section	24.50	.36	68.1			Sash Section	24.50	.36	68.10			0.4"/in ft furring [	12.97	.112	115.6		
	INTERIOR	122.5"	4"	2.25"	Sash Section	24.50	.36	68.1			Sash Section	24.50	.36	68.10			0.4"/in ft furring [	12.45	.112	111.0		

© Using centroidal distances of top and bottom chords
⊕ Using intersection of centroids of web members

Fig. 5—Analytical Determinations of Joists Characteristics

various members. These empirical factors are compared in Table 1 with the corresponding values computed on the assumption that the parts of the welded trusses were connected by frictionless pins located along the centroidal axes of the chord members.

To eliminate any confusion it should be stated that the stud and hall span lengths tested were shorter than the lengths used in Lagunita Court. This change was necessitated by the laboratory setup and made for better and more accurate results.

Discussion of Test Results

In the process of testing sufficient readings were taken to permit a check of the results by applying Maxwell's Theorem of Reciprocal Deflections. This check was very satisfactory in the case of the room span of the first floor joist, but not for the other members. The unabridged thesis discusses the probable causes of the discrepancies in some detail. The test data for this span, however, were so consistent and the empirically obtained characteristics in such close agreement with the computed values that the writers consider them to justify the following conclusions.

Table 2—Moment Distribution Characteristics as Determined by Laboratory Tests

Member	Carry-over Factor	Stiffness	Fixed-End Moment From Tests	Conventional for Beam
First-floor room joist	0.436	11.02×10^6	44.80	44.89
First-floor hall joist	0.174	21.7×10^6		
Shallow floor joist	0.439	15.17×10^6	18.80	14.38
Exterior wall stud	0.223	4.55×10^6	19.46	12.25

CONCLUSIONS

1. Sufficient accuracy in the determination of moment distribution characteristics of the shallow welded trusses for use in rigid frames can be obtained by making pin-jointed analyses of the trusses, provided the effective depth is taken as the distance between centroids of chord members.

2. Fixed-end moments produced on Warren trusses of uniform pattern and constant chord area by *symmetrical* transverse loads may be taken as being of the same magnitude as for a beam similarly loaded. Carry-over factors, stiffnesses and fixed-end moments for unsymmetrical loading must be computed (Table 2) An approximate value of carry-over factor may be used.

3. Bending stresses produced in the individual members of the welded trusses due to the effect of joint rigidity and eccentricity are of considerable magnitude and should be considered.

Discussion of Review of Literature on Welding Chromium and Chromium-Nickel Corrosion and Heat Resisting Steels

MR. J. T. CATLETT, General Electric Company supplies the following discussion of the thinnest and thickest 18–8 which can be welded successfully by the atomic hydrogen process.

"Several characteristics of the chromium and chromium-nickel corrosion and heat resisting steel alloys, when allowed full play, introduce obstacles which tend to fix the lower limit of thickness. These are: the high coefficient of expansion, the low heat conductivity, and tendency to oxidize at high temperatures.

The effect of the first two is increased distortion and may be partially negated by holding and cooling clamps. The effect of the last is destruction of the under-surface film, which by its surface tension holds the molten metal in place until it solidifies. This can be prevented by providing a reducing atmosphere around the underside of the weld. An effective method is to provide a groove in the clamp into which a small amount of hydrogen is introduced. If this method is employed, material 0.020–0.025 inch thick can be welded. Without it, the lower limit is probably $^1/_{16}$ inch. Without cooling and holding clamps, the limit is somewhat heavier.

The maximum thickness is largely determined by the amount of permissible distortion. Complete penetration cannot be secured unless the molten metal is held in place until it solidifies, either by surface tension or by solid metal underneath. Practically, this means that the use of filler rod must be resorted to if the material thickness is much greater than $^1/_8$ inch. This may be done in additional passes or by allowing the molten pool to solidify from the bottom upward as metal in the form of filler rod is added, thus gradually building the weld zone up to the thickness of the plate."

Discussing the conclusion on page 67 of the review that "intergranular corrosion occasioned by carbide precipitation is bound to occur in the heat-affected zone of welded 18–8 in severely corrosive reagents, unless the joint is heat treated or unless the 18–8 contains an alloying element to prevent intergranular corrosion, or unless the carbon content is exceedingly low," J. H. G. Monypenny expressed the opinion (private communication, March 1939) that the clause "or unless the actual time the steel is heated during the welding operation is very short" should be added. Monypenny has found (see review of literature on Corrosion Resistance of Welded Joints, AMERICAN WELDING SOCIETY JOURNAL, **16** (8) Suppl. 20 (1937)) that unstabilized 18–8 may be heated for nearly a minute at 600 to 700° C. without becoming sensitive to boiling acid copper sulphate solution. Tests in other reagents, such as boiling 65% HNO_3, were not reported. The incubation time for precipitation of carbides in unstabilized 18–8 does not appear to have been determined. Accordingly, the importance attached by spot welding experts to preventing the heat-affected zone from reaching the surface of the sheet appears justified.

The following tests on all-weld-metal deposited by high Cr-Ni, electrodes containing tungsten are reported by R. D. Thomas, Jr., Arcos Corporation.

Composition of Weld Metal					Yield Strength, Lb./In.²	Tensile Strength, Lb./in.²	Elongation, % in 2 Inches
Cr	Ni	C	W	Mo			
21.29	19.96	0.072	0.98	..	55,000	84,500	34
25.10	20.07	0.088	1.08	..	55,000	86,500	38
12.86	20.11	0.44	2.16	0.63	56,000	60,000	5

THE EFFECT OF PHOSPHORUS ON the Welding of Steel

A Review of the Literature to July 1, 1937

By W. SPRARAGEN* and G. E. CLAUSSEN†

This report is prepared under the auspices of
THE LITERATURE DIVISION OF THE ENGINEERING FOUNDATION
WELDING RESEARCH COMMITTEE

TABLE OF CONTENTS

	Page
Summary	123
Introduction	124
Phosphorus in Steel	124
Oxyacetylene Welding	124
Bare Electrodes	126
Covered Electrodes	126
Resistance Welding	127
Forge Welding	127
Other Welding Processes	127
Oxygen Cutting	128
General Observations	128
Welding Low-Alloy Steels Containing Phosphorus	128
Copper-Phosphorus Steels	128
Chromium-Copper-Phosphorus Steels	128
Copper-Nickel-Phosphorus Steels	129
Cu-Ni-Cr-P Steels	129
Mn-Si-Cu-P Steels	129
Acknowledgments	130
Bibliography	130
Suggested Research Problems	130

Summary

Phosphorus in Steel

The usual percentages of phosphorus in solid steel are in solid solution. Phosphorus has a reputation for segregating, and above about 0.15% decreases the ductility and notch impact value. It exerts a useful effect in raising the strength of some low alloy steels.

Oxyacetylene welding

A number of foreign investigators have studied the effect of phosphorus in the form of PH_3, phosphine, as an impurity in the acetylene on oxyacetylene welding. The phosphine within usual limits had no effect on welding.

The ductility steadily decreased and the hardness increased as the phosphorus content of the weld metal increased. With 1.0 and 2.0% phosphine the welds contained numerous blow-holes or slag inclusions.

Bare Electrodes

Apart from dilution by base metal, the phosphorus content of weld metal deposited by bare electrodes is practically the same as the phosphorus content of the electrodes themselves.

Covered Electrodes

In bend tests multi-layer welds in steel containing 0.08 C, 0.058 S, 0.10 P made with covered electrodes failed at the junction of weld with base metal on account of phosphorus segregation, which embrittled the fusion zone.

Forge Welding

One author states that steel containing 0.09 C, 0.43 Mn, 0.050 S was easier and safer to forge weld with 0.103 P than with 0.013 P. It was his opinion that phosphorus up to Bessemer limits (0.11% max.) was beneficial to forge welding. With higher carbon contents phosphorus is not desirable.

Oxygen Cutting

A smooth oxyacetylene flame cut was made without difficulty in an iron containing 2% P.

Welding Low Alloy Steels Containing Phosphorus

Copper-Phosphorus Steels.—Arc welds in steel containing 0.15 C, 0.60 Mn, trace Si, 0.09 P, 0.42 Cu have excellent tensile strength and ductility.

Chromium-Copper-Phosphorus Steels.—The addition of up to 0.15 P to chromium-copper steels does not affect the welding properties appreciably. The steel contained 0.10 C max., 0.50–1.50 Cr, 0.30–0.50 Cu, 0.10–0.20 P, 0.10–0.30 Mn, 0.50–1.00 Si and was $^3/_8$ inch thick.

Copper-Nickel-Phosphorus Steels.—Oxyacetylene welds in a steel containing 0.10 C, 0.61 Mn, 0.19 Si, 0.62 Ni, 1.15 Cu, 0.102 P had good tensile strength and ductility and offered no difficulties.

Cu-Ni-Cr-P Steels.—Tests were made on butt welds in a commercial low alloy steel sheet containing 0.08 C 1.06 Cu, 0.50 Ni, 0.35 Cr and 0.118 P. There was no air hardening and the strength and notch impact value were good, but the ductility was a bit low. Phosphorus created no welding difficulties whatever.

*Secretary, Welding Research Committee.
†Research Assistant, Welding Research Committee.

The Effect of Phosphorus on the Welding of Steel

INTRODUCTION

PHOSPHORUS may affect the welding of steel in several ways. First, the phosphorus content of base metal may be the vital factor; or the filler rod may have an exceptional phosphorus content, or the welding atmosphere may add phosphorus to the weld metal, or remove it therefrom. Phosphorus may affect the welding operation as well as the properties of the welded joint. Ordinarily it is high phosphorus that is to be guarded against, but in some welding processes high phosphorus is not a disadvantage. Although phosphorus *may* affect welding in all these ways, the literature reveals that the effects of phosphorus are relatively slight up to 0.15%. This summary of the literature on phosphorus in welding deals first with the aspect of phosphorus as an impurity, each welding process being considered separately. Since every steel that has ever been welded contained phosphorus as an impurity, the review has been restricted to those instances in which phosphorus was the major variable. The part phosphorus plays as an intentional addition to low alloy steels is discussed in the later sections of the review.

PHOSPHORUS IN STEEL

Phosphorus enters steel through the blast furnace charge. Although phosphorus boils at 280° C., it is not removed from steel as vapor in the refining processes. Instead, in the basic process, phosphorus is oxidized to pentoxide, P_2O_5, which combines with lime (or FeO under extraordinary conditions) to form phosphate (melting point of $(CaO)_3P_2O_5$ = 1650° C.). In the basic open-hearth process, removal of phosphorus from metal to slag is favored by low temperature, and high FeO and CaO content in the slag. In the acid open-hearth process there is no removal of phosphorus. Phosphorus oxidizes in the Bessemer process later than carbon, silicon and manganese, and only if the slag is basic.

The usual percentages of phosphorus in solid steel are in solid solution. Phosphorus has a reputation for segregating, and above about 0.15% decreases the ductility and notch impact value. It exerts a useful effect in raising the strength of some low alloy steels. There is no iron-carbon-phosphorus compound in steel. The depth-hardening properties of steel appear to be little affected by phosphorus up to 0.15%, but the Joint Committee on Investigation of the Effect of Phosphorus and Sulphur in Steel (A. S. T. M., 1934) showed that an increase in phosphorus content up to 0.08% adversely affected the behavior of quenched, plain carbon steels (0.10 C, 0.50 Mn, 0.02 Si) in cold bending.

OXYACETYLENE WELDING

A plate containing 0.09 P, 0.056 S, 0.59 inch thick from an old boiler that had operated at 112 lb./in.^{2} for 25 years was cut up and oxyacetylene welded by Baatz,[1] who found that the welds had:

Tensile strength	= 48,000 to 52,000 lb./in.^{2}
Elongation	= 10.7 to 14.3% in 1.18 inches
Bend angle	= 44 to 59 degrees
Notch impact value	= 3.5 to 4.4 mkg./cm.^{2}

The standard German notch impact specimen (DUV; 0.59 x 1.18 inches, 0.59 inch notch depth) was used. After an aging treatment the notch impact value fell to 1.6 mgk./cm^2. In view of the age of the steel the results were not considered unsatisfactory. Pomp[2] found that it was segregation, not total phosphorus content that had a bad effect on the oxyacetylene welding of pipes. A steel with 0.074 P, 0.068 S gave good, smooth welds, whereas pipes with the same carbon content that yielded bad oxyacetylene welds had lower S and P contents. However, the troublesome pipe had segregated areas.

Filler rods for oxyacetylene welding, according to Coulson-Smith,[3] should contain not over 0.05 P, otherwise there is boiling of the weld metal and the weld is brittle. Specifications for filler rods generally limit the phosphorus content to 0.03–0.05% without stating the reasons.

A number of foreign investigators have studied the effect of phosphorus in the form of PH_3, phosphine, as an impurity in the acetylene on oxyacetylene welding. In Séférian's[4] tests, Table 1, the phosphorus content was determined (ammonium phospho-molybdate) in fused plates, deposits from rods, or welds. The acetylene was analyzed for phosphine by the Granjon method ($HgCl_2$). The phosphine had no effect on welding.

Table 1—Pick Up of Phosphorus from Acetylene, Rod and Plate. Séférian[4]

		Phosphorus, Wt. %					
				Acetylene*	Deposit	Acetylene*	Deposit
Specimen	Rod	Rod	Plate				
Melted once	Armco	0.007	.....	0.040	0.009	0.131	0.022
Melted twice	Deposit	0.009 or 0.022	.....	0.040	0.009	0.131	0.025
Weld	Armco	0.007	0.025	0.040	0.010	0.131	0.028
Weld	Armco	0.007	0.077	0.040	0.030	0.131	0.066
Electrolytic iron plate fused without rod	.....	...	0.012	0.04	0.019	0.133	0.038
Second fusion	.....	...	0.019	0.04	0.020	...	...
Weld	Electrolytic iron	0.012	Electrolytic iron	...	...	0.133	0.048
Plate melted down without rod	.....	...	0.023	0.04	0.028	0.141	0.050
Second fusion	.....	...	0.028 or 0.050	0.04	0.029	0.141	0.052
Line of fusion on plate without rod	.....	...	0.023	...	...	0.141	0.044
Weld	Steel	0.023	0.023	0.04	0.025	0.141	0.062
Weld	Steel	0.057	0.057	0.04	0.047	0.141	0.074
Plate melted down without rod	.....	...	0.057	0.04	0.063	...	...
Second fusion	.....	...	0.063	0.04	0.062	...	...
Weld	Steel	0.057	0.057	0.04	0.065	...	...
Remelted without rod	.....	...	0.065	0.04	0.067	...	...
Remelted with rod added	Steel	0.057	0.065	0.04	0.064	...	...
Plate melted down without rod	....	...	0.077	...	...	0.131	0.079
Weld	Steel	0.077	0.077	...	...	0.131	0.078

	C	Mn	Si
Armco iron	0.035	0.014	trace
Electrolytic iron	0.025	trace	trace
Dead mild steel	0.05	0.18	trace

*Vol. % PH_3 in acetylene.

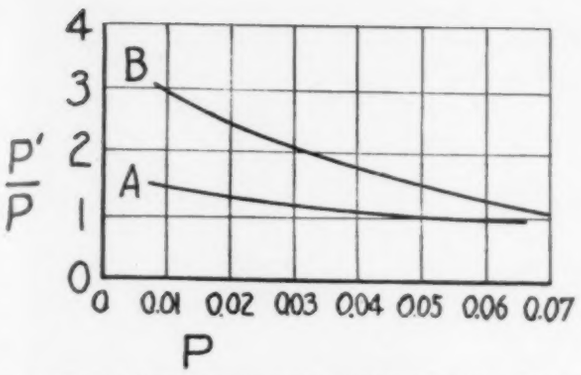


Fig. 1—Effect of Phosphorus Content of Acetylene and Base Metal on Phosphorus Content of Weld Metal

P = Phosphorus content (%) of unwelded base metal
P' = Phosphorus content (%) of weld metal
A = Acetylene containing 0.040% PH_3
B = Acetylene containing 0.131 and 0.140% PH_3
Séférian[4]

Inspection of Table 1 shows that an increase in phosphine in the acetylene within the limits of the tests increased the phosphorus content of the deposit if the rod and base metal had relatively low phosphorus, Fig. 1. Repeated fusion of an Armco iron rod in impure acetylene did not greatly increase the phosphorus content of the deposit. The deposit became nearly saturated in the first melting. Repeated fusion of the soft steel containing 0.057 P in acetylene containing 0.04% PH_3 did not change the phosphorus content. Steel with 0.077 P was in equilibrium with acetylene containing 0.131% PH_3. However, the phosphorus contents of the rod and plate were important factors in welds, as shown by the welds in high-phosphorus steels made with Armco rods. Notwithstanding the small increase in phosphorus content of the repeatedly fused deposit, there is indication that the longer time that the metal was molten in the welds led to greater absorption of phosphorus than in the simple melting down tests, as shown by the results for electrolytic iron. All in all, Séférian believed his tests showed that, for a given phosphine content in the acetylene and phosphorus content in the steel, there is an equilibrium or saturation content of phosphorus in weld metal. He could not decide whether the rate of absorption of phosphorus by weld metal from impure acetylene was greater than the rate of absorption of phosphorus by pure acetylene from impure weld metal.

Using a wider range of phosphine contents, Holzhäuser[5] found, like Séférian, that pick up of phosphorus from acetylene was approximately proportional to the phosphine content of the acetylene, Table 2. Dry generator acetylene and tank oxygen (99.2% purity) were used. The ratio of oxygen to acetylene was 1.06 to 1 in all tests. Butt welds, 60° V, 11 inches long were made by hand by an experienced welder in dead mild steel 0.24 inch thick with a filler rod containing 0.07 C, 0.34 Mn, 0.0 Si, 0.020 P, 0.022 S, 0.20 Cu. Four welds were made with each gas mixture. The mechanical properties were determined on standard German (DIN 1605) specimens, the weld being machined flush. The hardness was measured in the center of the cross section of the weld.

The behavior of the weld puddle was not changed in any way by 0.1 vol. % PH_3 in the acetylene. With 0.25 vol. % PH_3 there was a little bubbling, which increased at 0.5 vol. % PH_3. With 1.0 vol. % PH_3 the color of the flame was yellowish white, the weld boiled and the slag (no details) was viscous. Blue fumes were evolved. These conditions were accentuated by 2.0 vol. % PH_3 and a band of white residue $^1/_2$ inch wide was found on both sides of the weld. Unfortunately, Holzhäuser did no more than describe these interesting conditions. Apparently the blue fumes represented vaporization and combustion of phosphorus, and the white residue may have been P_2O_5. The slag, doubtless, contained quantities of $(FeO)_3P_2O_5$ to which perhaps the increased viscosity of the slag was due. The phosphate has a low heat of formation (52, 360 cal. per mol. P_2O_5), but its melting point and viscosity are not known.

Together with a pick-up of phosphorus, there was a slight decrease in sulphur content of the weld metal as the phosphine content of the acetylene was increased. The ductility steadily decreased and the hardness increased as the phosphorus content of the weld metal increased. With 1.0 and 2.0% phosphine the welds contained numerous blow-holes or slag inclusions. Although Holzhäuser concluded that the phosphorus content of weld metal increased in proportion to the phosphine content of the acetylene, a plot of his results indicates that for a unit increase in phosphine content in the acetylene, the pick-up is maximum at about 0.5 vol. % PH_3. The increase in phosphorus content between 1 and 2 vol. % PH_3 is relatively small, perhaps for the reason that the fumes represent loss of P from the flame before the gas reaches the weld puddle.

The effect of hydrogen sulfide together with phosphine in the generator gas is also shown in Table 2 (see review of literature on Effect of Sulphur on the Welding of Steel AMERICAN WELDING SOCIETY JOURNAL, **18** (2) Suppl., 44–49, Feb. 1939). At a content of 0.25 vol. % for each gas the weld bubbled. With 0.5 vol. % of each the weld foamed, the slag was viscous, and the color of the flame was yellowish white. Conditions were worse at 1.0 vol. % of

Table 2—Effect of Phosphine, with or without Hydrogen Sulphide, in the Acetylene on Oxyacetylene Welds. Holzhauser[5]

Impurities in Acetylene, Volume %		Composition of Weld Metal %					Mechanical Properties			
PH_3	H_2S	C	Mn	P	S	Cu	Yield Strength, Lb./In.²	Tensile Strength, Lb./In.²	Rockwell Hardness, B Scale	Bend Angle, Degrees
0.05	..	0.04	0.30	0.030	0.036	0.21	38,000	40,600	62.8	138
0.1	..	0.05	0.28	0.037	0.036	0.22	37,000	43,000	64.7	131
0.25	..	0.05	0.31	0.061	0.036	0.22	38,000	44,300	65.2	104
0.5	..	0.04	0.30	0.124	0.032	0.25	38,400	41,000	69.4	75
1.0	..	0.04	0.29	0.250	0.030	0.23	37,200	34,200*	76.9	27
2.0	..	0.04	0.29	0.370	0.030	0.22	....	24,400	84.1	9
0.05	0.05	0.03	0.28	0.029	0.032	0.22	37,500	42,400	66.0	145
0.1	0.1	0.03	0.29	0.037	0.038	0.22	36,800	45,500	65.0	139
0.25	0.25	0.05	0.29	0.063	0.052	0.22	38,200	41,800	69.5	107
0.5	0.5	0.05	0.29	0.115	0.066	0.23	38,900	41,100	68.2	64
1.0	1.0	0.05	0.25	0.180	0.088	0.24	....	25,600	68.5	16
2.0	2.0	0.08	0.29	0.340	0.168	0.23	....	14,500	73.0	8
0	0	0.04	0.28	0.019	0.028	0.23	37,600	42,400	63.5	142

*Probably a misprint.

each gas, and with 2.0 vol. % of each the foaming was so bad and the slag so viscous that the weld metal had to be stirred to secure penetration.

The absorption of phosphorus was 45% greater than the absorption of sulphur, and the absorption of both was directly proportional to the content of impurity in the gas. If the gas did not contain H_2S, 8% more P was absorbed. If the gas contained only H_2S, 9% less S was absorbed. That is, sulphur decreases the absorption of phosphorus but phosphorus increases the absorption of sulphur. There was no change in the microstructure of the welds until the acetylene contained 0.5 vol. % of each gas. The welds then were slightly porous. There were coarse grains and large oxide inclusions (no details) in the fusion zone in the welds made with acetylene containing 1.0 vol. % each gas. With 2.0 vol. % of each gas these conditions were accentuated. The addition of H_2S to acetylene containing PH_3 led to no further decrease in ductility but decreased the hardness. Holzhäuser concluded that 0.1 vol. % PH_3 with or without 0.1 vol. % H_2S in the acetylene has a negligible effect on welding. With 0.25 vol. % PH_3 with or without 0.25 vol. % H_2S the ductility is impaired.

Using an impure acetylene containing 0.025 gm. P and 0.01 gm. S per 100 liters for V butt welding plates 0.08, 0.24 and 0.63 inch thick containing 0.04 C, 0.3 Mn, 0.03 S, trace P, with a rod containing 0.03 C, 0.27 Mn, 0.028 S, trace P, 0.01 Si, Rimarski[6] found that the bend angle and tensile strength were the same as with pure acetylene, even if 1.6 vol. % H_2O was added to the oxygen. The weld metal deposited with the impure acetylene contained 0.05–0.06 C, 0.01–0.02 P, 0.024–0.027 S. Since the inner cone of the flame was held close to the weld in order to facilitate absorption of S and P, there was a slight pick-up of carbon.

Rimarski believed that pick-up of phosphorus from impure acetylene was more likely than pick-up of sulphur for the reasons that iron can be dephosphorized less rapidly than it can be desulphurized in hydrogen at 1200° C., and the heat of formation of H_2S is greater than that of PH_3. Besides, Rimarski pointed out that phosphorus has greater solubility and diffusion rate in iron than sulphur.

Table 3—Recovery of Phosphorus in Oxyacetylene Welding Mild Steel. Reichsanstalt[7]

	Commercially Pure Acetylene		Acetylene with 0.02 Vol. % Phosphine	
Phosphorus in rod, %	0.045	0.037	0.01	0.005
Phosphorus in deposit, %	0.043	0.034	0.016	0.010

According to a German laboratory,[7] phosphorus in acetylene may or may not be absorbed by the weld puddle depending on the phosphorus content of the puddle and on the phosphorus and hydrogen contents of the flame. As a general rule, Table 3, with a ratio of oxygen to acetylene of 1.1 or 1.2 to 1 the weld metal has slightly lower phosphorus content than the mild steel welding rod, if commercial acetylene is used.

Phosphorus as an impurity (20–40 cc. per 100 liters) in acetylene, LeGrix[55] found, is rapidly converted to P_2O_5 in the inner cone of the oxyacetylene flame whereas in the air-acetylene flame P_2O_5 is formed only on the outskirts of the outer cone. Consequently, there is relatively little pick-up of phosphorus in oxyacetylene welding steel, because the tip of the inner cone rarely touches the weld metal. However, the reaction of P_2O_5 with flux may produce noxious fumes.

BARE ELECTRODES

Apart from dilution by base metal, the phosphorus content of weld metal deposited by bare electrodes is practically the same as the phosphorus content of the electrodes themselves. A number of determinations of the phosphorus loss in bare electrodes containing up to 0.05 P have been examined but no clearly defined trend toward loss could be detected. A cold rolled bare electrode containing 0.11 C, 0.72 Mn, 0.011 Si, 0.097 P, 0.123 S deposited metal containing 0.05 C, 0.11 Mn, 0.011 Si, 0.086 P, 0.072 S, according to Hobart.[8] A small loss of P may occur, therefore, in high-phosphorus electrodes, but as Losana[9] stated on the basis of his experimental results, unless the conditions of oxidation are unusually severe, there is no loss of phosphorus from electrodes with up to 0.05 P.

COVERED ELECTRODES

In bend tests performed by *Arcos*,[10] multilayer welds in steel containing 0.08 C, 0.058 S, 0.10 P made with covered electrodes failed at the junction of weld with base metal on account of phosphorus segregation, which embrittled the fusion zone. They found that a good test for detecting high phosphorus in a plate consists in making a weld and noting whether fracture occurred in the fusion zone. Stieler[11] believed that the phosphorus content was responsible for the cracking of weld metal deposited by a heavy covered electrode containing 0.14 C, 0.58 Mn, 0.03 Si, 0.071 P, 0.035 S, 0.13 Cu, 0.05 Ni, 0.02 Cr, 0.0006 H_2. Using mild steel electrodes with a coating containing different amounts of Na_3PO_4, Linetsky[56] found that the deposited metal had no tendency to crack even if the phosphorus content reached 0.098%.

Weld metal from covered electrodes may pick up phosphorus from base metal, Table 4, in which case the notch impact value is lowered. The notch impact specimens were 0.39 inch diameter with a circumferential 60° notch 0.06 inch deep. *Arcos*[10] was of the belief that phosphorus is not favorable to grain refinement in the heat-affected zone of multilayer welds.

Covered electrode welds in a 25-year-old boiler plate containing 0.09 P, 0.056 S, 0.59 inch thick, had:

Tensile strength, lb./in.^{2}	=	53,000 to 56,500
Elongation, % in 1.18 inches	=	6.7 to 8.3
Notch impact value, mkg./cm.^{2}	=	2.8 to 3.5
Bend angle, degrees	=	24 to 30

Baatz[1] was not disappointed with these results.

Table 4—Phosphorus Pick-Up by Covered Electrodes. *Arcos*[10]

	All-Weld-Metal Deposit	Plate Rich in S and P		Plate Low in S and P	
		Base Metal	Weld	Base Metal	Weld
C	0.12 to 0.14	0.08	0.115	0.156	0.124
S	0.02 to 0.025	0.058	0.018	0.032	0.027
P	0.04 to 0.045	0.100	0.077	0.016	0.044
Mn	0.5 to 0.6	...	...	0.53	...
Si	< 0.08	...	...	0.023	...
Notch impact value, mkg./cm.^{2}*		...	4.0 to 5.95	....	5.95 to 6.40

*4 tests on each weld.

The recovery of phosphorus from covered electrodes may be quite low, as shown in Table 5. There has been no systematic study of phosphorus in coated electrodes. Without supplying quantitative results, Notvest[13] points out the well known fact that if electrode or base metal has a high phosphorus content, the coating must be high in iron oxide and low in silica to remove phosphorus from the puddle. Again without experimental support, it was stated that phosphorus reverts from slag to puddle if

Table 5—Recoveries from Bare and Covered Electrodes. Schoenmaker[12]

Type of Electrode*	Carbon, %		Manganese, %		Silicon, %		Phosphorus, %		Sulphur, %	
	Electrode	Deposit	Electrode	Deposit	Electrode	Deposit	Electrode	Deposit	Electrode	Deposit
Bare	0.068	trace	0.41	trace	0.052	0.010	0.035	0.031	0.024	0.017
Covered	0.068	0.031	0.41	0.20	0.052	0.011	0.035	0.017	0.024	0.016
Bare	0.14	trace	0.47	0.08	0.027	0.008	0.035	0.040	0.024	0.021
Covered	0.14	0.09	0.47	0.45	0.027	0.010	0.035	0.030	0.024	0.020
Bare	0.74	0.22	0.77	0.43	0.024	0.020	0.033	0.043	0.020	0.018
Covered	0.74	0.38	0.77	0.70	0.024	0.020	0.033	0.027	0.020	0.018

*D.c. for bare; a.c. for covered.

the total SiO_2 content of the coating is higher than the total content of basic materials. Notvest does not make clear what constituents of coatings are to be regarded as basic. Okada[14] had little difficulty recovering phosphorus from covered electrodes. His deposits contained up to $2^1/_2$% P. Although the high-phosphorus deposits had low hysteresis loss, they were brittle and could be cracked by a light hammer blow.

RESISTANCE WELDING

Tafel and Heil[15] had no difficulty resisting butt welding steel containing 0.085 C, 0.51 Mn, 0.00 Si, 0.092 P, 0.059 S, 0.052 As, 0.16 Cu. The strength was up to expectations but the ductility was not determined. However, in 1919, Heaton[16] could not make resistance butt welds in open-hearth steel sheets containing 0.09 C, 0.38 Mn, 0.05 Si, 0.159 P, 0.072 S. More recently, Hughes[17] believed that over 0.025 P creates difficulty in spot welding.

FORGE WELDING

Speller[18] stated that steel containing 0.09 C, 0.43 Mn, 0.050 S was easier and safer to forge weld with 0.103 P than with 0.013 P. It was his opinion that phosphorus up to Bessemer limits (0.11% max.) was beneficial to forge welding. McCaffery[19] and others have found that Bessemer skelp for butt welded pipe contains 0.09 P as a general rule and that Bessemer steel with up to 0.110 P is noted for good forge weldability. Howe[20] in 1890 and Bonsmann and Püngel[21] in 1930 thought forge weldability was improved by phosphorus, although the latter point out the danger of cold shortness. On the basis of mill experience, Ristow[22] stated that phosphorus exerts a good influence on forge welding properties, especially in pipe steel. Henning[58] was not certain whether it was the phosphorus or the oxygen content of Bessemer steel that accounted for its excellent forge welding properties. In wrought iron with up to 0.1 C, Ledebur[23] and Schäfer[24] found that up to 0.4 P has no effect on forge weldability. With higher carbon contents phosphorus is not desirable. In 1877 Holley[25] found that wrought iron with 0.32 P gave no trouble in forge welding. However, Mrázek[26] could not forge weld a steel containing 0.16 C, 0.38 Si, 0.038 S, 1.08 P.

The forge weldability of unkilled (silicon free) mild steel plate $^3/_4$ to 1 inch thick containing 0.04–0.12 C, 0.35–0.53 Mn, 0.006–0.008 N_2 was considered by Schottky[27] to be revealed by a bend test at 1350 to 1450° C. A steel with 0.049 P gave poor results, because, Schottky states, the phosphorus-rich segregations coagulate at high temperatures into phosphorus-rich globules which associate themselves in some unexplained way with a brittle phosphorus-rich intergranular material. There is no basis for the hypothesis although the experimental results are interesting.

A plate containing 0.021 S, 0.025 P cracked during water gas welding, the reason, according to Körber,[28] being that segregated areas containing 0.042 P, 0.051 S and perhaps high carbon were present. Tests by Diegel[29] on water gas welding plates containing 0.1 C, 0.4 Mn, 0.01 Si showed that up to 0.13 P was not a disadvantage if the sulphur content was low (0.04%). If the sulphur content was 0.08 to 0.11% bad water gas welds were obtained.

OTHER WELDING PROCESSES

During atomic hydrogen welding the phosphorus content of a high-phosphorus rod, Table 6, is greatly reduced, according to Weinman.[30]

A thermit weld in a roll (0.51 C, 0.27 Si, 0.50 Mn, 0.05 P, 0.045 S) was found by Granowski[31] to contain 0.53 C, 0.31 Si, 0.87 Mn, 0.101 P, 0.053 S. The high

Table 6—Atomic Hydrogen Welding with a High-Phosphorus Rod. Weinman[30]

Specimen	C	Mn	P	S	Proportional Limit Lb./In.²	Tensile Strength, Lb./In.²	Elongation, % in 2 In.	Reduction of Area, %	Fatigue Strength,* Lb./In.²
Rod	0.08–0.16	0.60–0.90	0.09–0.13	0.075–0.15	....	....	..	..	....
All-weld-metal	0.07	0.79	< 0.02	0.086	32,000	58,500	20.1	48	30,000
Single V weld on steel with 0.09 C, 0.50 Mn	0.07	0.78	< 0.02	0.056	21,500	63,750	20	63	20,000
Single V weld on steel with 0.35 C, $2^1/_4$ Ni, 0.40 Mo	0.08	0.74	< 0.02	0.045	35,000	73,750	13.2	20.4	20,000

*Rotating bend fatigue strength (10×10^6 cycles), machined specimens.

phosphorus content was believed to have been obtained from the cast-iron borings that were added to the thermit mixture. Notwithstanding the high phosphorus content, the welded roll produced 400,000 tons of steel without difficulty. For some mysterious reason, Heaton[16] had trouble making Benardos carbon arc welds in steel containing 0.15 C, 0.52 Mn, 0.093 S, 0.141 P.

OXYGEN CUTTING

A smooth oxyacetylene flame cut was made without difficulty by Wiss[32] in an iron containing 2% P. Wiss believed that phosphorus had no effect on oxygen cutting steel.

GENERAL OBSERVATIONS

Phosphorus has attracted surprisingly little attention in welding circles. Johnson[33] stated that 0.045 P in medium and low carbon steel, 0.04 P in high carbon and alloy steels, and 0.03 P in stainless steels has no effect whatever on welding. In their discussions of the impurities in aircraft steels, the German investigators, for example, Bollenrath[34] and Müller,[35] have sometimes seemed to imply that both phosphorus and sulphur are dangerous above 0.02%. However, it is always sulphur, never phosphorus, to which they refer directly as the undesirable impurity.

Without supplying details, Miller[36] stated that Bessemer steel with 0.10 P has excellent weldability. On the other hand, for fusion welding, Daeves[37] stated that 0.05 P was a disadvantage in mild steel. Bonsmann and Püngel[21] also believed that phosphorus impaired the fusion welding of mild steel. A vague reference was made by Reeve[38] to the presence at the junction of a weld with base metal (high-phosphorus industrial steel) of a line of phosphorus segregation.

According to a special subcommittee of A. S. T. M.,[57] phosphorus segregations or bands in wrought iron may cause a zone of brittleness, especially as a result of high heat influences in welding.

In welding high carbon steel (0.72 C) hoe tips to low-carbon steel (0.16 C) shanks (method not described), Powell[39] observed that an increase in the phosphorus content of the shanks from the usual limits to 0.06–0.10% reduced scrap welds from 10 to 1%. Powell offered no explanation. In agreement with Holzhäuser,[5] Kautny[40] observed that high phosphorus weld metal is viscous. A vivid theory of spatter based on the belief that phosphorus segregations in the electrode melt at relatively low temperatures was evolved by Atkins.[41] Another fanciful hypothesis was put forward by Booer,[42] who supposed that phosphorus in welds forms P_2O_5 which combines with water vapor to form corrosive phosphoric acid.

WELDING LOW ALLOY STEELS CONTAINING PHOSPHORUS

Copper-Phosphorus Steels

The Committee on Low Alloy Steels[43] supplies Table 7 for a steel (AW. 70–90) containing 0.15 C, 0.60 Mn, trace Si, 0.09 P, 0.42 Cu. The substitution of phosphorus for carbon assists in avoiding hardening of welds, according to Williams,[54] but the total content of phosphorus and carbon should not exceed 0.25% if the steel is to have adequate impact values.

Table 7—Tensile Properties of Welded Copper-Phosphorus Steel[43]

Specimen	Yield Strength Lb./In.²	Tensile Strength Lb./In.²	Elongation, % in 2 Inches
Unwelded, normalized	55,000 65,000	70,000 80,000	25
As-welded, bare plain-carbon steel electrode	63,000	78,000	28
As-welded, oxyacetylene; rod contained 3% Ni	55,000	73,000	16

Chromium-Copper-Phosphorus Steels

The addition of up to 0.15 P to chromium-copper steels does not affect the welding properties appreciably, according to Kinzel and Crafts[44] and Stuebing.[45] The latter recommends a special "Cor-Ten" electrode the properties of which are shown in Table 8. The steel contained 0.10 C max., 0.50–1.50 Cr, 0.30–0.50 Cu, 0.10–0.20 P, 0.10–0.30 Mn, 0.50–1.00 Si and was $^3/_8$ inch thick. The butt welds ($^1/_8$ inch spacing) were made with $^3/_{16}$-inch electrodes, 180 amp., reversed polarity.

Table 8—Cor-Ten Welded with Two Types of Covered Electrodes. Schramm[46]

	Unwelded	Weld Made with Special Cor-Ten Electrode	Weld Made with Covered Plain Carbon Steel Electrode
Tensile strength, lb./in.²	70,700	74,200	71,700
Elongation, % in 2 inches	33.0	17.5	19.7
Brinell hardness, max.	140 to 143	143	143

The tests summarized in Table 9 were reported by the Committee on Low-Alloy Steels.[43] The stress relieved

Table 9—Mechanical Properties of Oxyacetylene Welded Cr-Cu-P Steel[43]

Steel	Yield Strength, Lb./In.² As-Rolled	As-Welded	Stress Relieved	Tensile Strength, Lb./In.² As-Rolled	As-Welded	Stress Relieved	Elongation, % in 2 Inches As-Rolled	As-Welded	Stress Relieved	Charpy Impact Value, Ft.-Lb. As-Welded	Stress Relieved
A	48,000	44,000	47,000	72,000	73,000	72,000	46	32	33	23	33
B	57,000	52,000	52,000	74,000	73,000	73,000	41	25	33	..	..
C	50,000	51,000	51,000	77,000	76,000	72,000	33	24	25	..	..

Composition of Steels, Per Cent

Steel	C	Mn	Si	P	Cr	Cu
A	0.10	0.27	0.48	0.140	0.89	0.43
B	0.09	0.25	0.49	0.140	0.89	0.41
C	0.09	0.37	0.73	0.117	0.89	0.43

welds were heated at 650° C. for 1 hr. Stress relieving had little effect on the properties. The maximum local hardness in the joints was not over 96 B Rockwell. Welded 150-gallon tanks of 16 and 18 gage steel of the type shown in Table 9 were tested by Conrad,[47] who filled them with water and dropped them on a concrete slab from a height of 20 ft. No leaks occurred.

The static and impact tensile properties of 60° V butt welds in steel 1/2 inch thick containing 0.12 C, 0.40 Mn, 0.76 Si, 0.39 Cu, 0.16 P, 1.08 Cr have been determined by Warner,[48] Table 10, who used an electrode with an organic and mineral coating having 0.13 C in the core rod and depositing steel with 0.5 Mo. The static and impact tension specimens had a width and parallel section of 1 inch, and the welds were machined flush. Consequently, the test results indicate the strength of the Mo-alloy weld metal.

The ductility and impact value of Warner's welds were lower than base metal. Stress relieving at 600° C. was not beneficial. The maximum hardness in the heat-affected zone of a plate 1/2 inch thick, 3 x 9 inches with a bead of weld metal 3 inches long deposited by hand in the center at a speed of 4 to 6 inches per minute was 270 Vickers.

The phosphorus content of steel containing 0.10 C, 0.10–0.30 Mn, 0.5–1.0 Si, 0.50–1.50 Cr, 0.3–0.5 Cu, 0.10–0.20 P did not raise any difficulties in Jennings'[49] discussion of the alloy. The steel is not subject to air hardening and needs no preheating. According to Gibson,[50] arc welds in Cor-Ten steel have the same strength and ductility as unwelded, as-rolled base metal. Graf[51] found that the pulsating bend fatigue strength of oxygen cut surfaces in a steel 1.58 inches thick containing 0.13 C, 0.48 Cu, 0.13 Ni, 0.21 Cr, 0.48 Si, 1.14 Mn, 0.07 P, 0.016 S was 37,000 lb./in.^{2} When the irregularities of the oxygen cut surface were removed by shallow grinding the fatigue strength (2×10^6 cycles) was 65,400 lb./in.^{2}.

Table 10—Mechanical Properties of Arc Welded Cr-Cu-P Steel. Warner[48]

	As-Rolled	As-Welded	Unwelded but Stress Relieved	Welded and Stress Relieved
Elastic limit, lb./in.^{2}	43,800	46,600	46,200	41,600
Tensile strength, lb./in.^{2}	75,800	85,350	75,000	78,000
Elongation, % in 1 inch	59	25	59	26
Tensile impact value, ft.-lb.	875	744	836	707

Copper-Nickel-Phosphorus Steels

Butt welds in a commercial low-alloy steel sheet containing Cu, Ni and P were tested by Epstein, Nead and Halley[52] (see review of literature on Welding Copper Steels, Tables 4 to 6, Steel *A*). In view of the low carbon content it is not surprising that there was no air hardening. The tensile strength and notch impact value were high, but the tensile elongation of the machined welds in the thinner sheet was low, perhaps because the weld metal had inadequate ductility. The yield ratios of both Steel *A* and Steel *B* were unusually high for low-alloy steel.

Oxyacetylene welds in a steel containing 0.10 C, 0.61 Mn, 0.19 Si, 0.62 Ni, 1.15 Cu, 0.102 P had the properties shown in Table 11, according to the Committee on Low-Alloy Steels.[43] The maximum local hardness in the joint did not exceed B 96 Rockwell and the fractures occurred in base metal. Welding lowered the yield ratio from 0.85 to 0.75. Stress relieving lowered the tensile ductility. The steel was foolproof in welding, the Committee decided.

Table 11—Tensile Tests of Oxyacetylene Welded Hy-Steel[43]

Specimen	Yield Strength Lb./In.^{2}	Tensile Strength Lb./In.^{2}	Elongation, % in 2 Inches
As-rolled	65,000	77,000	33
As-welded	56,000	76,000	27
Welded and stress relieved 1 hr. at 650° C.	58,000	76,000	21

According to J. H. Nead (private communication, March 1939), the maximum hardness in the heat-affected zone of a weld in steel (0.09 C, 0.53 Mn, 0.23 Si, 0.103 P, 0.028 S, 1.04 Cu, 0.62 Ni) 3/4 inch thick made with heavy covered electrodes 1/4 inch diameter was Rockwell B 84. Unaffected base metal was B 82 and weld metal was B 89. Bend tests showed that base metal and heat-affected zone were more ductile than weld metal. The weld metal picked up 0.06 Cu, 0.06 Ni, 0.015 P from base metal which had no appreciable effect on the mechanical properties of all-weld-metal specimens.

J. H. Nead also has made spot welds so as to pull a button in tensile tests in steel 0.050 and 0.125 inch thick containing 0.08 C, 0.50 Mn, 0.035 Si, 0.098 P, 0.027 S, 1.07 Cu, 0.58 Ni. The welding time was 50% longer than for unalloyed steel.

Cu-Ni-Cr-P Steels

Tests were made by Epstein, Nead and Halley[52] on butt welds in a commercial low-alloy steel sheet containing 0.08 C, 0.48 Mn, 0.13 Si, 0.118 P, 0.025 S, 1.06 Cu, 0.50 Ni, 0.35 Cr (see review of literature on Welding Copper Steels, Tables 4 to 6, Steel *B*). There was no air hardening and the strength and notch impact value were good, but the ductility was a bit low. Phosphorus created no welding difficulties whatever.

Welds in a steel containing 0.08 C, 0.50 Mn, 0.10 Si, 0.25 Cr, 0.34 Ni, 0.50 Cu, 0.099 P made with a coated mild steel electrode had about the same tensile properties, Table 12, as unwelded base metal, according to the Committee on Low-Alloy Steels.[43] The maximum local hardness in the welded specimens did not exceed B 96 Rockwell, and fractures occurred in base metal. The Committee considered that the steel was foolproof from the standpoint of welding.

Table 12—Tensile Tests of Arc Welded Mayari R Steel[43]

Specimen	Yield Strength, Lb./In.^{2}	Tensile Strength, Lb./In.^{2}	Elongation, % in 2 Inches	Reduction of Area, %
As-rolled	52,000	69,000	30	61
As-welded	56,000	70,000	23	47
Welded and stress relieved at 570° C. (1050° F.)	52,000	66,000	25	59

Mn-Si-Cu-P Steels

Machine oxygen cut tension, bend and notch impact specimens of a steel 0.39 inch thick containing 0.12 C, 1.00 Mn, 0.62 Si, 0.26 Cu, 0.088–0.098 P, 0.024 S, 0.04 Ni, 0.00 Cr had at least as good properties in Dümpelmann's[53] tests as similar specimens prepared with a milling cutter.

ACKNOWLEDGMENTS

Contributions to the review were received from: R. H. Aborn, United States Steel Corporation; J. H. Nead, Inland Steel Company; W. A. Spindler, University of Michigan.

BIBLIOGRAPHY

1. Baatz, K., *Stahl u. Eisen*, **53**, 1149–1155 (1933).
2. Pomp, A., *Autog. Metallb.*, **21**, 64–66 (1928).
3. Coulson-Smith, C., *Trans. Faraday Soc.*, **20**, 163–167 (1924).
4. Séférian, D., *Rev. Soud. Autog.*, **26** (249) 4–6, Nov. 1934.
5. Holzhäuser, P. K., *Autog. Metallb.*, **23**, 266–275, 286–296 (1930).
6. Rimarski, W., Kantner, C., and Streb, E., *Forschungsarb. Geb. Ingwes.*, **317**, 44 pp. (1929).
7. *Chem.-Tech. Reichsanstalt Jahresbericht* **7**, 83–86 (1928).
8. Hobart, H. M., *Weld. Engr.*, **4** (12) 40–55 (1919).
9. Losana, L., *Metallurgia Italiana*, **26**, 391–403 (1934).
10. *Arcos*, **12**, 1193–1197, 1285–1291 (1935).
11. Stieler, C., *Stahl u. Eisen*, **58** (13) 346–350, Mar. 31, 1938.
12. Schoenmaker, P., *Poly. Weekblad*, **25**, 647–648 (1931).
13. Notvest, R., *Wire & Wire Prod.*, **11**, 527–551 (1936).
14. Okada, M., *Tetsu-to-Hagane*, **20**, 535–547 (1934).
15. Tafel, W., and Heil, W., *Stahl u. Eisen*, **47**, 1010–1011 (1927).
16. Heaton, T. T., *Proc. Instn. Mech. Engrs.*, 49–71 (1919).
17. Hughes, G. A., *Welding*, **3**, 84–87 (1932).
18. Speller, F. N., *Trans. A. S. M. E.*, **44**, 399–407 (1922); Proc. A. S. T. M., **24**, Part 2, 162–164 (1924).
19. McCaffery, R. S., *Year Book Amer. Iron & Steel Inst.*, 351–377 (1931).
20. Howe, H. M., *Metallurgy of Steel*, **1**, 73 (1890).
21. Bonsmann, F., and Püngel, W., *Werkstoffhandbuch Stahl u. Eisen*, **E11**, 1–5 (1930).
22. Ristow, A., Daeves, K., and Schulz, E. H., *Stahl u. Eisen*, **56**, 921–927 (1936).
23. Ledebur, A., *Handbuch der Eisenhüttenkunde, 1 Abt, 5Aufl., Arthur Felix, Leipsig*, 363–367 (1906).
24. Schäfer, R., *Die Konstruktionsstähle und ihre Wärmebehandlung*, Springer, Berlin, 125–129 (1923).
25. Holley, A. L., *Trans A. I. M. E.*, **6**, 101–124 (1877).
26. Mrázek, W., *Berg. u. Hüttenmann. Jahrbuch Bergakad. Pribram u. Leoben*, **20**, 406–519 (1870).
27. Schottky, H., *Kruppsche Monatsh.*, **5**, 1–6 (1925).
28. Körber, F., *Stahl u. Eisen*, **47**, 1157–1166 (1927).
29. Diegel, C., *Verein Beförderung Gewerbfleisses*, **79**, 233–250 (1918).
30. Weinman, R. A., American Welding Society Journal, **10**, (10) 12-18 (1931).
31. Granowski, O., *Mech. & Weld. Engr.*, **3**, 344 (1929).
32. Wiss, E., *Autog. Metallb.*, **22**, 46–53, 58–66, 74–79 (1929).
33. Johnson, J. B., *Airplane Welding, Goodheart-Wilcox Co. Inc. Chicago* (1929).
34. Bollenrath, F. and Cornelius, H., *Stahl u. Eisen*, **56**, 565–571 (1936).
35. Müller, J., *Luftfahrtfschg.*, **11**, 93–103 (1934).
36. Miller, J. R., *Blast Furnace & Steel Plant*, **18**, 1602–1605 (1930).
37. Daeves, K., *Stahl u. Eisen*, **53**, 1155 (1933).
38. Reeve, L., *Jnl. Iron & Steel Inst.*, **135**, 143P (1937).
39. Powell, R., *Welding*, **3**, 469–470 (1932).
40. Kautny, T., *Autog. Metallb.*, **17**, 139–141, 150–154 (1924).
41. Atkins, E. A., *Weld. Engr.*, **7** (7) 32–38 (1922); **10** (1) 23–25, 39–41 (1925).
42. Booer, J. R., *Chem. Trade Jnl.*, **74**, 189–191 (1924).
43. Critchett, J. H., The Welding Journal, **17** (1) Suppl., 8–14, Jan. 1938.
44. Kinzel, A. B., and Crafts, W., *Alloys of Iron and Chromium*, 1, McGraw-Hill Book Co., N. Y. (1937).
45. Stuebing, A. F., *Rwy. Mech. Engr.*, **111**, 517–520 (1937).
46. Schramm, G. N., Taylerson, E. S., and Stuebing, A. F., *Iron Age*, **134**, 33–38, Dec. 6, 1934.
47. Conrad, C. S., *West. Machy. & Steel World*, **26**, 179–180 (1935).
48. Warner, W. L., American Welding Society Journal, **15** (10) Suppl., 21–32(1936).
49. Jennings, C. H., *How to Weld 29 Metals, Westinghouse E. & M. Co.*, 100 pp. (1937).
50. Gibson, A. E., American Welding Society Journal, **14** (9) 2–8 (1935).
51. Graf, O., *Autog. Metallb.*, **29**, 49–57 (1936).
52. Epstein, S., Nead, J. H., and Halley, J. W., *Trans. A. I. M. E.*, **120**, 309–339 (1936).
53. Dümpelmann, R., *Autog. Metallb.*, **30**, 219–223 (1937).
54. Williams, C. E., *Iron & Steel Inst. Advance Copy* No. 16, Oct. 1938, 14–16.
55. LeGrix, M. G., *Rev. Soud. Autog.*, **19**, 1383–1385, July 1927.
56. Linetsky, I. A., *Vestnik Metallopromish.*, **18** (2) 109–124; (3) 135–149 (1938).
57. *Proc. Amer. Soc. Testing Materials*, **34**, Part I, 144 (1934).
58. Henning, C. C., *Trans. A. I. M. E.*, **116**, 137–158 (1935).

SUGGESTED RESEARCH PROBLEMS

1. Several explanations have been offered for the beneficial effect of phosphorus on the forge welding of Bessemer steel. The phosphorus may vaporize to protect the surface or may form a fusible phosphate. The precise rôle of phosphorus has not been made clear.

2. Methods and welding rods for depositing weld metal indentical in composition with the low-alloy steels containing phosphorus do not appear to have been developed.

3. The effect of phosphorus on the properties of spot and seam welds in mild steel sheets has not been investigated.

4. Despite the lack of interest in phosphorus displayed by welding investigators, they often have relied upon phosphorous segregations to provide an explanation for otherwise mysterious difficulties. Whether the phosphorus was the main difficulty or whether it was the associated inclusions was never determined. Further, it is not plain whether the effect of segregated phosphorus on melting point or on brittleness was the more important.

A New Spot Welder for Shipyards*

By O. WUNDRAM

IN CONTRAST with arc welding, resistance welding has not made much progress in shipbuilding despite the great similarity of spot welding to riveting. The use of resistance welding in shipbuilding has been limited on mechanical and electrical grounds by the massive machines that are required. Massive machines are not readily portable and the high currents which are needed must be provided by transformers and cannot be transmitted over large distances. Furthermore, high pressure is required to press together the parts to be spot welded.

The conditions under which spot welding machines operate advantageously, namely: series production of similar and rather light parts, are not fulfilled in the shipyard. However, the advantages of spot welding from the standpoints of metallurgy, strength and substitution of unskilled for skilled labor have provided the urge for the present tests, which have been quite successful.

The customary spot welder, of whatever size and manufacture, is limited in possible depth of throat because electrical and magnetic conditions become so severe with deep throats that welding may not be suc-

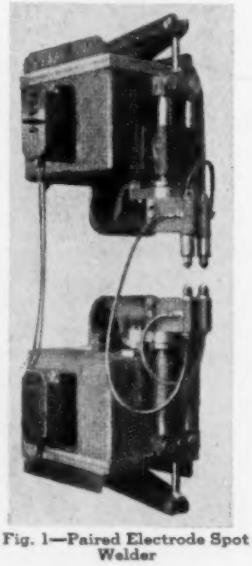


Fig. 1—Paired Electrode Spot Welder

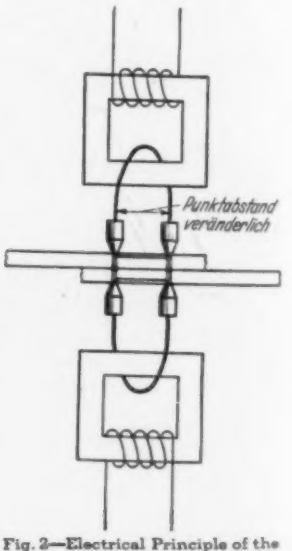


Fig. 2—Electrical Principle of the Paired Electrode Spot Welder. The Distance Between Electrodes Is Variable

*From "Eine neue Punktschweissmaschine für den Schiffbau" published in *Werft-Reederei-Hafen*, **20**, 22–23, Jan. 15, 1939.

cessful. The best means to avoid the throat limitation in shipyards is to adopt paired electrode welding. Two transformers are used each with two electrodes. The type of machine is illustrated in Fig. 1; the principle of operation is shown in Fig. 2. Other operating principles may be adopted for paired electrode machines but have not been found necessary for shipyard welders.

Each transformer in Fig. 1 develops 75 kva. and has five taps on the high voltage side. Special electrodes have been designed for welding ship plate. The machine is rated to weld two plates each 0.47 inch thick. To bring the stiff plates into good contact electrodes are used which are actuated by pneumatic pistons. Small irregularities in the plate are overcome by a lever clearly visible above the upper pair of electrodes in Fig. 1. Since it is necessary to vary the distance between spot welds, the electrodes may be revolved about a vertical axis. Electrode pressure is controlled by either pedal or push button.

As usual the hard copper electrodes must be water cooled, which is not a source of difficulty in shipyards. The voltage between the electrodes amounts to only a few volts. The voltage of the control mechanism for compressed air and welding current is only 42 volts. Consequently, electrical hazards practically do not exist. Compressed air is supplied at 85 lb./in.^{2} The primary voltage of the transformers is either 380 or 550, depending on the source of power. Under maximum load—two plates of ship steel each 0.47 inch thick—two spot welds 0.39 to 0.47 inch diameter are made in several seconds at a current of 8000 to 10,000 amp.

It should be emphasized that the surface of the plates requires no special preparation. Even rust proof paint between the sheets need not be removed. Should current not flow at once from one plate to the other, the current will flow to the adjacent electrode. The resulting heat burns all paint or oil in the vicinity, a special advantage of paired electrode machines.

In adapting the machine shown in Fig. 1 to shipyards the two transformers are supported in a way that plates of any width may be inserted between the electrodes. A shelter may be constructed to protect the transformers from the weather. A deep-throat frame (throat 6 to 10 ft. deep) may be constructed to support the two transformers. The rate of production depends primarily on handling methods, since welding occupies only a few seconds. If necessary, the transformers may be movable so as to move along the plates. On the other hand, it does not pay to make the paired electrode machine fully automatic, because variations in pressure, time, and surface quality are too large and frequent in shipyards. For that matter the push button or pedal is a convenient control system. The machine is particularly adapted to the fabrication of bulkheads, stiffeners, and to the attachment of angles to plates. A smaller machine (two 30 kva. transformers) is available for the production of steel doors, two steel sheets being laid over a framework.

Cracks Caused by Corrosion of a Welded Aluminum Tank*

By M. BOSSHARD

THE corroded tank (14 feet long, 5 feet wide, $1^1/_2$ feet deep) was used for the following liquids, sometimes hot, sometimes cold.

1. Ammonium sulphide solution with less than 1% $(NH_4)_2S$
2. Aqueous sulphuric acid with less than 1% H_2SO_4
3. Soapy water.

The maximum temperature was 60–80° C. The tank was made of commercial aluminum 0.08 inch thick (99.44 Al, 0.30 Fe, 0.25 Si, 0.006 Cu, 0.005 Zn, trace of titanium and manganese). After two years in service the cracks shown in Fig. 1 were found. The aluminum was welded in the half-hard, cold-rolled condition and was softened near the weld.

As shown in Fig. 1, which depicts the specimen cut from the tank that was available for investigation, the lower part was uniformly and considerably roughened by corrosion. On the other hand, the upper part was not corroded to so great an extent. The most numerous and pronounced cracks were found at the approximate probable water line.

The main crack was nearly straight and followed the softened zone. In a zone $^3/_8$ inch wide parallel to and on both sides of the main crack were numerous fine cracks detectable with a magnifying glass which did not penetrate through the tank as the main crack did. These parallel cracks were especially numerous at area B Fig. 1, which contained a number of deep scratches, created by a scraper. The cracks often followed the sharpest scratches. The broader scratches, however, were merely widened by corrosion. Microscopic examination showed that the cracks occurred in the softest zone that was recrystallized at the lowest temperature during welding. The cracks were not intercrystalline. The weld had a satisfactory, fine grained structure and was not corroded to any greater extent than the plate outside the affected zone.

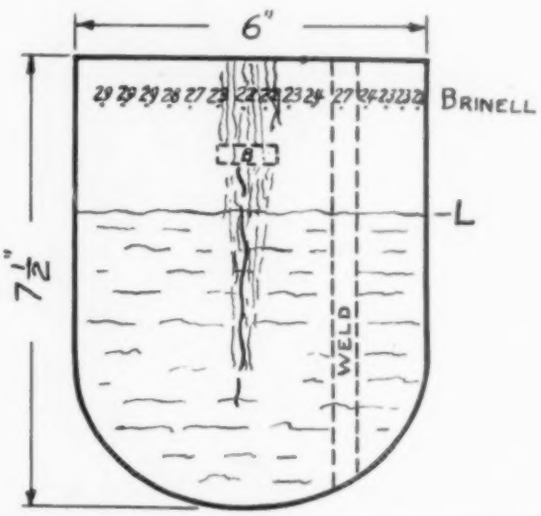


Fig. 1—Specimen Cut from Corroded Aluminum Tank, 0.08 Inch Thick
L = Probable liquid level. Above *L* there was little corrosion. Below *L* the surface was roughened by corrosion

(*Continued on page 152*)

* Translation of "Durch Korrosion verursachte Rissbildung an einem geschweissten Aluminium-Behälter." Report issued by Aluminum-Industrie A.G., Neuhausen, and published in *Korrosion und Metallschutz*, 15, 35–36, Jan.–Feb. 1939.

USING "CONTROLLED" Predistortion to Produce Straight Headers*

By GERARD PESMAN†

INTRODUCTION

CONSTRUCTING headers up to six or eight inches in diameter is a common job in any welding shop where there is much pipe welding to be done. The generally accepted method of constructing such a header is to clamp the header pipe to a heavy beam, lay it out, cut the openings for the branches and then tack weld the parts together. The header pipe is then released and small metal blocks of varying thickness placed under each branch in order to bend the pipe in a direction opposite to the distortion caused by the contracting weld metal. The pipe is clamped down over these blocks and the branches welded in. When the metal has cooled so that it is no longer hot to touch, the header is released and is supposed to be straight. Generally, however, the results are good, bad or indifferent, depending upon luck and the experience of the men doing the work. Uniformly good results are practically non-existant, and although occasionally a header turns out nearly perfect, often the product requires some straightening. The actual amount of blocking required for any one job is largely a matter of guess. The general condition is well illustrated by Old John's reply when asked how much blocking he used when welding up a header, "I block her up till she looks about right then hope for the best." The result of such a condition is that many of the headers made will require straightening and consequently never fit as they should, many more will require at least some additional time on the part of the pipe fitters in order to make them fit without serious leakage or excessive strain on flanges or fittings, and only a few will be entirely satisfactory. Although a header may be relatively easy to pull into place by means of the fittings, it is quite easy to cause stresses in the material well over that generally assumed as being safe. This report describes a method developed by the author telling how most of the guesswork may be eliminated, making it possible to produce headers that are straight within much closer limits than those generally achieved previously. Two methods have been developed, one for shops that handle a relatively large amount of pipe work, and one that can be used in places where the need is only occasional. Since it was possible in the limited time available to determine the necessary constants for only a very small part of the entire field of sizes and grades of pipe and tubing, the methods of determining the necessary constants and data for the two methods are also included.

* Contribution to Fundamental Research Division of Welding Research Committee.

† Inst. Mech. Engrg., Montana State College, Bozeman.

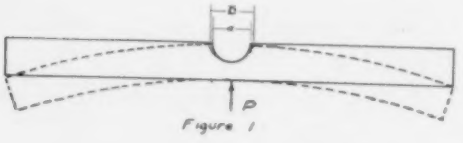


Figure 1

THE GAP GAGE METHOD

The first method has, for want of a better name, been called the gap gage method. Because it requires an investment of approximately fifty dollars for gages and jacks, it would probably be used only in the larger shops which do a relatively large amount of pipe work.

When a pipe that has the opening for a branch cut in it is distorted by a force P, Fig. 1, the width of the opening parallel to the axis of the pipe will increase a few thousandths of an inch.

This change in dimension can be taken as a measure of the distortion caused by the blocking generally introduced under the opening in the present procedure. Once the necessary change has been determined by test, it will be possible to always distort the header pipe just the right amount to offset the welding regardless of the length of header or number or size of branches.

It would not be possible, however, to measure accurately the width of the gap itself because of the rough edges left by the cutting torch. Also the branches must be tack welded into place before the header is distorted, thus eliminating the possibility of measuring the opening directly. If, however, two center punch marks—say, six inches apart for four-inch pipe—are placed one on each side of the opening and the increase in distance between these two points is measured, the same result can be achieved. The equipment necessary then, beyond a header beam, which is usually available in every welding shop, is the gap gage, Fig. 2, and the dual center punches, Fig. 3. A set of end blocks and jacks similar to Figs. 4 and 5 will save much trouble. The end blocks can be easily built from scrap material and ordinary planer jacks can be substituted for the special jacks if desired. The jacks shown above were built for a header beam such as is shown in Fig. 6. This set up consisted of two fifteen-inch I beams twenty feet long, spaced one inch apart between flanges and then welded together with spacer plates. The one-inch gap serves as a convenient centering device and guide slot.

STEP BY STEP PROCEDURE FOR GAP GAGE METHOD

1. The cut to length pipe for the header is placed on the end blocks (a) Fig. 7, with a jack (b) in place under the approximate position of each proposed branch.

2. The ends of the pipe are clamped down on the blocks. Since the jacks will push up against the clamps, there is no reason for clamping the pipe down tight.

Fig. 2

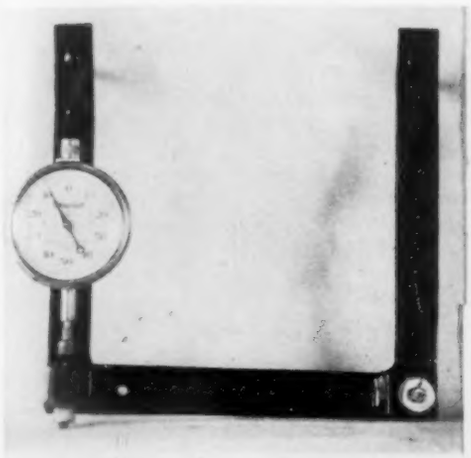


Fig. 3

Fig. 4

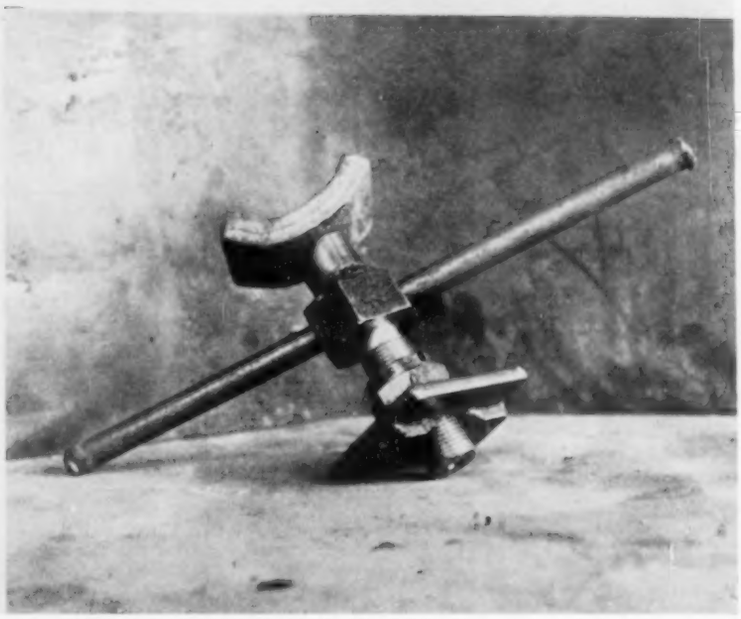


Fig. 5

The bolts need only be drawn down snug, in fact, they should not be tighter than this or they will prevent the pipe from deflecting as it should.

3. All the jacks are run up by hand until they are just snug against the pipe. Since a four-inch pipe will deflect approximately 0.018 inch when the opening for a two-inch branch is cut into it and 0.035 for a four-inch branch, it is necessary to run the jacks up into place to prevent this initial distortion, so that the layout may be made properly and the branches tacked into place with the pipe straight (Fig. 8).

4. The layout for the header is made and the outline of the openings marked.

5. Using the dual centerpunch (*c*) Fig. 9, the punch marks are placed on the pipe, one on each side of the proposed openings.

6. The openings are carefully cut with an oxyacetylene cutting torch.

7. The branches are placed in position, the flanges leveled or branches plumbed and tack welded into position with a long tack on each side as shown on Fig. 10. It is important that no other tacks be placed until after the header pipe has been distorted. To do so would interfere with the free spreading of the gap when the header is given its proper reverse bend.

8. Take a zero reading with the gap gage across each branch and record the zero reading on or near the branch.

9. With the jacks spring the pipe until the gap gage shows the correct reading across each branch. These readings will of course have to be corrected for the initial zero readings. The correct readings for four-inch standard pipe are as follows:

Table 1—Gap Gage Readings in Thousandths of Inches

Branch	4-Inch Header	Actual Blocking
4 Inch	−33	0.114 Inch
3 "	−17.5	0.109 "
2 "	− 7	0.065 "

Some difficulty may at first be experienced in getting the jacks properly adjusted because changing the setting of one will change the reading across some of the other branches.

10. Finish tacking the branches and complete the welding.

11. Allow the header to cool before removing from the beam.

Accuracy

Using this method, if the distances across the openings have been carefully measured and set to the correct amount, the error in four feet across any one opening should not be more than 0.005 inch unless the type of welding or materials are quite different from those used for this test. Standard weight lap welded steel pipe was

Figure 6

used for this test. The welding procedure consisted of two beads, a penetration bead followed by a strength bead using $^1/_8$-inch coated rod, 75 volts on the 100-amp. tap for the first bead and $77^1/_2$ volts on the 75-amp. tap for the second bead. The resulting weld had a maximum width of from $^3/_8$ to $^1/_2$ inch.

THE PROTRACTOR METHOD

This method should be satisfactory for the smaller shops. In general it would be necessary to have three or four protractors, one for each size of pipe generally used, and for that reason the cost should not be high.

The basic idea is this: When the pipe is distorted, it will assume *nearly* the same curve for each size of branch opening, with the exception of the maximum deflection. If then, a protractor is constructed such that with it the curve the pipe will assume when it is properly distorted can be determined on a small scale, the blocking required

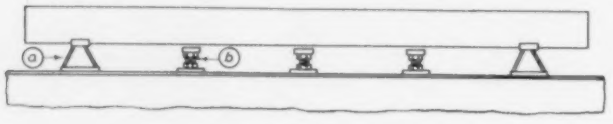


Figure 7

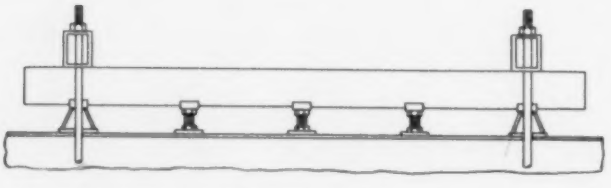


Figure 8

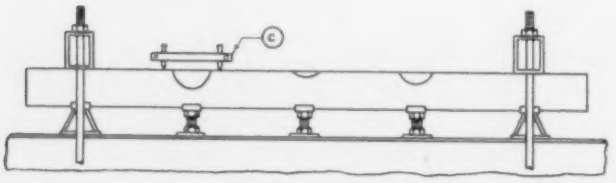


Figure 9

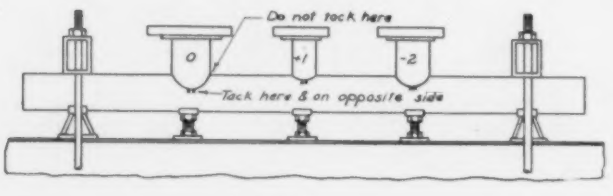


Figure 10

can be scaled off of this diagram. No other equipment beyond the header beam is necessary with the exception of the small metal blocks for blocking.

Suppose the following header is to be constructed (Fig. 12). Using the protractor, Fig. 13, and starting at the left end of the header, the curve is drawn for the first 12 inches with the protractor, as shown in Fig. 14*a*. The protractor is then reversed and with zero again on the first branch position and the 4 on the left end of the protractor in line with the curve, the curve is drawn halfway to the position of the next branch (14*b*). The positions of the perpendicular-to-the-tangent points are also marked with a small circle through the holes in the protractor. Reverse the curve again, (14*c*), line up the perpendicular points and draw the curve to the

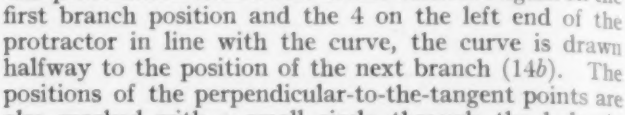
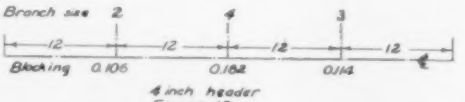


Figure 11



4 inch header
Figure 12

next branch. Continue in this manner, setting the numbers 2, 3 or 4, on the curve, depending on the branch size, until the curve for the entire header has been drawn. The completed curve then should look like Fig. 14*d*. Draw a straight line connecting the two ends and with the scale on one end of the protractor measure up from the straight line to the curve at each branch position for the number of hundredths of blocking required.

From here then the procedure is identical with that ordinarily used for constructing a header. The pipe for the header is clamped to the beam, the layout made, openings cut and the branches tacked in place, using long side tacks. The header is then released, the proper amount of blocking inserted under each branch, the header again clamped down and the welding completed.

Accuracy

The accuracy achieved with this method will not be quite as good as that for the first method, but it will produce headers that are much more satisfactory than those made by cut and try. The header shown on Fig. 12 was made using the method described above and showed a maximum error of only 0.014 inch in four feet when complete.

SINGLE JACK METHOD FOR LINE T OR Y JOINTS

Often it is necessary to make T or Y joints in place on a pipe line. In such a case it is, of course, impossible to use a header beam. If it is important that the main line continue straight, as might well be the case for a service line in a building, the device shown in Fig. 15 may be used. The clamps supporting the ends of the truss rods are clamped to the pipe, and the jack used to distort the pipe the required amount as measured by a gap gage. The equipment can be used in a limited space and should require only one man to handle it.

DETERMINING THE CONSTANTS FOR THE GAP GAGE METHOD

Determining the necessary constants for this method is not a particularly difficult piece of work, but does require care to obtain reliable results.

Equipment

With a little scheming the equipment necessary for this work can be arranged for almost anywhere. A gap gage, a rider beam (Fig. 17) a standard dial gage reading to thousandths of inches and some means of applying and measuring the load applied at the opening is all that is necessary, outside of welding and cutting equipment and the header beam. A regular beam testing machine was used for applying the pressure in this work, but other means could be easily arranged. Figure 16 shows the general set up used, and Fig. 17 shows a possibility where such equipment is not available.

Procedure

The following steps are necessary.

1. Take the piece of header pipe 5 feet long and lay out on it the points 4 feet apart for the location of the rider beam. Center punch and drill one end for the cross angle, file a small flat for the cross angle to seat on, then

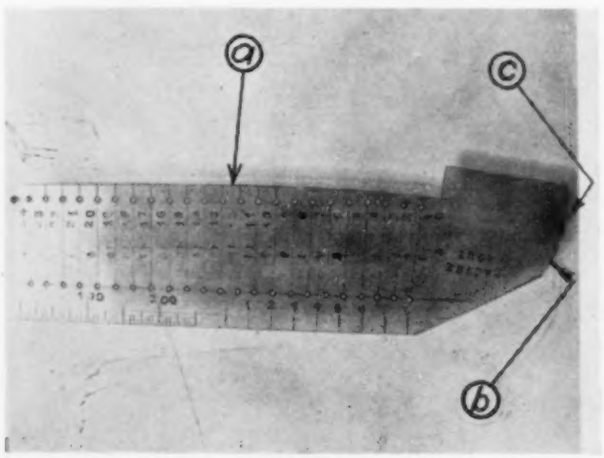


Fig. 13

attach the angle with a small bolt. Set the rider beam on it, then, using the rider beam as a compass, locate the position of the single-point support and center punch the position. Using the dial gage, set the rider beam so that it is parallel to the top surface of the pipe by adjusting the single point support and record the zero reading at the center. The layout for whatever size branch is being tested is made diametrically opposite the rider beam set up. Figure 18 shows the layout complete, preparatory to cutting the opening for the branch. The opening is then cut, the pipe allowed to cool to room temperature and the deflection caused by cutting is measured and recorded.

The second step is to clamp the pipe down flat on the header beam to eliminate the cutting distortion and then weld in the branch. After cooling, the resulting deflection caused by the welding is measured, as was done for the cutting.

The third step requires the use of a second piece of pipe as large as the first. Repeat the procedure used for the first pipe through the layout for the branch opening. Using the dual center punch, place a center punch mark on each side of the proposed opening and take a

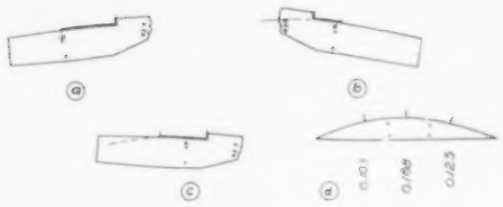


Figure 14

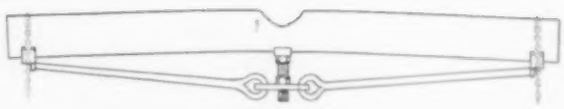


Figure 15

zero reading with the gap gage. Cut the opening, cool and measure the rider beam deflection to check the first determination.

The pipe is then mounted on the beam machine (or your substitute) with the gap down. Diagrammatically the condition of the pipe now is as shown on Fig. 19.

The distortion due to the cutting must be taken care of before any effective distortion in the elimination of that caused by welding is obtained. A load is applied sufficient to bring the pipe back through zero distortion and in the opposite direction an amount equal to the distortion caused by the welding. We now have the condition shown in Fig. 20.

When the distortion caused by the welding was measured on the first pipe, the pipe was straight beyond the weld. See the dotted lines on Fig. 20. Now the pipe has some curve because of the load placed upon it, and this curve is operating in an effective direction opposite to that in which we are deflecting the pipe. In other words, to obtain the correct deflection to obviate the distortion caused by the welding, it is necessary for the total deflection from zero to equal the deflection to offset the welding plus that due to the load imposed on the pipe. A load-deflection curve calculated by the equation $Y_{max.} = \frac{Pl^3}{48EI}$ where

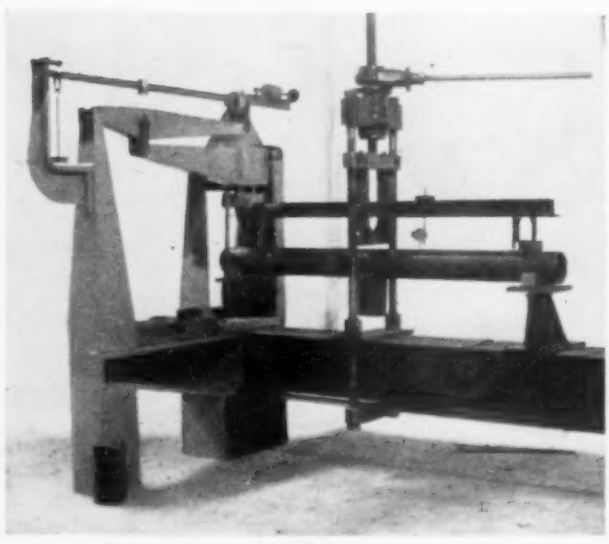


Fig. 16

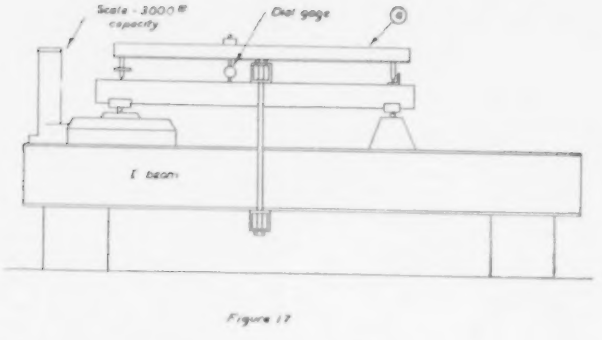


Figure 17

Figure 18

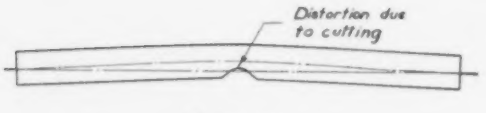


Figure 19

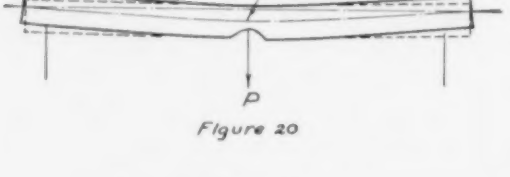


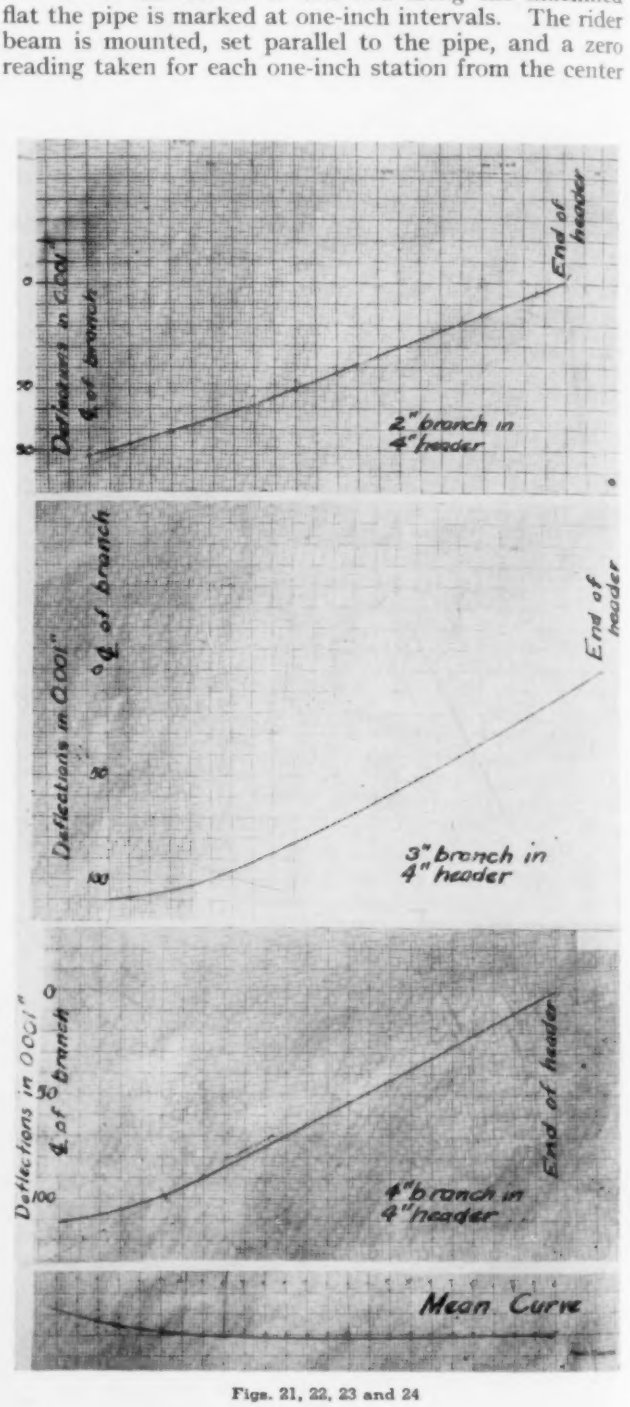
Figure 20

Y = Deflection
P = Load in lb.
l = Total length in inches
E = 29,000,000
I = Moment of inertia of pipe

and plotted on the basis of no opening cut in the pipe makes this a relatively simple procedure. The total deflection being known and the load known, it is easy to determine the sum of the welding deflection needed and the load deflection to see if it equals the total. When this is the case, the distance between the center punch marks is measured and recorded as the gap gage figure necessary when distorting a pipe on the header beam. It should be noted that zero deflection in this case is considered as the zero recorded on the original uncut pipe. The distortion caused by cutting would be considered opposite in sign to that for the elimination of welding distortion plus load deflection. Preferably a check should be made by actually welding up this test piece with the total blocking determined above, measuring the gap and then checking the pipe for straightness after it has cooled. The entire test then will require ten feet of pipe for any one header and branch combination. It is possible that the time, material and labor may later be reduced by the development of a shorter method which so far has not been adequately checked. For pipe up to six inches in size, a length between supports of four feet is satisfactory, but for pipe size over six inches the length should be increased depending on the total force required to deflect the pipe the correct amount.

DETERMINING THE CURVE FOR THE PROTRACTOR METHOD

The equipment necessary for this work is similar to that needed for the previous method except the gap gage and dual center punch are not needed. It is, however, necessary to machine a flat on the side of the pipe opposite the position of the branch. This flat furnishes a smooth surface for the stem of the dial gage to operate on. From the center to one end along the machined flat the pipe is marked at one-inch intervals. The rider beam is mounted, set parallel to the pipe, and a zero reading taken for each one-inch station from the center



Figs. 21, 22, 23 and 24

out to one end. The procedure then is identical with that for the gap gage method down to the point where the final gap gage reading is taken on the deflected pipe. At this point deflection curve readings are taken at the one-inch intervals.

A curve of this nature must be determined for each size branch that is to be used with this particular size header. When all of these curves have been determined they are plotted to a large scale on profile paper as shown by Figs. 21, 22 and 23. These sheets are then placed one on top of another on a sheet of glass above a strong light and shifted about until all the curves correspond to one another as closely as possible. Two points that match quite closely are selected and through these points on each curve a straight line is drawn. Using these lines as a base, all the curves are transposed to another sheet and a median curve plotted through the points. This curve is the one used for plotting the protractor (see Fig. 24).

Making the Protractor

A scale of $^1/_{100}$ inch equal to $^1/_{1000}$ inch for the vertical measurements and $^1/_4$ inch = 1 inch for the distances along the header will give a protractor of convenient size and will require a piece of pyralin about three inches wide and eight inches long. The curve, (*a*) Fig. 13, is first plotted along one side of the blank. A steel scale, dividers and a magnifying glass should be used when plotting the curve. The curve plotted, the next step is to determine the lines perpendicular to the curve at each inch point. These perpendiculars may be determined by the mirror method or by the following procedure.

Three points at equal distances along the curve on each side of the initial point are chosen (see Fig. 25). Choosing a convenient radius, arcs are swung from each of the six chosen points. A curve is plotted through the intersections of these arcs. A final arc is then swung from the initial point and a straight line from the intersection of this arc with the curve plotted through the arc intersections will be a perpendicular to the curve at the initial point.

The perpendiculars are scribed for a distance of about $1^1/_2$ inches from the curve. Two $^1/_{16}$-inch holes are then drilled on each perpendicular. The two scales for the blocking required, one in hundredths to represent thousandths of inches and the other to represent blocking in 32nds of inches, are plotted using a steel scale. The curved portion, (*b*) Fig. 13, has its center at the zero point on the curve and has a radius corresponding to seven inches on the curve, one of the common points on the original three curves. A line from the end of this arc to the end of the 32nd scale to get rid of excess material completes the outline of the protractor. It is jig sawed and sanded to shape, particular care being taken to make the curve portion exact. After cutting to shape, the notches, (*c*) Fig. 13, are located and filed in. The location is found by constructing two lines perpendicular to one another, locating to scale on the vertical perpendicular the three points corresponding to the correct amount of blocking for each size of branch, 0.065 for a two-inch branch, 0.109 for a three-inch and 0.114 for four-inch, in this case. Curves from these points are drawn to the horizontal line with the protractor. The

Figure 25

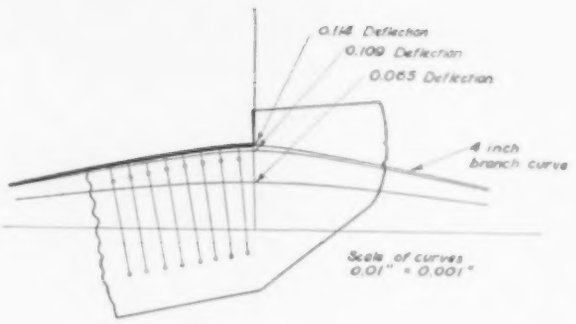
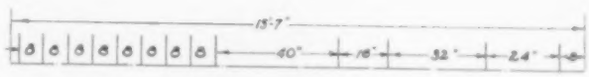


Figure 26



Header & branches all 3 inch
Figure 27

point where the curve appears from under the curved portion of the protractor when the zero is placed on each of the three points is the location of the notch for that particular size branch (see Fig. 26). Stamping the numbers for the scales and curve completes the protractor.

ECONOMIC JUSTIFICATION FOR THIS WORK

There may be serious doubts in some minds as to the justification for such procedures in order merely to eliminate some distortion. It may be argued, first, that in many cases even though headers are slightly out of line, they can be pulled into place without too much trouble. Further, it may be argued that as the size of the pipe or the thickness of the metal increases, the problem of distortion decreases quite rapidly and gives way to one of stress concentrations. Examination of a few examples will, however, prove that the use of either method can be economically justified. The first example is of the header having the general dimensions shown in Fig. 27. The header was welded up in a commercial plant by a commercial welder to see how much distortion might take place when no blocking was used. The header was merely clamped down so that it was held straight during the welding procedure. The final result was a header with a total distortion at the center branch of $1^9/_{16}$ inches. The cost of gas and labor, not counting overhead, required to straighten the header enough to make it usable was approximately $1.50. This expenditure was a pure waste.

Of course it must be admitted that such figures would not apply to headers that have been predistorted even approximately. However, the table below shows the final result for a group of five typical commercially produced headers.

4-in.	header	184 in.	long	5–2 in.	branches	$^1/_2$ in.	distortion
3 "	"	184 "	"	5–2 "	"	$^1/_4$ "	"
4 "	"	52 "	"	4–4 "	"	$^1/_4$ "	"
3 "	"	124 "	"	10–3 "	"	$^3/_{32}$ "	"
4 "	"	52 "	"	4–2 "	"	$^3/_{32}$ "	"

Any one of these headers could be pulled into place during the assembly process, but a short analysis shows that the fiber stress produced is worth sidestepping if possible. A short calculation shows that a bow of 0.076

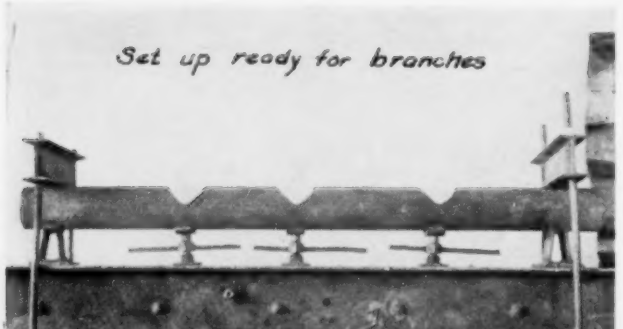


Fig. 28

inch in a distance of 48 inches, the distortion caused by welding a 4-inch branch into a 4-inch header without using blocking, requires a load of approximately 7300 pounds to bring it back to a straight line. This means imposing a tensile stress of 24,600 pounds per square inch which might well be eliminated, thus leaving that strength available in case of an unlooked-for emergency.

Suppose we look at the situation from a slightly different angle. The cost of the equipment over and above the header beam, which would usually be available, is about fifty dollars for the gap gage method. If the use of this equipment would prevent each year the breakage of only two ordinary 125 lb. C. I 4-inch flanges costing about $3.20 a pair, the return on the investment would still be nearly 6½%. Surely such a saving is justifiable.

When welding single branches into line pipe that cannot, of course, be placed on a beam, one of the schemes occasionally used to prevent serious distortion is to heat the opposite side of the line to almost a dull red and keep it hot while the welding is being done. Such a procedure will require considerable gas, besides the use of an extra man. All this material and labor could be saved by using equipment similar to Fig. 15. The process described above will require nearly a half hour, and the cost of labor plus gas will figure up to about three dollars. The equipment of a type necessary to eliminate this waste has not been built here to date, but it should not cost more than thirty dollars. If the equipment can be used only two or three times a year, it will return good dividends on the investment. Just how large the total savings per year to the industry would be I have no way of estimating, but believe it would be enough to justify the adoption of the systems outlined. The savings to the public would appear either in lower costs or better service.

WELDING RESEARCH COMMITTEE

Research Program to Study the Weldability of Plain Carbon Steels—(with particular reference to the Effect of Carbon and Manganese)

THE Welding Research Committee of the Engineering Foundation has received requests from a number of sources in the welding industry for information as to the weldability of plain carbon steels of varying carbon and manganese contents. An important request for information of this nature originated with a consideration by the American Society for Testing Materials of the weldability of steels conforming to the standard specifications of that society. A comprehensive survey of available information on this subject was made by the Welding Research Committee, but no satisfactory correlation of available data could be made. The desirability of obtaining quantitative data was indicated and a research program was developed to study the Effect of Carbon and Manganese on the Weldability of Plain Carbon Steels. The results of this test program will present data, for steels throughout a range of carbon and of manganese, on the following:

(1) Effects produced in the heat affected region of the base metal as revealed by changes in hardness, toughness, microstructure and tendency to cracking for (*a*) fusion welding, and (*b*) gas cutting.
(2) Variation in the physical properties of welded joints made in the various steels.
(3) Variation in the physical properties of steel plates throughout the selected range of chemical analyses.
(4) Influence of plate thickness (within a range of ¼ inch to 1 inch) on items (1), (2) and (3).
(5) Influence of important variables in welding procedure, such as preheating temperature, on items (1) and (2).
(6) Influence of heat treatment on items (1), (2) and (3).
(7) Comparison of the weldability of steels of inherently coarse grained type and inherently fine grain type.

The accumulated data on the above items will serve the following purposes:

(1) Permit engineers to select and specify a steel possessing the highest physical properties consistent with limitations of welding procedure arising from its composition and from the nature of the structure to be welded.
(2) Permit specification writing bodies to designate suitable commercial limits of chemical composition for steels of standard specifications which are to be welded.
(3) Permit welding engineers to prescribe welding techniques for best results and to exercise any necessary precautions in the welding of a steel of given carbon and manganese contents.
(4) Establish a basis upon which steel makers and fabricators may discuss welding problems of mutual interest.

(5) Establish basic data to which may be correlated the effects of additive alloying elements in the base material, as in the welding of low-alloy steels.
(6) Determine the suitability of various tests as criteria for the determination of the weldability of a steel.

OUTLINE OF RESEARCH PROGRAM

A—*Base Material*

Twenty compositions of steel will be studied, comprising five manganese ranges for each of four carbon ranges. The carbon ranges will extend from 0.10 to 0.45% and the manganese from 0.30 to 1.70%. Since only a limited quantity of steel of each composition is required, it is practically impossible to obtain the required twenty compositions from commercial open-hearth heats made under comparable steel melting practice. The twenty compositions are, therefore, to be obtained from the small experimental open-hearth furnace of the Jones and Laughlin Steel Corporation, in which steels may be made under controlled and reproducible melting practice.

B—*Base Material for Preliminary Investigation*

A preliminary investigation will be made of the welding characteristics of the experimental steels in comparison with commercial open-hearth steels of both inherent fine and coarse grained types. Three compositions, representing relatively low, medium and relatively high combinations of carbon and manganese, will be represented in each type of steel; that is, (*a*) experimental, (*b*) commercial open hearth, coarse grained and (*c*) commercial open hearth, fine grained.

C—Base Material for Main Program

If the behavior during welding of the experimental open-hearth steel is substantially the same as that of the commercial open-hearth steels of similar composition, the main portion of the program will be undertaken using steel produced in the experimental open-hearth unit. This main portion of the program will be comprised of the remaining seventeen compositions.

D—*Plate Thickness*

Three thicknesses of plate, 1/4 inch, 1/2 inch and 1 inch, will be represented in each composition of steel.

E—*Welding and Gas Cutting*

The required welding will be performed under prescribed reproducible conditions. The conditions will be varied to produce information on (*a*) effect of base metal temperature at time of welding over a range of −10 to 300° F., (*b*) effect of welding speed; that is, rate of travel of arc, (*c*) single bead deposition *vs.* multiple bead deposition, (*d*) effect of rigidity of specimens being welded and (*e*) effect of stress relieving treatment.

Material will be available for the investigation of other variables in welding procedure if such investigations are warranted.

Comparative gas cutting tests will be made.

F—*Laboratory Investigations*

All laboratory investigations will be conducted at Battelle Memorial Institute. These will include: (*a*) chemical analyses of each heat, (*b*) determination of physical properties of base plates for each thickness in both the "as-rolled" and normalized conditions, (*c*) determination of physical properties of welded joints in as-welded and stress-relieved conditions, (*d*) hardness measurements and microstructure examinations of the heat-affected regions adjacent to fusion welds and to torch cut edges and (*e*) notch bar impact tests on specimens heat treated to simulate the condition of the steel in the heat affected region of a welded joint.

Auspices

The Research Program will be in direct charge of Materials Committee II—Plain Carbon Steels of the Industrial Research Division of the Welding Research Committee, reporting progress to the Executive Committee of the Division. The program was originally published in the January 1938 Research Supplement to the JOURNAL OF THE AMERICAN WELDING SOCIETY for comment by the welding industry. Comments from eighty out-standing authorities were received and carefully considered by the Committee. General approval of the program was indicated.

Cost

The total cost of the program is estimated at $14,500. Of this amount, $4500 will be used in the preliminary series of investigations referred to in Item B. When the results of these are available and analyzed, the main program will be undertaken with such modifications as then seem desirable. It is desired now to obtain pledges for the complete program, but at this time payment of only the amount needed to cover the preliminary work will be requested. Payment of the amount needed for the main program will be requested later on, when the main program is undertaken, probably nine months from date. The Engineering Foundation will act as treasurer for the funds. It is anticipated that the steel industry, pressure vessel manufacturers, the petroleum industry, shipbuilding industry and governmental departments will cooperate.

Cash contributions ranging from $100 to $500 are needed at once for the preliminary program, with corresponding pledges ranging from $200 to $1000 for the main program, to be called for when, and if, undertaken. Checks should be made payable to the Welding Research Committee and sent with a letter of transmittal specifically designating that the subscription is for the work on Weldability of Plain Carbon Steels.

SUMMARY OF ESTIMATED COST

	Preliminary Program	Main Program	
I—Steel Plates			
Experimental Ingots	$690	$3910	
Rolling of Experimental Plates	150	850	
Commercial Open-Hearth Plates	216	...	
	$1056	$4760	
II—Welding and Torch Cutting	177	356	
III—Radiographic and Magnaflux Examination	45	85	
IV—Laboratory Investigations (Battelle Memorial Institute)	2544	4789	
	$3822		$ 9990
V—Miscellaneous Tests (not outlined)	688		4510
Total Cost of Both Preliminary and Main Programs			$14,500

ELASTIC BEHAVIOR AND STRENGTH of Side Fillet Welds*

By D. ROSENTHAL and P. LEVRAY

SUMMARY

UP TO a value of 20 for the ratio l/a (l = length, a = throat of side fillet weld) the stress concentration at the ends of a relatively small isoceles weld (a = 0.084 inch) scarcely exceeds 15%, and the static strength is proportional to the length of the weld. Beyond l/a = 20 the stress concentrations are greater, attaining 50% at l/a = 57, and the static strength is up to 25% less than proportionality would suggest.

NATURE OF THE PROBLEM

Despite the economic importance of the stresses that exist in a welded connection, they have not received much study principally on account of experimental difficulties. For example, in side fillet welds, Fig. 1, there are stress concentrations at the ends A and A' of the fillets. Nevertheless, static tensile tests by numerous investigators, of which those made by H. Dustin are the most comprehensive, show that the static strength of the fillets is practically proportional to their lengths. Superficially these two statements are contradictory, stress concentrations a priori excluding the proportionality law.

Notwithstanding the overwhelming accumulation of experimental and theoretical information,[1] there is a gap in our knowledge of side fillet welds, which the present research is intended to fill. Methods and specimens have been developed to eliminate uncertain factors. The main object has been to compare static strength with elastic stress concentrations. In order to lighten the text, details of methods and derivations of formulas are confined to the Appendix.

*Research conducted in 1936–37 at the Materials Laboratory of the Université Libre de Bruxelles of the Welding Research Commission of the Belgian National Foundation for Scientific Research. Contribution to the Welding Research Committee. Translated by G. E. Claussen.

DEFINITIONS AND NOTATION

Load P is considered to act on the side fillet weld, Fig. 1, the axis of coordinates x, y and z being 0.
Notation

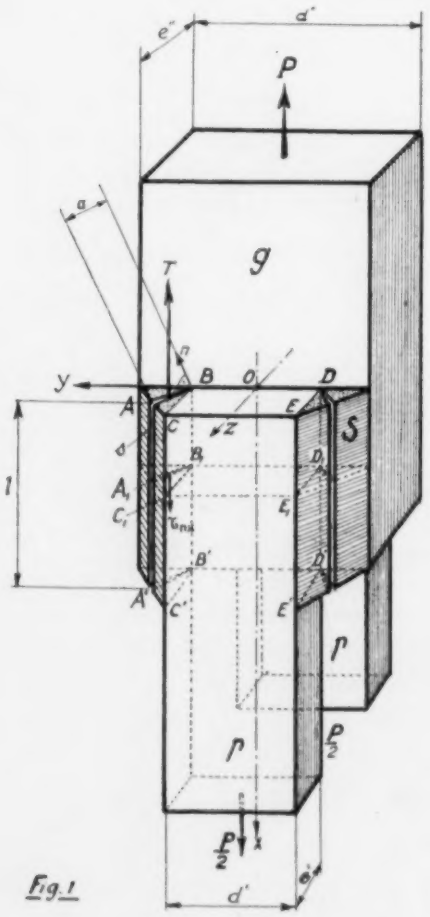


Fig. 1

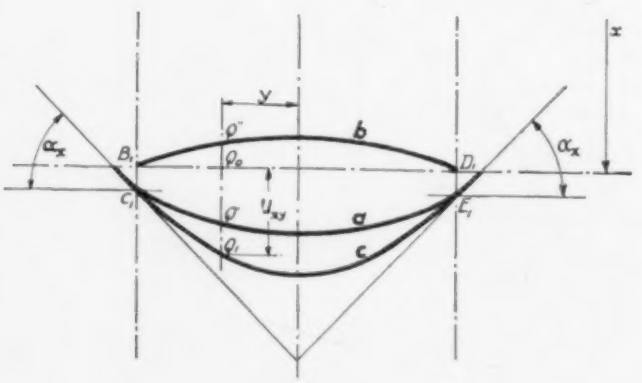


Fig. 2

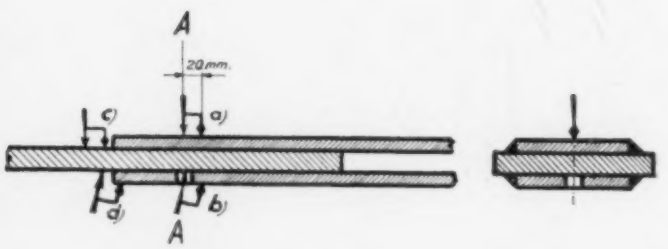


Fig. 3

l = length of weld
a = throat of weld
$AA'BB'$ = throat section of weld
n = the normal to that section
d' = width of plate
e' = thickness of plate ($e' = 1.41\ a$)
d'' and e'' = corresponding dimensions of gusset, g
$CC'BB'$ = area of weld on gusset
G = shear modulus of elasticity = 11,700,000 lb./in.^{2}.

Experience shows that when the length l of the welds is sufficiently short compared with the width d of the plates, fracture under static load generally occurs by shear through the throat section $AA'BB'$ of the weld. Hence, the stress distribution on that section appears to govern the static strength of side fillets. In the present research the shear load t_x for every value of x is used to express the distribution of load along the section.

To define t_x let us consider the shear stress S_{nx}, Fig. 1, acting at any point on the throat section and in the x direction. By definition,

$$t_x = \int_o^a S_{nx} da \qquad (1)$$

The total load in shear applied to the throat section of each weld is

$$T = \int_o^l t_x dx \qquad (2)$$

The average value of t_x is called t_m

$$T = lt_m$$

At equilibrium

$$4T = P \qquad (3)$$

Hence

$$t_m = P/4l \qquad (3')$$

METHOD OF MEASUREMENT

Measurements below the elastic limit were made to determine the unit shear load t_x in the weld. The interior of the weld not being accessible, the direct method based on the definition of t_x (see preceding section, formula 1) is inapplicable. The indirect method adopted in the present research consists in calculating t_x from the curve of relative displacement of the plate with respect to the gusset for every value[2] of x. The relative displacements are a consequence of the difference in deformation undergone by plate and gusset in the joint. The rubber model clearly shows the difference, Figs. 24 and 25. In Fig. 2, curve a represents one of the lines initially straight that were drawn on the surface of the plate. Curve b represents a similar line on the surface of the gusset. These two lines are referred to the same origin, line B_1D_1, see also Fig. 1. The relative displacement of the plate with respect to the gusset U_{xy} at point Q_o shall be defined as the sum of displacements Q_oQ' and Q_oQ''. Thus point Q_1 and curve c are obtained, the curve representing the relative displacement of plate with respect to gusset.

It has been shown (see Appendix) that angle α_x, which is the angle at which curve c, extrapolated to section $CC'BB'$, Fig. 1, intersects B_1D_1, is proportional to the unit shear load t_x acting on the weld at x. Furthermore, if the width d' of the plates is sufficiently large compared with their thickness e', the shear load is expressed by the approximate formula, derived in the Appendix:

$$t_x = \frac{G\alpha_x e'}{1 + \frac{2e'}{e''}} \qquad (4)$$

Its limit of application is discussed later.

SPECIMENS

Specimens of the type shown in Fig. 1 were made from wide plates of commercial mild steel having a tensile strength of 54,000 to 60,000 lb./in.^{2}. Covered electrodes (Arcos Veloxend 37) 0.13 inch diameter were used. The deposited metal had the following properties:

Elastic limit	= 47,000 lb./in.^{2}
Tensile strength	= 57,000 lb./in.^{2}
Elongation	= 32% in 5 diameters

All welding was done under the same thermal conditions. The welds were machined to exact length and to

Table 1—Dimensions and Static Strength of Specimens (see Fig. 1)

Series	Dimensions of Each Plate, Inch		Dimensions of Gusset, Inch		Dimensions of each of Four Welds, Inch		l/a	d'/e'	γ	Breaking Load, Lb.	Breaking Stress, Lb./In.^{2}
	e'	d'	e''	d''	a	l					
A	0.12	5.67	0.39	6.3	0.084	4.72	56.5	48	1.84	57,700	36,700*
"	"	"	"	"	"	3.14	37.7	"	"	44,000	42,100*
"	"	"	"	"	"	1.57	18.8	"	"	23,200	44,800*
"	"	"	"	"	"	0.79	9.45	"	"	12,500	47,900
B	"	"	0.24	"	"	4.72	56.5	"	1.11	57,500	36,500*
"	"	"	"	"	"	3.14	37.7	"	"	42,700	40,800*
"	"	"	"	"	"	1.57	18.8	"	"	23,400	45,000*
"	"	"	"	"	"	0.79	9.45	"	"	11,900	45,400
C	"	1.97	0.39	2.36	"	0.80	9.45	16.6	2.0	13,200	49,500
"	"	"	"	"	"	1.19	14.2	"	"	18,700	47,000
D	0.19	3.55	0.38	3.94	0.13	3.94	30	19	1.13	....	.....
					0.12	1.97	16.7				
E	0.28	2.36	0.39	3.12	0.19	2.76	14.6	8.5	0.94	....	.....
						1.38	7.25				
F	0.31	2.16	0.47	3.0–3.1	0.22	2.36	10.9	6.9	1.06	....	.....
						1.18	5.45				
G†	0.39	3.56	0.78	3.93	0.16	3.94	25	9.05	1.1	....	.....
						1.97	12.5				

γ = Cross section of gusset/cross section of plate.
*Fractured through the measuring holes.
†The leg of the fillet weld was only 57% of the thickness of the plate.

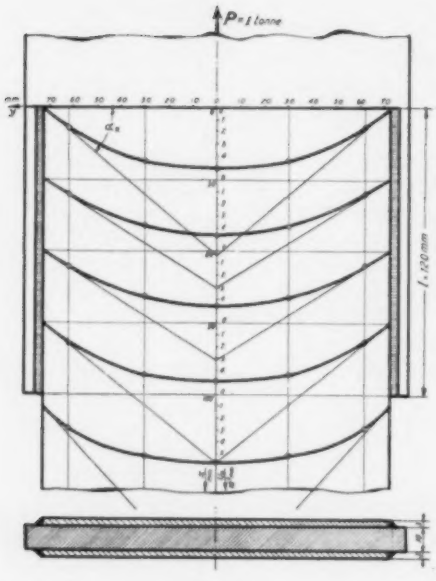


Fig. 4

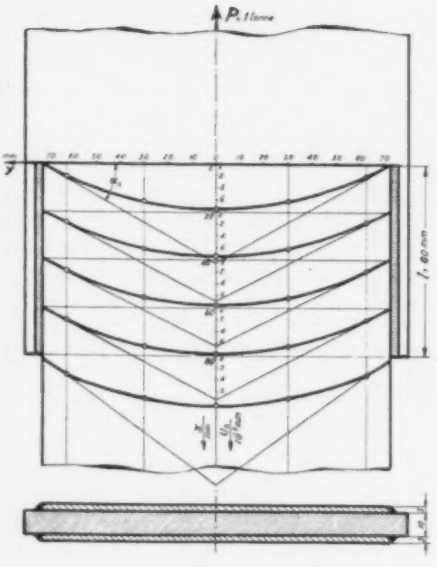


Fig. 5

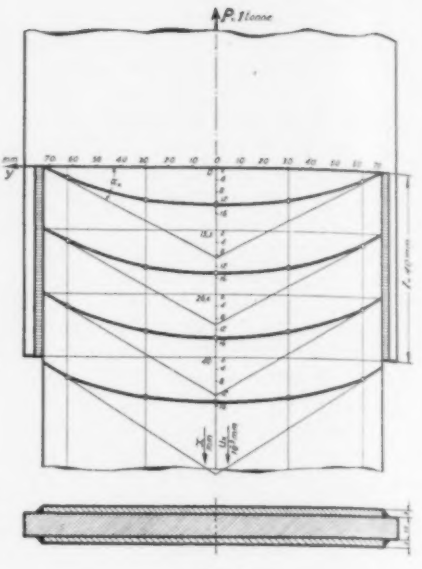


Fig. 6

the profile of an isoceles triangle. The dimensions of the specimens are given in Table 1. Seven series of specimens were tested in triplicate. In Series *A* the ratio of cross section of gusset to cross section of plates was 1.84; in Series *B* the ratio was 1.11. In both series $d'/e' = 48$. The specimens of a series differed only in length of weld. The plates and welds were of the same size in both series, the length of weld and section of gusset being the variables. In Series *C*, which were used only for static tensile tests, the ratio of cross sections was 2.0 and $d'/e' = 16.6$. The remaining four series were used to test the limits of application of formula 4 of the preceding section. In all, 38 specimens were utilized.

TEST PROCEDURE

Figures 3 and 26 illustrate the test procedure. A Huggenberger tensometer (0.78-inch gage length) was placed in two positions at each gage mark. The first position is indicated by letter *a* in Fig. 3, the two legs of the tensometer resting on the plate. In the second position indicated by letter *b* in Fig. 3 one leg rests on the plate, the other on the gusset, through the hole drilled in the plate. If U'_{xy} designates the deformation measured in the first position and U''_{xy} the deformation measured in the second position, the relative displacement of the plate with respect to the gusset, U_{xy}, defined in the preceding section, is represented at point *A* by the following expression.

$$U_{xy} = U''_{xy} \pm U'_{xy} \qquad (16)$$

At the ends of the plates the tensometer was placed in positions *c* and *d* shown in Fig. 3. To avoid the effect of the holes on the deformation of the plate, the deformations U'_{xy} were made before the holes were drilled. Any lack of symmetry in loading was equalized by averaging measurements made on both plates. A complete program of measurements was carried through on a single specimen of each series. The remaining two specimens of each series were used to control the reproducibility of the measurements, which was accomplished by taking measurements of U_{xy} at the end of the plate ($x = 0$) and on the axis of the specimen ($y = 0$).

An Amsler machine (110 tons capacity) was used to apply load in increments until the average stress in the throat section of the welds was 8500 lb./in.^{2} Readings were taken at intervals of about 2100 lb./in.^{2} to confirm proportionality between stress and strain. Accuracy of measurement was estimated to be 3%, reproducibility being within the limit of accuracy.

Table 2—Calculated Unit Shear Loads t_x in the Welds Using Formula 4, at an Applied Load of 2200 Lb. (Shear Modulus of Elasticity = 11,700,000 Lb./In.^{2})

Series	1, Inch	l/a	d'/e'	$\alpha_x \times 10^3$	t_x Kg./Mm.	$4T$ Lb.	$4T/P$	$t_m = P/4\,l$ Kg./Mm.	t_x/t_m
A	4.72	56.2	48	0.151*	2.34*	2460	1.12	..	..
"	3.15	37.5	"	0.225*	3.48*	2460	1.12	..	...
"	1.57	18.7	"	0.457*	7.1*	2510	1.14	..	..
B	4.72	56.2	"	0.186*	2.29*	2420	1.1	..	..
"	3.15	37.5	"	0.266*	3.30*	2310	1.05	..	..
"	1.57	18.7	"	0.553*	6.88*	2420	1.1	..	..
D	3.94	28.8	19	0.163	3.25	...	..	2.5	1.30
"	1.97	14.4	"	0.270	5.30	...	..	5.0	1.04
E	2.76	14.9	8.5	0.133	3.2	...	..	3.56	0.9
"	1.38	7.45	"	0.166	4.2	...	..	7.12	0.6
F	2.36	10.6	6.9	0.087	2.44	...	..	4.15	0.59
"	1.18	5.3	"	0.153	4.4	...	...	8.30	0.53
G	3.94	25	9.05	0.069	3.0	...	..	2.5	1.20
"	1.97	12.5	"	0.106	4.6	...	..	5.0	0.92

*Average, using Simpson's Rule, of values at 4 or 5 locations along the length of the weld. Values for Series *D* to *G* were determined at $x = 0$.

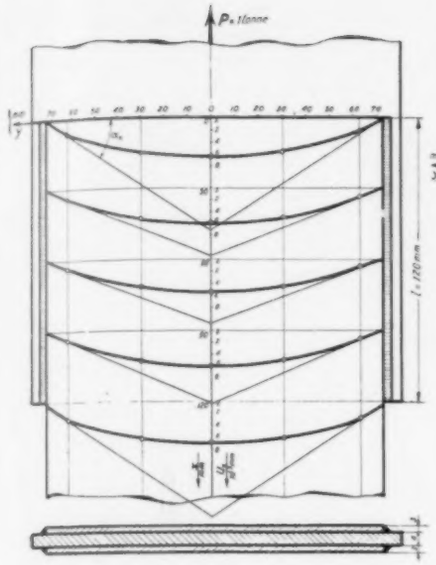


Fig. 7

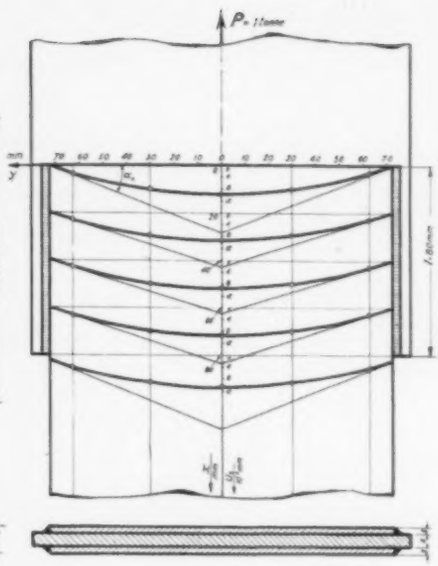


Fig. 8

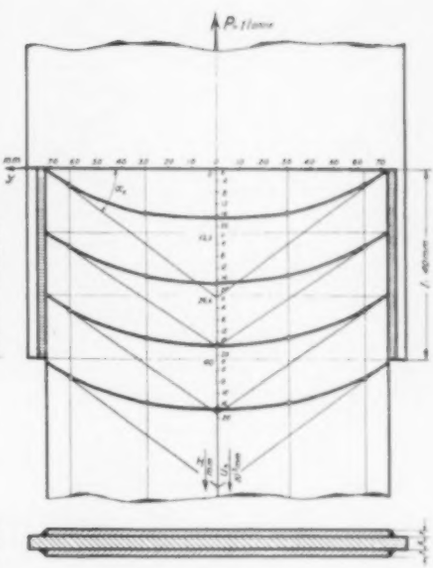


Fig. 9

RESULTS

Averages of the measurements of U_{xy}, the relative displacement of the plate with respect to the gusset, were calculated and are the bases of curves Figs. 4 to 9 of type *c*, Fig. 2, which were used to determine tan α_x by extrapolation. Formula 4 was used to calculate the unit shear loads t_x, Table 2; and Formula 2 to calculate the total shear load 4 T in the four welds of the joint, Table 2.

The following conclusions are drawn from Series *A* and *B*.

1. Equilibrium requires that the shear load 4 T applied to the four welds must equal load P applied to the gusset, Formula 3. Table 2 reveals an excess of 10% above the load 4 T, which evidently is a result of the approximate nature of Formula 4. To secure better agreement the unequal distribution of deformations across the thickness of the plate must be taken into account, Figs. 27 and 28.

2. The unit shear loads t_x are not distributed uniformly, but increase toward both ends of the weld. As the weld is made longer, the values of t_x for a given applied load decrease in absolute value at the ends, at first rapidly, then more slowly. Indeed, the values of t_x seem to approach a limit, Figs. 10 and 11.[3] However, with respect to the average shear load, t_m, the stress concentration increases rapidly as the length of the weld is increased.

3. When the cross section of the gussets is larger than

Table 3—The Stresses on the Surface of the Weld

Joint	x, Inch	$\delta_x \times 10^4$	$\delta_t \times 10^4$	$\delta_z \times 10^4$	S_{nz} Lb./In.²	σ_z Lb./In.²	Notes
I	3.54	4.18	−1.58	1.67	7270	6550	S_m = 4950 lb./in.²
"	2.76	2.67	−0.75	2.2	4180	7110	$P/4\ la$ = 9250 lb./in.²
"	1.97	2.19	−0.26	2.7	3730	7900	whence S_m = 0.54 $P/4\ la$
"	1.18	2.60	−0.58	3.22	4270	9600	P/S_a = 6150 lb./in.²
"	0.39	2.84	−1.5	2.30	5340	6900	
II	7.48	3.02	−0.835	1.66	4650	6050	S_m = 1710 lb./in.²
"	6.55	1.33	+0.166	2.08	1420	6400	$P/4\ la$ = 4620 lb./in.²
"	5.71	0.84	+0.419	2.26	500	6580	
"	3.94	0.75	+0.50	2.34	280	6750	whence S_m = 0.37 $P/4la$
"	2.16	1.25	+0.084	2.34	1420	6830	
"	1.32	1.67	0.00	2.50	2000	7540	P/S_a = 6170 lb./in.²
"	0.39	2.51	−1.00	1.83	4220	5900	

Dimensions of Joints

	Joint I	Joint II
Plates (2)	0.43 x 3.96 inches each	0.43 x 3.94 inches each
"	S_p = 3.40 sq. in. cross section	S_p = 3.35 sq. in. cross section
Gusset	0.61 x 5.57 inches	0.61 x 5.61 inches
"	S_g = 3.40 sq. in. cross section	S_g = 3.42 sq. in. cross section
Weld	0.303 x 3.94 inches	0.301 x 7.88 inches
"	throat section = 1.19 sq. in.	2.37 sq. in.
"	cross section = 0.093 sq. in.	0.091 sq. in.
Total Cross Section of Joint	S_a = 3.40 + 3.40 + 4 x 0.093 = 72. sq. in.	3.35 + 3.42 + 4 x 0.091 = 7.1 sq. in.
	3.94 inches	7.88 inches
	Applied load = 44,000 lb.	
	E = 30,600,000 lb./in.² Poisson's Ratio = 0.3	
	G = 11,700,000 lb./in.²	

that of the two plates, the value of t_x in the weld is lower on the gusset side; that is, for $x < 1/2$, and is higher on the plate side; that is, for $x > 1/2$. The opposite applies, of course, if the cross section of the gusset is smaller than that of the two plates. Under both circumstances, inequality in cross section increases the stress concentration.

4. Figures 10 and 11 are of the same type as similar diagrams of earlier researchers, but the stress concentrations are lower in the present research than in earlier work.

In Fig. 12 the stress concentrations have been expressed as percentages of the unit average shear load ($P/4l$), and have been plotted as a function of the ratio l/a (l = length; a = throat of weld) in curves m and n. The isolated points refer to earlier research, which obviously is at variance with the present research.[4]

STATIC TENSILE TESTS

The results of the static tensile tests are summarized in Table 1. Curve m of Fig. 14 shows the variation of breaking load with the ratio l/a. In curve n the load has been divided by the cross section of the weld at midsection ($l \times a$) and is expressed in kg./mm^2.

The following conclusions have been drawn from these curves:

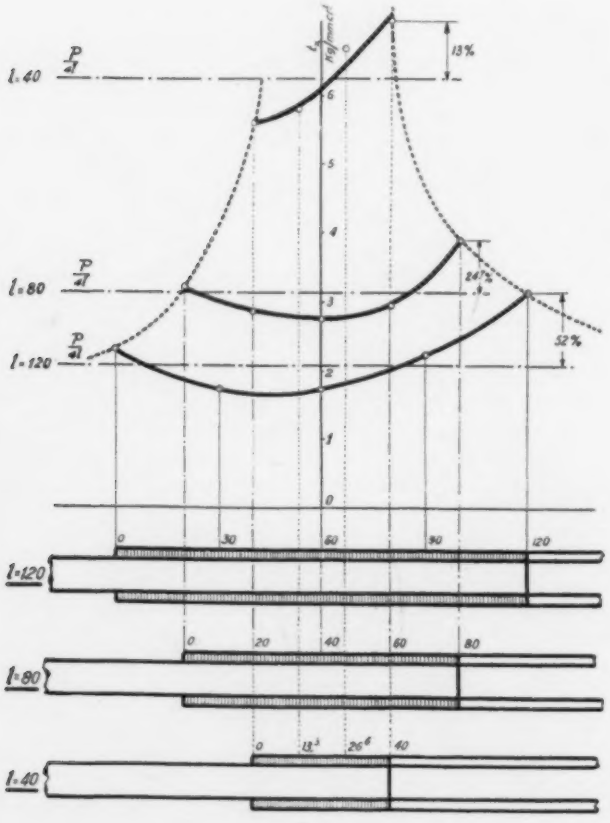
1. Breaking load increases less rapidly than the length of the welds. At first sight this result seems to contradict earlier research. However, the divergence is simply a question of size. The majority of tests performed hitherto have been made on specimens which were analogous to those used in the present research and in which the ratio l/a did not exceed 20. Figure 4 shows that deviation from the law of proportionality does not exceed 4% at a ratio of 20. The deviation from proportionality has escaped notice in previous research because the deviation was no greater than deviation in experimental results. Consequently, the law of proportionality appeared to be verified.

2. The plotted points for the three series of specimens lie on a smooth curve. The breaking load of identical welds, therefore, is practically independent of the dimensions of the joint, which vary from series to series. Within the limits of the present research, then, the static breaking load seems to be an intrinsic property of the weld.

CONCLUSIONS

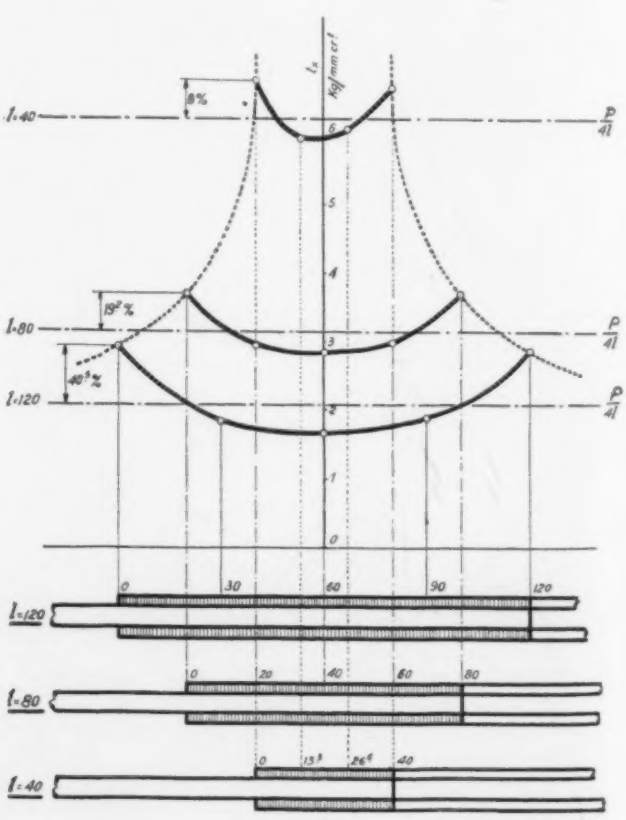
The experimental results show that an adequate explanation of the breaking load of side fillet welded joints of the type shown in Fig. 1 is to be found in the distribution of unit shear loads within the elastic limit. The distribution is not uniform; the load is concentrated at the two ends of the weld. However, when the ratio l/a (l = length, a = throat of weld) is less than 20, the concentration of load does not exceed 15%. In joints in which the ratio is less than 20 it seems reasonable to expect that the small concentration of load at the ends of the welds will have little effect on static strength, particularly if the dispersion of the experimental results and the effect of plastic deformation in equalizing the concentration are kept in mind. Hence, the law of proportionality became accepted, namely, that static breaking load is practically proportional to the length of the welds.[5]

On the contrary, when the ratio l/a greatly exceeds 20, for example, $l/a = 57$, the concentration of load at the ends of the welds approaches 50%, and the static breaking load is up to 25% less than the law of proportionality would predict. Within the limits of the



A

Fig. 10



B

Fig. 11

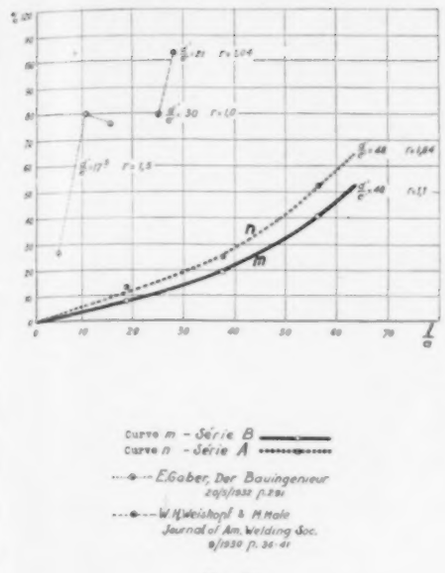

Courbe m - Série B
Courbe n - Série A
E.Gaber, Der Bauingenieur 20/5/1932 p.291
W.H.Weishopf & M.Hale Journal of Am. Welding Soc. 9/1930 p. 36-41

Fig. 12

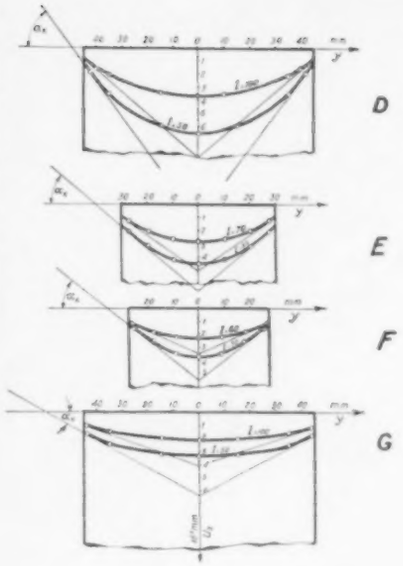


Fig. 13

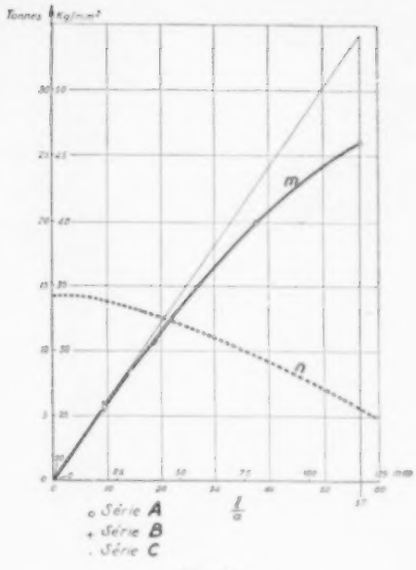


Fig. 14

present research the decrease scarcely is influenced by the variation of stress concentration brought about by changes in the ratio of cross section of plates to cross section of gusset.

APPENDIX

CRITIQUE OF METHODS FOR MEASURING ELASTIC STRESSES IN SIDE FILLET WELDS

By D. ROSENTHAL

Introduction

As stated in the first section of this report, the problem consists in determining the load along the throat section of the weld, Fig. 1. In other words, the stresses at each point on the throat section must be expressed as a function of the external load and the geometrical characteristics of the joint. Fracture occurring by shear suggests a preponderance of shear stresses S_{nz} acting in the direction of the x axis, Fig. 1. For this reason the shear stresses have been the principal object in measurements made on side fillets (see Footnote 1). However, the methods of measurement have yielded results that have been far from concordant. This critique endeavors to demonstrate the sources of disagreement and to indicate the most suitable method.

Direct Methods

In these methods measurements are made only on the surface of the welds. It is assumed that the law expressing the distribution of surface stresses applies also to the unit shear loads t_x along the weld. By definition,

$$t_x = \int_o^a S_{nx} da$$

To estimate the degree to which the formula approximates experimental results, the measurements outlined below have been made.

If at point M on the surface of the weld, Fig. 15, the strains δ_s and δ_t are considered along two directions AC and BD at 45° to the x axis, the stress applied to the surface element, $dx\,da$, of the throat section is:[6]

$$S_{nx} = G(\delta_s - \delta_t) \qquad (5)$$

where G = shear modulus of elasticity, 11,700,000 lb./in.^{2}.

If the value of δ_x at the same point is known, the system of stress around point M is completely determined. In particular, the normal stress on the transverse section of the weld is:[6]

$$\sigma_x = \frac{E}{1 - n^2}[\delta_x + n(\delta_s + \delta_t - \delta_x)] \qquad (6)$$

where E = Young's Modulus, 30,600,000 lb./in.^{2}
n = Poisson's ratio = 0.3

Two joints of the type shown in Fig. 1 have been studied by this method. The results are given in Table 3. The length of the welds was 4 inches in the first joint and 8 inches in the second. Otherwise the joints were identical. The surface of the welds was machined to provide correct location of the extensometers.

The values of δ_s, δ_t and δ_x determined by means of Huggenberger extensometers (gage length[7] = 0.39 inch) are given in Table 3. The stresses S_{nx} and σ_x were calculated by means of Formulas (5) and (6). These stresses have been plotted in Fig. 16 as a function of x.

The following conclusions may be drawn.

1. The normal stresses σ_x calculated at the surface of the weld are generally higher than the average stress P/S_a obtained by dividing load P by the transverse section S_a of the joint (see Table 3). Nevertheless, the difference is comparatively small, especially in the middle part of the weld if it elongates.

2. The stresses S_{nx} increase as the ends of the weld are approached. When the weld elongates, the stresses at the ends—for a given applied load—decrease in absolute value, but greatly increase with respect to the average stress S_m.

If stress S_{nx} were constant over the throat of the weld, the average stress S_m shown in Fig. 16 would be equal to the average unit shear load t_m divided by the throat (see Formula 1). According to Formula (3′)

$$t_m = P/4l \qquad (3')$$

Whence we should deduce

NOTE: From the quantitative standpoint the results obtained apply rigorously only to relatively thin welds of isoceles profile (throat of weld = 0.084 inch) and to joints of the type shown in Fig. 1 in which the entire thickness of the plate is engaged by the weld, that is, $a = 0.707\,e'$. Expedience alone would justify extending the results to more complicated joints, which have not been considered in the present research.

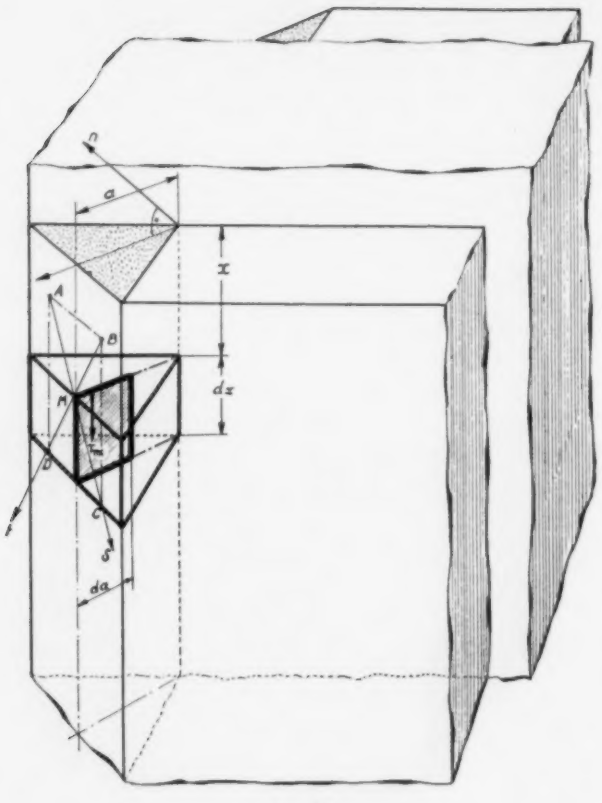


Fig. 15

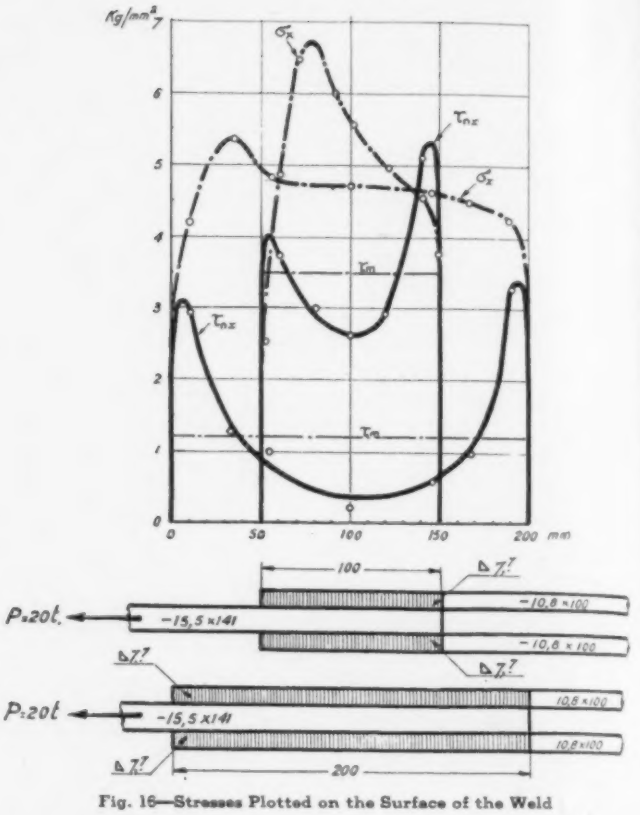


Fig. 16—Stresses Plotted on the Surface of the Weld

$$S_m = P/4\,la \qquad (7)$$

In reality, S_m is 0.54 P/la for the first joint (l = 4 inches) and is 0.37 P/la for the second joint (l = 8 inches). Therefore, stress S_{nz} is not constant over the throat, but is lower at the surface than in the interior of the weld. There is not at all a constant ratio between S_m and t_m. The ratio is much smaller for the second joint than for the first.

In conclusion, the experiments do not justify the methods which apply to the unit shear loads t_z in the side fillet the same distribution law as to the shear stresses on the surface of the side fillet weld.[8]

Indirect Methods

Instead of direct measurements, indirect methods may be adopted to determine the stress in the weld.

1. *Method of Decrease of Load P.*—Conditions of static equilibrium applied to the element dx of one of the plates require, Fig. 18:

$$2t_z dx = \frac{P_z + dP_z - P_z}{2} = dP_z/2 \qquad (8)$$

whence

$$t_z = {}^1/_4 dP_z/dx \qquad (8')$$

According to Formula 8', it is sufficient to plot $P_z/4$ as a function of x from $x = 0$ to $x = 1$. The tangent to this curve yields the value of t_z at any point x under consideration. Strictly speaking, to determine P_z the normal stresses at each point of the transverse section of the plates at section x must be summed. However, as in the discussion of the welds, only the surface of the plates is accessible for measurements. Although the difficulty appears at first sight to be of the same order in both circumstances, it is less for the plates than for the welds.

Inspection of the deformation undergone by the end of the plate in a rubber model, Figs. 27 and 28, shows that the interior fibers are displaced to the same extent as the corresponding exterior fibers, except in the immediate vicinity of the welds for a distance nearly equal to the thickness of the plate (equal to the distance between the lines drawn on the surface of the plate). Over this width the interior fibers are displaced less than the exterior, the difference becoming less as the top of the weld is approached.

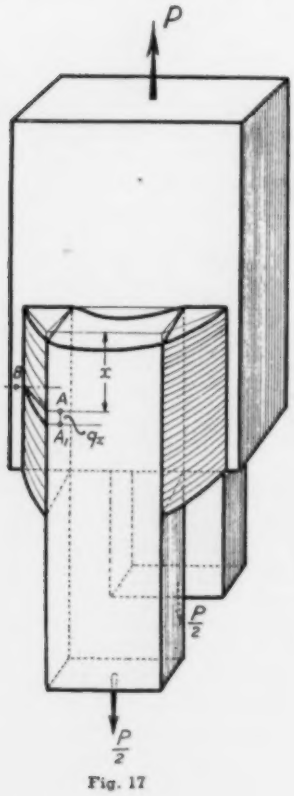


Fig. 17

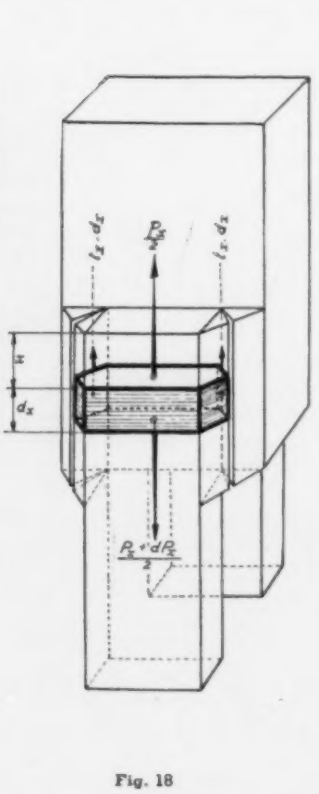


Fig. 18

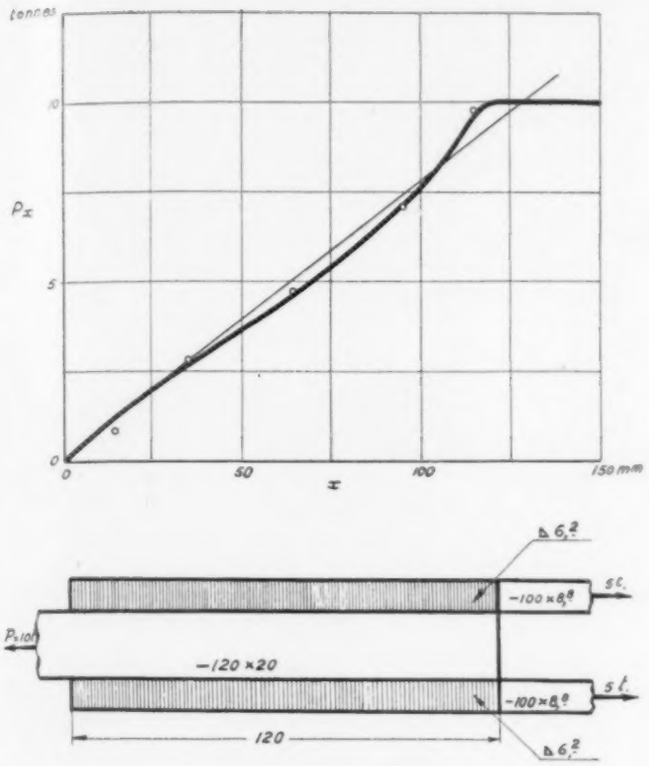
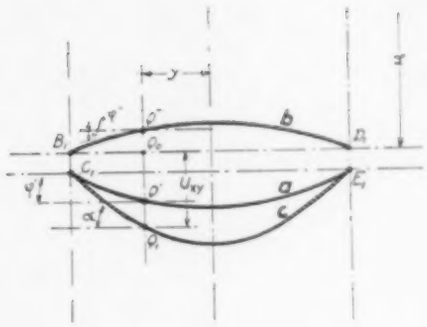


Fig. 19—Diagram of P_x in the Plate

Fig. 20



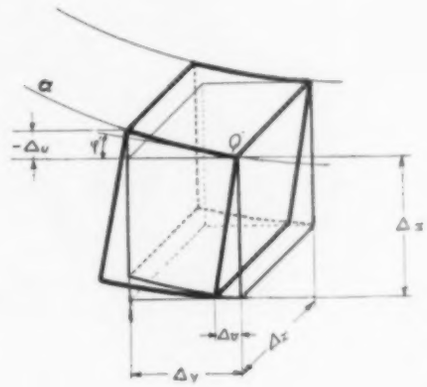


Fig. 21

Hence, it may be assumed that, except near the welds, the interior fibers undergo the same deformation as the exterior and, consequently, are subjected to the same stress. The hypothesis will be better justified for joints in which the thickness of the plate is small compared with the width than for joints in which the plate is relatively thick. Keeping in mind the conditions on which the assumption is based, we can neglect the disturbance in the stress system of the plate due to the presence of the welds.

As a result we can determine rather exactly the value of P_x from measurements made on the surface of the plate. There still remain two experimental difficulties in deducing values of t_x:

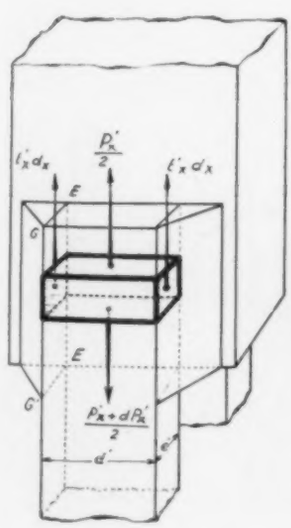


Fig. 22

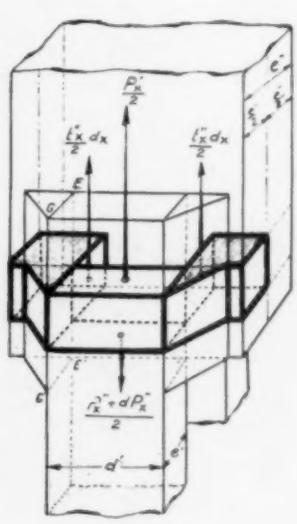


Fig. 23

(*a*) As shown[9] in Fig. 19, the plot of P_x, while not linear, is not far from a straight line. For this reason, small errors in P_x have a large effect on the variation of $t_x = dP/dx$.

(*b*) The accuracy of measurement decreases as the length of the welds decreases, for the load P likewise must be decreased to avoid overloading the welds.

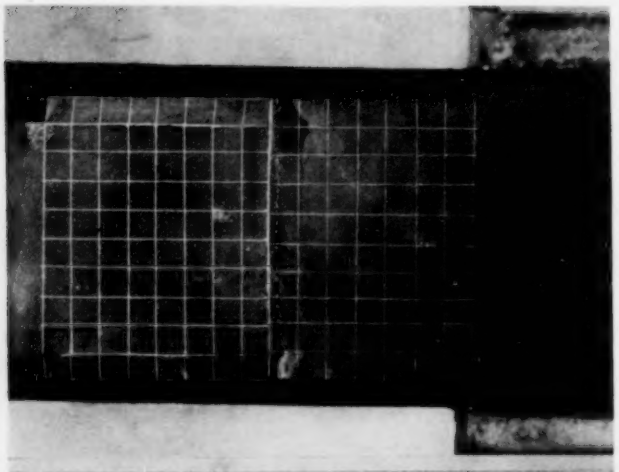
Despite theoretical advantages, this method is difficult to employ. It has found little application.[10]

2. *Method of Relative Displacements.*—The experimental difficulties disappear if the method of relative displacements (defined in the section on Method of Measurement) is adopted. In this method the separation Q_0Q_1, Fig. 20, produced along the x axis under the applied load is measured between two curves a and b drawn on the surface of the plate and the surface of the gusset, respectively, at the same section x and for the same value of y. Curve c is obtained in this way.

Experiment shows that this displacement is of the same order of magnitude for all values of x, and that it does not decrease appreciably when the applied load is reduced with respect to the length of the welds. Furthermore, in the vicinity of the axis of the joint; that is, for small values of y, the displacement is much larger than the deformation of the plate itself. In the same proportion the accuracy of measurement is increased. From the experimental point of view it is evident that the present method is more advantageous than the preceding Method 1.

From the theoretical point of view we must relate the curve of relative displacements with respect to x, to the unit shear loads t_x in the weld. To do so we must examine the deformation of an elementary volume $\Delta x \Delta y \Delta z$ of the plate (and of the gusset) at point Q_0. Figure 21 shows that the tangent at point Q' of curve a represents the limit of the ratio $\Delta u/\Delta y$.

Fig. 24



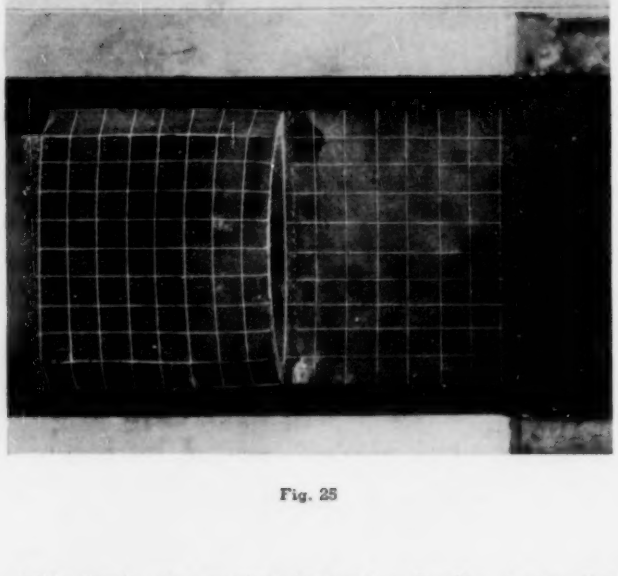


Fig. 25

This limit, the partial derivative $\partial u/\partial y$, is the first term of the expression for shear γ_{yz} of face $dzdx$ (and for shear γ_{xy} of face $dy\,dz$, for $\gamma_{yz} = \gamma_{xy}$). The second term is furnished by the limit of the ratio $\Delta v/\Delta x$; that is, by the partial derivative $\partial v/\partial x$. Hence,

$$\gamma_{xy} = \partial u/\partial y + \partial v/\partial x$$

In the side fillet joint the second term can be neglected compared with the first.[11]

To a close approximation we may then write[12]

$$\gamma'_{yz} \backsimeq \partial u/\partial y \backsimeq \tan \phi' \qquad (9)$$

Hence the value of the tangential stress in the plate is

$$S'_{yz} = G\gamma'_{yz} \backsimeq G \tan \phi' \qquad (10)$$

In the same way for the gusset at point Q'', Fig. 20,

$$S''_{yz} = G\gamma''yx \backsimeq G \tan \phi'' \qquad (10')$$

whence

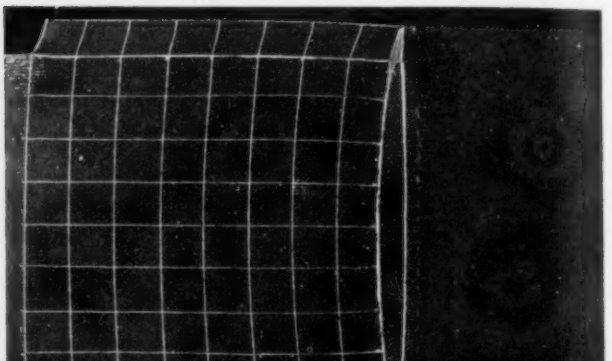
$$S'_{yz} + S''_{yz} = G(\tan \phi + \tan \phi') = G \tan \alpha \qquad (11)$$

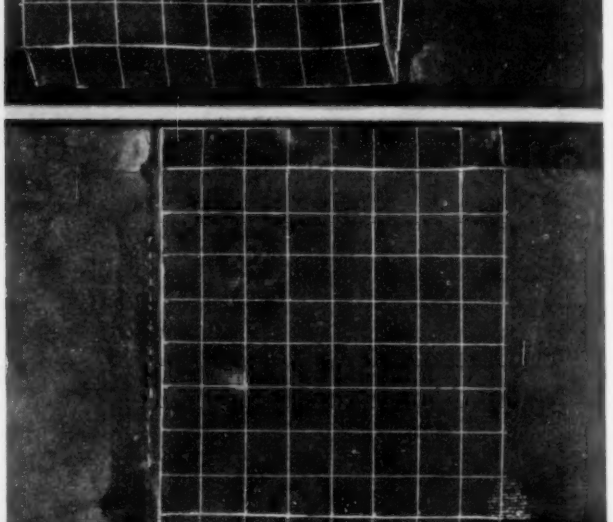
The minuteness of the elastic deformations permits us to replace the tangent of the angle by the angle itself. Formula 11 can then be written, Fig. 20,

$$S'_{yz} + S''_{yz} = G(\phi' + \phi'') = G\alpha \qquad (12)$$

In conclusion, the tangent of the curve of relative displacements U_{xy} at point Q_1 is approximately constant and is the sum of the tangential stresses in section xy passing through this point. One of these stresses is applied in the immediate vicinity of the exterior surface of the plate. The other stress is applied in the immediate vicinity of the exterior surface of the gusset, Figs. 20 and 28. The tangent at Q_1 evidently will be the same as the tangent α_x at point C_1 obtained by extrapolating curve c to section $GG'\,EE'$, Fig. 22, which is the section on which the weld makes contact with the plate.

If tangential stress S'_{yz} is distributed uniformly over this section, we may write in conformity with Formula 4′, Fig. 22,





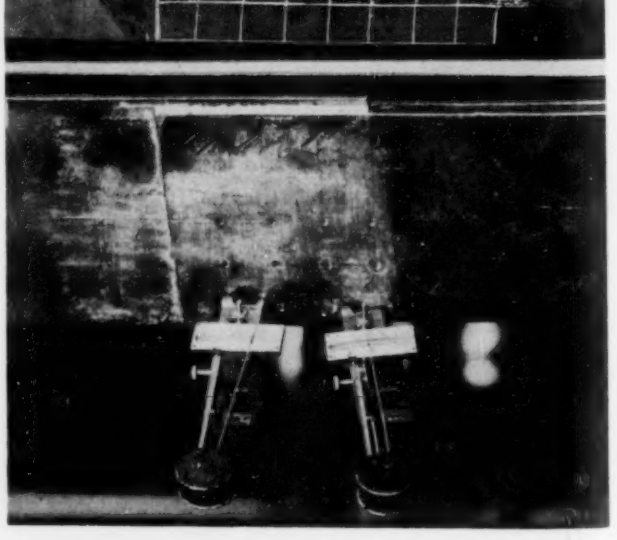


Fig. 26 (Top) Fig. 27 (Center) Fig. 28 (Bottom)

$$t'_x = S'_{yz}e' \tag{13}$$

and similarly for the gusset, Fig. 23,

$$t''_x/2 = S''_{yz}e''/2 \tag{14}$$

The values of t_x, t'_x and $t''_x/2$ are not exactly the same (see Figs. 18, 22 and 23), but the difference is small if width d' is large with respect to e' and e'' (see below).

Under these circumstances

$$2S'_{yz}e' = S''_{yz}e'' \tag{15}$$

whence (see Formula 12)

$$2t_x = (G\alpha_x - t_x/e')e''$$

and[13]

$$t_x = \frac{G\alpha_x e'}{2\dfrac{e'}{e''} + 1} \tag{16}$$

If the tangential stress in the vicinity of the weld were constant across the thickness of the plate and gusset, Formula 16 would permit simple calculation of the value of the unit shear load t_x in the weld utilizing measurements of the relative displacement of the plate with respect to the gusset.

In reality, as we have seen (section on Direct Methods), the tangential stress at the edges of the weld cannot be constant across the thickness of the plate (and of the gusset). Formula 16 then is only approximately true. Nonetheless, the approximation is closer for plates and gussets that are wide with respect to their thickness (and with respect to the thickness of the weld).[14]

Under these conditions the method of "relative displacements" is manifestly superior to the "direct" methods. It is comparable to the method of "decrease of load," from the standpoint of approximation, and is more sensitive than the latter method. For this reason the method of "relative displacements" was selected for the present research.[15]

REFERENCES

1. A review of the literature on the subject has been made by A. G. Solakian and G. E. Claussen, AMERICAN WELDING SOCIETY JOURNAL, **16** (5) Suppl. 1–22 (1937).
2. x is defined later.
3. In accordance with the preceding statement, the values of t_x in Table 2 have been decreased in the ratio $P/4T$ in drawing Figs. 10 and 11.
4. The reason for the divergence must be sought in the more or less approximate nature of the methods utilized. The isolated points have been traced to measurements affected by the unequal deformation created by the different thicknesses of the weld. Table 2 illustrates the effect of thickness on the method of "relative displacements" adopted in the present research. Table 2 contains the values of α_x and t_x calculated from the measurements in Fig. 13 for $x = 0$; that is, for the ends of the weld. From these has been deduced the stress concentration, t_x/t_m. All the evidence shows that the ratio t_x/t_m cannot be less than unity. Nevertheless, the results show that the ratio becomes less than unity when d'/e' (d' = width, e' = thickness of plates) is less than 19. Consequently, Formula 4 cannot be used when $d'/e' < 19$. Table 2 also contains results for specimen G Table 1, the welds of which are thinner than the plates ($a = 0.56\,e'$ instead of $0.707\,e'$). For this specimen the ratio t_x/t_m is higher than for an analogous specimen E having thicker welds.
5. These considerations explain the acceptance of the law of proportionality by researchers whose specimens did not exceed the ratio $l/a = 20$.
6. See, for example, L. Baes, Résistance des Matériaux, Brussels, 1930, p. 174.
7. Complete measurements were made on only one weld of each joint. For purposes of control the values of δ_s were determined also on another weld of each joint. The average deviation between measurements made on two different welds was of the order of 10%.
8. This conclusion applies particularly to methods based on the measurement of the displacement q_x between points A and B at the edges of the weld. Fig. 17(W. H. Weiskopf and M. Male, AMERICAN WELDING SOCIETY JOURNAL, **9** (9) 23–48; (12) 68–82 (1930); E. Gaber, *Baningenieur*, **13**, 290–294 (1932)).
9. *Bulletin A. I. Br.*, **25**, 129–165, 179–196 (1929).
10. Hollister, S. C. and Gelman, A. S., AMERICAN WELDING SOCIETY JOURNAL, **11** (10) 24–31 (1932).
11. Tests in 1929 (Footnote 9) showed that the error does not exceed 2%. The only exception is the end face of the plate; that is, at $x = 0$ (and for the end face of the gusset). On this face the tangential stresses must be zero. Since

$$S_{yz} = G\gamma_{yz}$$

where G = shear modulus of elasticity,

$$\partial w/\partial x = -\partial u/\partial y$$

That is, the second term must equal the first and must be of opposite sign. This restriction (see section on Results) is not serious for the method that is utilized.
12. The same approximation is encountered in the direct method based on measurements of q_x, Fig. 18.
13. If account were to be taken of the effect of the normal stresses on the shaded portion of the gusset, Fig. 23, we would write

$$2S'_{yz}e' = (1 - \epsilon)S''_{yz}e''$$

where ϵ represents approximately the ratio of the shaded section to the section of the plate. Hence

$$t_x = \frac{G\alpha_x e'}{2\dfrac{e'}{e''}(1 - \epsilon) + 1} \tag{16'}$$

For $e'' = 2e'$, $\epsilon = 0.05$, the difference between 16 and 16′ will be scarcely 2.5%.
14. The tangent α_x of Formula 16 is obtained by extrapolating curve c, Fig. 20. The curve c is plotted from the deformations of the plate and gusset. Beyond the edges of the weld these deformations are practically the same throughout the thickness. The tangent therefore will be approximately the same as tan α_x provided that the system of stresses in the weld does not affect curve c. This condition will be more nearly fulfilled for plates (and welds) that are thin with respect to the width than for plates (and welds) that are relatively thick.
15. This method has been used before in a much simpler form by E. Höhn (*Chaleur et Industrie*, **13**, 293–300 (1932)).

German Boiler Code

SUPPLEMENTING the German National Boiler Code,* rules have been issued for the training and qualification of boiler welders.† Starting April 1, 1939, all boiler welders must be trained and qualified. After April 1, 1940, only welders qualified under Code rules will be permitted to weld steam boilers. Welders are classified as field or shop welders and as arc or gas welders. The theoretical parts of the training as well as the testing schedule involve familiarity with welding specifications, apparatus, materials, filler rods and safety precautions. Practical tests include the production of butt welds in boiler plate 0.59–0.79 inch thick in both vertical and flat positions. The gas welder also must demonstrate familiarity with the care of his apparatus.

The welded test plates are X-rayed and three sets of tension, bend and notch impact specimens are cut therefrom. The flush-machined, reduced-section tensile specimens must show 95% of the strength of unwelded plate. The bend specimen is tested on rollers with a plunger of the same diameter as the thickness of the specimen. The required bend angles are 120° for vertical welds, 150° for horizontal welds. DVMR notch impact specimens must attain 5 mkg./cm.² in vertical welds and 6 mkg./cm.² in horizontal welds at room temperature. The cross section of fracture in the notch impact test represents the upper part of the weld metal. In addition to these mechanical tests, the macro-and micro-structure of the weld is examined at three sections. Large inclusions must be absent and penetration must be good.

* See AMERICAN WELDING SOCIETY JOURNAL, **16**, 23–26, Nov. 1937.

† *Richtlinien für die Ausbildung und Prüfung von Kesselschweissern, Zeit. Bayer. Revisions Ver.*, **43** (1) 5–9, Jan. 15, 1939.

Weldability and Mechanical Properties of High-Tensile Steel Sheet*

By H. CORNELIUS and F. BOLLENRATH

SUMMARY

The tensile strength of unwelded and gas welded customary aircraft sheet and several substitutes 0.032–0.047 inch thick containing 0.25–0.32 C, 0.3–1.3 Si, 0.6–1.2 Mn, 0–1.1 Cr and 0–0.3 Mo was determined, as well as the tendency to cracking during welding and the hardness of welded joints. The requisites (at least 100,000 lb./in.² tensile strength without high hardness and with low Cr and Mo contents) were possessed by a steel containing about 0.3 C, 1.4 Si, 1.2 Mn, 0.25 Mo and by a steel containing about 0.3 C, 0.6 Si, 0.9 Mn, 0.6 Cr, 0.2 Mo. Four other steels could not be considered for practical applications on account of the hardness of the welded joints. Complete tests on commercial melts are necessary to confirm the experimental results.

THE following investigation was intended to determine the properties of cheaper substitutes for the well-known chromium-molybdenum aircraft steel (Aircraft material 1452 containing about 0.25 C, 1 Cr, 0.25 Mo).

Seven experimental heats, Table 1, were made in an acid high frequency furnace (18-lb. capacity) from low-carbon iron, Swedish pig iron, 60% low-carbon ferrochromium, 92% ferromanganese, 45% ferrosilicon and 70% ferromolybdenum. Melt 45 was killed with aluminum as well as with silicon. Except in Melts 44 and 45, sulphur was added to obtain 0.026% since previous experience showed that 1 Cr, 0.2 Mo steels with 0.026 S are sensitive to welding cracks unless special metallurgical procedures, not utilized in the present research, were observed.

The ingots, 3 inches diameter, 8½ inches high were forged and rolled to sheet. The bad surface of sheets 40 and 41 was attributed to inexpert rolling and was removed by grinding. Sheets 1 and 40 to 44 were annealed before the final polishing pass, whereas sheet 45 was normalized. Therefore, all sheets were tested in the soft annealed condition as well as after finish rolling. The properties of the soft annealed sheet are particularly significant because they correspond with the lowest strength obtained in the heat-affected zone of butt welds within sheet. Similarly the properties of the normalized sheet correspond with the maximum strength in the heat affected zone.

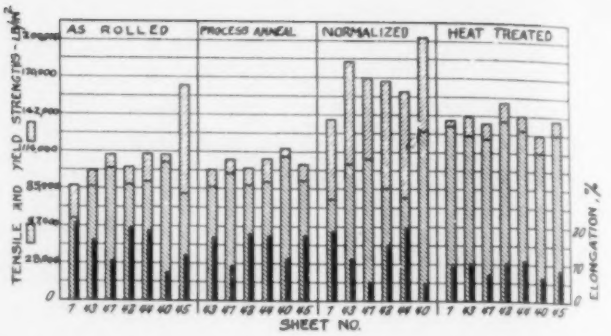


Fig. 1—Tensile Properties of Unwelded Experimental Sheets (Averages of two determinations)

//// = Tensile strength, lb./in.²
\\\\ = Yield strength, lb./in.²
■ = Elongation, % in 5.65 $\sqrt{\text{Area}}$

Column 1—as-rolled
Column 2—process annealed 1 hr. at 640° C.
Column 3—normalized ¼ hr. at 840° C.
Column 4—heat treated; oil-quenched from 850° C., drawn ½ hr. at 600° C.

The properties of the unwelded sheets in different heat treatments are summarized in Figs. 1 and 2. Since normalized sheets of the special steel are much stronger than the normalized sheet 1, their hardness after welding might be expected to be higher too.

There was no difficulty in welding the experimental sheets. Butt welds 1 inch long were made with an oxy-acetylene torch and a wire 0.06 inch diameter containing 0.18 C, 0.25 Si, 0.6 Mn, 0.75 Cr, 0.16 Mo. Sheet 44 did not flow so readily as the others and was a little more difficult to weld. The minimum tensile strength of the unmachined welds, Fig. 3, was over 100,000 lb./in.² for all sheets. Heat treated welds had practically the same strength in all sheets except sheet 40. With few exceptions fractures occurred outside the weld. Accordingly, the strength of the welds was the same as the strength of unwelded material in the same heat treatment.

Welds in sheet 45 which had been heat treated to a tensile strength of 136,000 ± 2800 lb./in.² before welding had 137,000 to 142,000 tensile, average = 139,000 lb./in². The material near the weld is heated above Ac_3 or between Ac_1 and Ac_3 and air hardens after welding. The material heated just below Ac_1 during welding is not held sufficiently long to soften. From the practical standpoint heat treatment before welding is simple and advantageous provided the welds are not subjected to repeated or long heating during fabrication.

The Focke-Wulf crack sensitivity tests† (rigid clamp) revealed no cracking tendencies except in sheets 41 and

* Extended abstract of "Festigkeitseigenschaften und Schweissbarkeit dünner Bleche aus hochfesten Baustählen." Report No. 452 of the Materials Committee of the German Society of Ferrous Metallurgy prepared by the Materials Testing Institute of the German Aircraft Experiment Station, and presented at a meeting of the Subcommittee on Weldability, Dec. 2, 1938. Published in *Arch. Eisenhüttenwes.*, 12 (7) 335–339, Jan. 1939.

† *Stahl u. Eisen*, 55, 903 (1935).

Table 1—Composition of the Experimental Sheets

Steel No.	Thickness, Inch	% C	% Si	% Mn	% P	% S	% Cr	% Mo	Remarks
1	0.039	0.25	0.31	0.60	0.015	0.007	1.0	0.21	Aircraft steel 1452
43	0.039	0.32	0.81	0.72	0.013	0.024	1.13	..	
41	0.035	0.31	0.72	0.71	0.014	0.026	0.75	0.25	Scarred surface
42	0.039	0.32	1.2	0.75	0.011	0.027	0.52	0.30	
44	0.047	0.32	1.35	1.17	0.013	0.015	..	0.25	
40	0.032	0.31	0.55	1.14	0.012	0.025	0.72	0.25	Scarred surface
45	0.039	0.28	0.57	0.91	0.018	0.013	0.57	0.19	Killed with aluminum

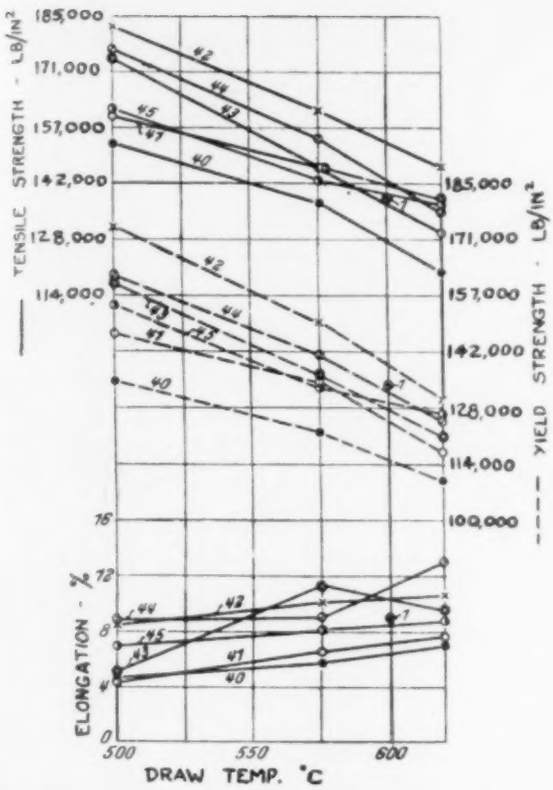


Fig. 2—Change in Tensile Properties of Experimental Sheets by Drawing ½ Hour at Different Temperatures After Oil-Quenching from 850° C. Numbers on Curves Signify Designation of Sheet, Table 1

44. The slight tendency in these two sheets could be corrected easily by suitable steel refining. The dependence of cracks on sulphur and carbon contents that has been found to apply to common aircraft and plain carbon steels does not apply to the experimental melts. There was no relationship between crack sensitivity and maximum hardness in the joint for the experimental sheets. The maximum hardness Fig. 4, is found in steel 40. Steels 44 and 45, on the other hand, are no harder than ordinary aircraft steels after welding, and therefore are of great practical value. The low hardness of 45 compared with 40 is related to the former's lower carbon and alloy content, and to aluminum killing.

Annealing the welds at 640° C. for 1 hour did not fully remove the hardness changes, the heat affected zones being up to 100 Vickers units harder than the unwelded sheet. Heat treatment, Fig. 4, was more effective in leveling the hardness. Examination of the microstructure was of little value in distinguishing between the hardness of the joints in the different steels.

Oral Discussion

K. L. Zeyen (Essen). Previous attempts to develop substitutes for Cr-Mo aircraft steel have been based on lower carbon contents in conjunction with special refining procedures. The present research shows that it is practicable to employ even higher carbon content than is customary for aircraft steel. The melting conditions were favorable to low crack sensitivity in the present research. Our experience has been that whereas experimental melts of unalloyed steels with comparatively high carbon content had good crack sensitivity

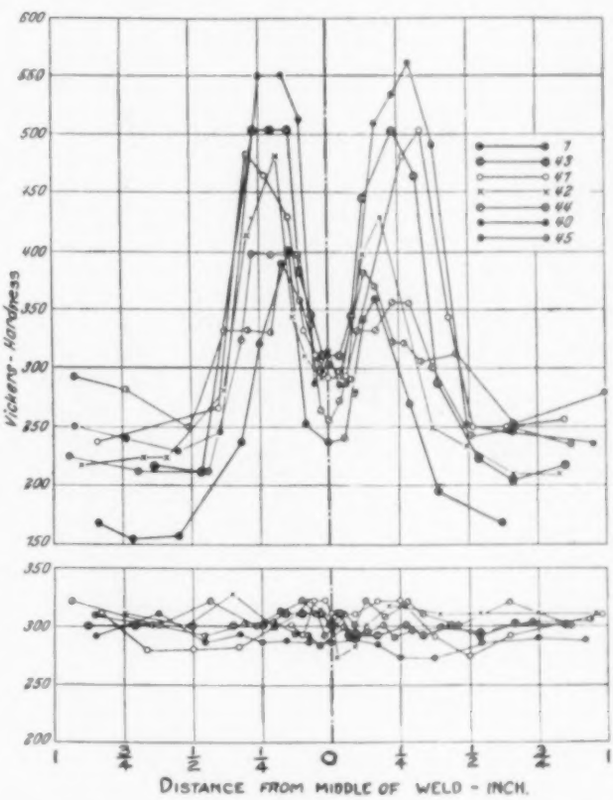


Fig. 4—Vickers Hardness Surveys of Welded Experimental Sheets
Upper chart—as-welded
Lower chart—heat treated after welding

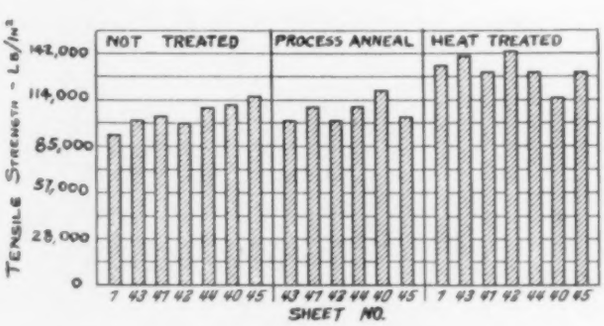


Fig. 3—Minimum Tensile Strength of Welded Sheets
Column 1—as-welded
Column 2—process annealed 1 hr. at 640° C. after welding
Column 3—oil-quenched from 850° C. and drawn ½ hr. at 600° C. after welding

characteristics, similar steels made in 22-ton basic open-hearth furnaces were extraordinarily sensitive to welding cracks.

Correspondence

H. Kiessler (Krefeld). Similar tests to those made by H. Cornelius and F. Bollenrath were made in Krefeld in 1936. Melts were made in a basic arc furnace using the same procedure as for Cr-Mo aircraft steel, and were cast as 580-lb. ingots, Table 2. Annealed sheets were butt welded with an oxyacetylene torch and a rod containing 0.18 C, 0.5–0.7 Cr, 0.15–0.25 Mo. Sheets 450 and 626 had desirable properties. These results agree with Cornelius and Bollenrath.

Table 2—Tensile Strength, Hardness and Crack Sensitivity of Gas Welded Experimental Sheets (H. Kiessler)

Cast No.	%C	%Si	%Mn	%P	%S	%Cr	%Mo	Minimum Tensile Strength Welded, Lb./In.²	Vickers Hardness Sheet	Vickers Hardness Maximum in Heat Affected Zone	Crack Sensitivity Measured in Rigid Clamp Test, %	Results of Cross Welded Crack Sensitivity Test*
450	0.30	0.63	1.46	0.025	0.006	..	0.21	128,000	240	375	0	Small cracks during bending
451	0.23	1.05	1.07	0.024	0.006	..	0.21	88,000	190	250	0	No cracks
626	0.25	0.70	1.52	0.025	0.006	1.04	0.17	102,000	235	400	0	Small cracks during bending

* Two rectangular sheets are placed one on top of the other to form a cross and are fillet welded together. A bend is made along the weld.

Cracks Caused by Corrosion of a Welded Aluminum Tank

(*Continued from page 131*)

Investigation revealed the following facts:

1. The inner surface of the plate which was constantly in contact with the contents of the tank was uniformly roughened by corrosion.

2. At a distance of about 1.6 inches from the weld the plate parallel to the weld was cracked through. On both sides of this crack were numerous more or less deep parallel cracks.

3. The half-hard plate was annealed and recrystallized by the heat of welding.

4. The cracks occurred in the annealed zone parallel to the weld.

5. There was no intercrystalline corrosion.

6. Numerous cracks originated in sharp scratches.

The type and location of the cracks favor the conclusion that they were caused by simultaneous corrosion and tensile stress at those parts which had been softened (low yield strength and tensile strength) to the greatest extent by the heat of welding and which had been sharply scratched. The scratches prevented ingress of oxygen and intensified the corrosion. Besides tensile stresses caused by the loading of the tank, shrinkage stresses due to welding may have been present whose effects may have been intensified by the rapid changes in temperature to which the tank was subjected in service.

Tentative German Specifications for Welding Rods for the Gas Welding of Copper*

I QUALITY

Surface

The surface of the rods shall be purely metallic. Special coatings are permissible.

Behavior During Welding

The rod must flow uniformly and must not spatter to any great extent. It must not foam to any great extent and shall form a fluid slag with a suitable flux.

Rods for vertical and overhead welding should not be too fluid and must deposit beads of good appearance.

Composition

The rod must be technically free from the injurious impurities listed in DIN 1787. The rod must contain at least 98% copper.

The arsenic, phosphorus, lead, iron and oxygen contents must be:

As + P = 0.24% max.
Pb = 0.03% max.
Fe = 0.03% max.
O_2 = 0.03% max.

Mechanical Properties of Welded Joints

Tensile Strength = 26,100 lb./in.² min.
Bend angle for plate 0.20–0.39 inch thick = 180° min.
Bend angle for plate 0.47–0.63 inch thick = 120° min.

* Abstract of DIN Vornorm E1192 Entwurf 1, published in *Maschinenbau* 22 (5/6) 155–156, March 1939. The Welding Committee of the German Society of Engineers (VDI) prepared the Specifications, which will remain tentative until further progress in welding rods has been made. The Specification is patterned after DIN 1913, Welding Rods for Arc and Gas Welding of Steel. Discussion should be sent to Deutschen Normenausschusses, Dorotheenstrasse 40, Berlin N W 7.

II TESTS FOR QUALITY

Welding Tests

1. Preliminary Tests on Behavior During Welding.

Flow of weld metal and loss by burning (slag loss) is determined on a plate 8 × 4 inches of the material to which the welding rod is to be applied. The rod diameter must be adjusted to plate thickness.

The plate shall be preheated to a dark red before the test is begun. If several rods are to be tested the plate must be preheated anew before each rod is tested, in case it has cooled between tests. The size of the torch must correspond with the size of the rod.

A suitable flux must be used (see Section I).

2. Mechanical Testing of Welded Specimens

If the Welding Tests are satisfactory two plates 12 × 5 inches of the material for which the welding rod is to be used are welded together along the long edge. The conditions of Section I must be met. The size of the torch must correspond with plate thickness.

Edge preparation:

For plates up to 2 mm. (0.08 inch) thick, unbeveled
For plates over 2 mm. and up to 8 mm. (0.32 inch) thick, V weld
For plates over 8 mm. (0.32 inch) thick, X_1 weld.

Scarf angle and root spacing are selected according to shop practice.

Specimens for tension and bend tests are machined from the welded test plate.

The following specimens are tested:

2 specimens in the tension test
2 specimens in the bend test

The weld is hammered at a dark red heat, perhaps after about 60 to 120 mm. ($2^1/_4$ to $4^1/_2$ inches) have been welded. The hammering may be done from both sides simultaneously or from one side against a support. A compressed air hammer weighing about 8.8 lb. is recommended. After being hammered, the weld must contain no cracks. Before being tested, the weld is machined flush with the plate.

At the purchaser's request unhammered as well as hammered welds may be tested, but lower values than those in Section I. must be allowed for the strength and ductility of the unhammered welds.

THE ENGINEERING FOUNDATION

Welding Research Committee

Sponsored by the American Welding Society « « » » and American Institute of Electrical Engineers

Supplement to the Journal of the American Welding Society, May 1939

COATINGS AND FLUXES IN THE Welding of Steel

A Review of the Literature to Jan. 1, 1938

By W. SPRARAGEN* and G. E. CLAUSSEN†

This report is prepared under the auspices of

THE LITERATURE DIVISION OF THE ENGINEERING FOUNDATION
WELDING RESEARCH COMMITTEE

TABLE OF CONTENTS

	Page
Summary	153
Introduction	154
Electrode Coatings	154
Composition of Coatings	155
Manufacture of Coated Electrodes	157
Metallurgy of Coatings	157
Welding Slags	159
Inclusions in Rod (Spatter)	159
Inclusions in Base Metal	161
Arc Stabilizers	161
The Arc Atmosphere	162
Viscosity	162
Surface Tension	163
Miscellaneous Fluxes	163
Acknowledgments	164
Bibliography	164
Suggested Research Problems	164

Summary

The principal functions of the different constituents of electrode coatings are:

1. Provide a protective, reducing atmosphere.
2. Stabilize the arc.
3. Provide a slag of suitable characteristics to protect the molten metal without contaminating the weld.
4. Add alloying elements to the weld metal.
5. Increase deposition efficiency.
6. Perform metallurgical refining operations.
7. Facilitate welding operators in overhead welding and in securing root penetration.

Other functions are covered in the review.

Composition of Coatings

Coatings are difficult to classify. At present the division into organic and inorganic seems most useful, although some authorities prefer to subdivide the inorganic into flux coated (mainly sodium and calcium compounds) and slag coated, (mainly silica or silicates) and to include a mixed organic-inorganic type.

It is safe to say that no single type of composition of coatings or flux is the best under all circumstances. Some of the types of coatings are mentioned in this review.

Metallurgy of Coatings

As in steel making, so in welding every factor influencing the raw materials and melting operation must be considered in discussing the metallurgy of the process. Too little is known of the mechanism of globule transfer in arc or gas welding to substitute terms more appropriate to welding. From the metallurgical viewpoint, for example, it is important to know whether the globule is coated with slag during passage through the arc, and whether it passes through a layer of slag before merging into the deposit. The type of coating determines to a large extent the type of inclusion in the deposit. The composition of the deposit also changes with the coating. The rate at which metal is melted from an electrode at constant power may depend on the coating.

Slags

The slag in welding is a non-metallic substance that covers the surface of the solidified weld. If the weld is made in a neutral atmosphere with a bare rod, no slag is present. A covering of iron oxide gathers on the surface of bare electrode welds deposited in air. The slag on welds made with covered electrodes may resemble the composition of the coating, excluding volatile or combustible constituents, and may be enriched in iron oxidized from the electrode. The physical behavior of the slag during welding is not fully understood. The slag seems to pile up just behind the arc with covered electrodes but whether the globules pass through the slag or are covered with slag during passage through the arc space, or whether the slag is stirred into the

* Secretary, Welding Research Committee.
† Research Assistant, Welding Research Committee.

metal is not known. Any constituent of a slag that remains solid throughout the welding process must exert very little beneficial effect.

Inclusions in Rod (Spatter)

Inclusions of gas or non-metallic material, such as silicates and sulphides, have been blamed on a number of occasions for the erratic behavior of an electrode. Spatter loss in bare electrodes is also affected by cold working.

Inclusions in Base Metal

Inclusions in base metal, it may be supposed, exert the same effects as inclusions in the filler rod. Since the base metal may be heated more or less rapidly than the filler rod, the effects may be more or less pronounced.

Arc Stabilizers

Constituents often are added to coatings to stabilize the arc. Of the influences that increase stability, the more important are increase in open-circuit voltage, decrease in arc length, and decrease in cathode and anode drops and in voltage gradient in the arc column.

The decrease in drops and gradient is affected by coatings in two ways. First, the coating may form a solid sleeve around the arc column in the vicinity of the electrode, which may reduce wandering of the arc on the tip of the electrode or may offset dilution of the ionized arc column by surrounding atmosphere. Second, the coating may contain materials exhibiting a high thermionic emission which increases the number of ions or electrons in the arc column at a given voltage. Arc stabilizers in the coating, therefore, decrease the arc voltage (decreased drops and gradient) for a given length and current. Non-stabilizers may be inert or may raise the arc voltage by diluting the arc column with unionized gas.

Coatings and Fluxes in the Welding of Steel

INTRODUCTION

A STUDY of the literature on welding steels would be incomplete without consideration of coatings and fluxes. In the reviews of literature on Welding Alloy Steels and Cast Iron, coatings and fluxes were discussed in describing welding procedure and recovery of alloying elements. It must have occurred to many readers of these reviews that there must be well-established theories and quantities of fundamental information underlying the application of coatings and fluxes. The present review was intended to present these fundamental aspects, of which, unfortunately, there are all too few, and these qualitative. Qualitative statements, it may be argued, are the only sort by which to express the effect of any variable on factors such as fluidity and penetration, which depend closely on other factors as well as on coatings and fluxes. The same sort of argument intended to demonstrate the impossibility of reducing slag technique to quantitative terms impressed steel makers for many years, but should not long retard progress in welding.

The review makes it clear that coatings and fluxes are confined with few exceptions to arc and gas welding in which the temperature may be extraordinarily high and the weld metal is exposed to the air for a relatively long time. Prevention of oxide formation, solution of refractory oxides in a fusible slag, and production of an inert atmosphere are some of the more important functions of coatings and fluxes. Nevertheless, fluxes are occasionally used in other processes, and even in spot welding the welded area may contain traces of oxide or slag. In flame cutting flux is never used, yet the slag is an important element in the process. Furthermore, small quantities of slag are present in all steels. Upon being fused, the slag is free to react with the metal and to continue in perhaps a distorted way the process of refining that was interrupted by solidification in the ingot mold.

The problem of evaluating the effect of a single constituent of a coating or flux on all welding factors is baffling and remains to be solved. Nevertheless, there is an important body of information available on fluxes and coating in the welding of steel, part of which has been dealt with in numerous earlier reviews, and the remainder of which, the more general topics, is summarized in the following sections.

ELECTRODE COATINGS

The functions of the different constituents of electrode coatings are:

1. Provide a protective neutral or reducing atmosphere[1,2,3,4] (with large amounts of free hydrogen to keep oxygen[5] and nitrogen away from the weld).
2. Stabilize[2,3,5] the arc (liberate electrons at relatively low temperature to ionize the arc gap before the metal melts[6]).
3. Provide a slag of low melting point[4] (sometimes nearly the same as base metal so that both melt at the same rate,[6] the coating gasifying or melting completely[7]).
4. Provide a slag of low density.[4,7]
5. Provide a slag of low viscosity[2,4,6] (not too viscous, otherwise slag is trapped in weld metal[3]).
6. Make good the losses of metallic elements in the electrode by burning.[3]
7. Add alloying elements to the weld metal[2,8] (to raise the tensile strength, or to reduce aging, or to produce nuclei for grain size control).
8. Permit a longer arc to be held in order that the globules, which are smaller than with bare electrodes, do not short circuit the gap.[5] (However, if the globules are smaller with covered than with bare, it would seem that a shorter arc could be held with the covered than with the bare electrode.)
9. Perform metallurgical refining operations, such as deoxidation.[2,6]
10. Change the apparent resistance of the arc gap, in this way controlling deposit efficiency.[6]
11. Retard the rate of cooling and solidification of weld metal.[6] (The annealing effect of the slag, particularly if it protects the weld from oxidation is said to reduce shrinkage stresses.[1,2,3] Since shrinkage stresses appear at relatively low temperatures, E. Helin (private communication, Dec. 1938) believes that the slag can exert but slight influence; furthermore, particularly in fillet welds, cooling rate is determined by base metal, the presence of slag being secondary.)

12. Facilitate specific welding operations, such as overhead welding and fillet welding.

13. Permit use of different types of current, for example, coated electrodes for a. c. welding of 14% Mn steel.

14. Reduce spatter or fume.

15. Produce a weld of desired smooth contour.

16. Provide slag that is easily separated from the deposit (low coefficient of expansion[7] or transformations).

17. Provide a slag that is immiscible in weld metal.[7]

18. Dissolve scale or rust on surfaces to be welded.[9]

19. Provide a slag to cover the weld puddle completely.[10] (The arc can only be maintained with a bare region of the pool.)

Doubtless other purposes have been served by coating constituents, for example, materials to facilitate binding and extrusion, as well as adherence to the core rod, and many other writers than those indicated by the superscripts have discussed one or more of the functions that are listed. Nevertheless, the list is sufficiently broad in scope to emphasize the complexity of the study of electrode coatings.

COMPOSITION OF COATINGS

The richest source of literature on coating compositions is the files of the patent offices, as a glance through Simon's[11] books will convince any one. Kjellberg's Swedish patent in 1907 for covered electrode is regarded as the first by Mathias.[12] A resumé of coating patents in this country is provided by Bliss,[13] as well as by Notvest,[6] who showed that slag forming constituents (presumably non-combustible and having a high boiling point) were present to the extent of 40 to 60% in several prominent patented coatings.

Coatings are difficult to classify. At present the division into organic and inorganic seems most useful, although some authorities prefer to subdivide the inorganic into flux coated (mainly sodium and calcium compounds) and slag coated, (mainly silica or silicates) and to include a mixed organic-inorganic type. Svantesson[9] divided inorganic coatings into those with hematite, magnetite and occasionally Mn ore, and those with iron and manganese oxides, whereas Garriott[129] classified electrodes as gas-shielded and slag-shielded, the latter being of two types: high TiO_2 and high iron oxide, Table 1. E. Helin (private communication, Dec. 1938) divides the inorganic type into oxide coatings and silicate coatings.

It has seemed advisable to avoid discussing coatings from the class standpoint and to adopt the viewpoint of "essential constituent" instead. It is true that many coatings contain a number of essential constituents, and that an essential constituent may represent a small fraction of the total coating. Furthermore, different constituents in the coating may yield the same eventual constituents in the slag. For these reasons, essential constituent has been taken to mean any constituent that is the main subject of discussion.

One of the oldest essential constituents is asbestos, of which there are two varieties, blue and white. Blue asbestos is a fibrous iron silicate ($52SiO_2$, 40FeO, 2MgO, $1Al_2O_3$, remainder not reported[14]) the softening point of which, when made into a coating with sodium silicate may[9] be 800°C. According to Sinfield,[15] the blue asbestos coating decarburizes the weld metal and lowers the manganese content. Shepherd and Moritz[10] state that the function of blue asbestos is to absorb metallic

Table 1—Types of Electrode Coatings for Mild Steel. Garriott[129]

Constituent	Gas-Shielded Type	Slag-Shielded Type: High TiO_2	Slag-Shielded Type: High Iron Oxide
	Percentage Composition*		
SiO_2	28.32	21.75	29.42
Fe_2O_3	2.54	2.14	18.73
TiO_2	12.00	47.88	19.31
MnO_2	15.85	8.35	19.60
CaO	0.51	3.87	4.13
MgO	2.99	2.99	4.49
Na_2O	7.20	3.66	6.30
Al_2O_3	0.11	4.19	...
MoO_3	2.25	...	...
Volatile	30.66	8.17	8.17
Total	102.43%	103.00%	110.11%
Recipe for extruded coating (parts by weight)			
Sodium silicate (42.5° Be)	80	40	70
Titanium oxide (pptd.)	12	..	..
Rutile	..	50	20†
Manganese carbonate	10	..	15
Talc	10	10	15‡
Ferromanganese	5	5	5
Wood flour	24	3	..
Ferromolybdenum	$2^1/_2$	..	..
Calcium carbonate	..	6	6
Iron ore	..	..	20†

* Ferroalloys and complex silicates are reported as oxides.

† Ilmenite (50% TiO_2, 50% Fe_2O_3) may be substituted for iron ore and rutile.

‡ Or Asbestos.

oxides in the molten state. Although Zeyen[16] considered that better results are obtained with asbestos covered and other covered electrodes positive than negative, E. Helin and H. Groebler (private communication, December 1938) stated that most slag covered electrodes are connected to the negative pole.

White asbestos is hydrated magnesium silicate and yielded excellent results in Baumgärtel's[17] tests. MgO was thought to dephosphorize the weld metal, here as elsewhere the analogy between the weld puddle and the steel furnace governing the judgment of the coating compounder. The ease of handling asbestos was a large factor in determining its use. According to Bliss,[13] an easy flowing light slag is obtained if anthophyllite (mainly magnesium ferrous silicate) is added to the coating. White asbestos mixed with blue, or with china clay, or with sodium carbonate, chloride, borate and CaF_2 was mentioned by Goodger.[14] The binder was sodium silicate.

Mathias[12] stated that sodium silicate (water glass) is almost universally used as a binder. However, Adams[18] regarded water glass as an agent to retard combustion in a coating consisting of carbohydrates. In addition to these purposes, the Na_2O improves the arc characteristics and the SiO_2 aids in forming the slag, according to Garriott.[129] Excessive sodium silicate lowers the freezing point of the slag too greatly and will prevent the slag having fast freezing characteristics.

"The combination of cellulose with sodium silicate forms a crater at the end of the electrode which directs the gas into the arc space. The silicon is reduced to the metal where it recombines to form SiO_2 and rids the weld of FeO." Notvest[6] quotes this statement from a patent debate and believes that free FeO–MnO mixtures are converted to silicates through waterglass in the coating. High temperature and violent agitation aid in completing the reaction. He believed the coating must contain at least 25% to secure good fluidity, which is in agree-

ment with Kinzel's[19] statement that coatings almost invariably consist largely of SiO_2.

Another constituent of early coatings was lime, which also was supposed to dephosphorize[17] the weld metal. Since low phosphorous core rods were nearly always insisted upon, it is surprising that so much emphasis used to be placed upon the dephosphorizing and desulphurizing capacities of coating constituents. Here again no distinction was made between steel refining and welding. A dipped coating of slaked lime has been used. Silica, waterglass, or Fe_3O_4[20] sometimes were added to the limestone. Garriott[129] adds calcium carbonate to slag coatings to produce a basic slag and to permit straight polarity welding. The latter characteristic is a peculiarity of calcium carbonate not possessed by barium or strontium carbonate. The CO_2 liberated by calcium carbonate forms a gas shield. A mixture of 65% CaO 35% SiO_2 (m. p. = 2120° C.) was Green's[7] choice, who found that a rod coated only with silica gave a light snappy sputter. The compound $CaO.FeO.SiO_2$ was aimed at in some coatings[14] to which sodium silicate or carbon was added.

Graphite is used as a deoxidizer. It is said to oxidize less rapidly than Al and Mg but may cause hard spots at the crater.[14] In 1918 Jeffries[21] stated that Mg was a good deoxidizer in coated electrodes. Welds made with Armco iron electrodes coated with magnesium were extraordinarily ductile. Other metals added to coatings for the purpose of deoxidation have been Al (as wire[22] or as ferro Al or ferro-Ti-Al[23]), Si,[6] Mn,[24] Ti,[25] V,[25] and boron[26] as suboxide. Notvest[6] favors silicon and manganese, possibly from the cost standpoint. Wood flour[129] is also used as a deoxidizer and produces a friable slag that is readily removed. SiC is not an efficient source of silicon, according to Bliss.[13] Nickel and lead have been suggested as deoxidizers,[14] the former erroneously, the latter without caution. In addition to the more common alloying elements added to coatings, Bliss[13] mentions V, U, Ce, Co, Mo, Al and Zr to increase the strength of the steel weld metal. The claim has been made[27] without support that metallic ingredients in a coating reduce loss by volatilization. Although Hurme[8] feels that alloying elements are always better added through the coating than through the rod, there are many exceptions, for example 18–8 and 14% Mn steel.

The functions of titanium oxide, according to Garriott,[129] are: (1) Improve the arc stability, (2) Remove iron oxides and sulphur from metal to slag; (3) Form nitrides (composition not stated) and remove them to slag; (4) Reduce the surface tension of the slag, thus reducing undercutting. Sound, strong 90° V butt welds were produced by Keidansky[139] with a coating containing 12% TiO_2, 39% waterglass, 7% Talc, 5% gum arabic, 10% ferromanganese, 17% water. The melting points of TiO_2 and ilmenite are said to be 1800 and 1350° C., respectively. A sodium titanate ($Na_2Ti_3O_7$) melts in the vicinity of 985–1126° C.

Coatings based on borax have been used in Japan, according to Okada.[24] Potassium ferrocyanide, sodium bisulphate, and manganese dioxide were other unusual additions to these coatings. Another Japanese investigator[135] reports good results with carbonates of lithium, sodium, strontium and barium. Carbonates of magnesium and manganese favored porous deposits. Strangely enough, Green[28] found that thorium oxide coatings completely wet the molten globule despite the high melting point of ThO_2. Electrodes covered with aluminum paint or with an electrodeposited layer of nickel were used at one time.[20] An English writer[29] found that dipping in paraffin protected coatings from moisture. Varnish has been used for the same purpose. Moisture in the coating as water of crystallization, such as hydrated sodium borate, was found by Ônisi[136] to create porosity. If the water was uncombined (coating contained 51% SiO_2, 22% MgO, 16% Fe_2O_3, 5% CaO, 2½% NaCl + KCl, 4% loss on ignition, 0.89% water), penetration and density were favored. According to H. Groebler (private communication, Dec. 1938), arc voltage and stability depend on moisture in the coating. Groebler mentioned that excessive moisture favors oxidation, and that the form of the moisture, whether combined or free, is important. From a study of 55 types of commercial mild steel covered electrodes, Sekiguchi[142] concluded that for a constant quantity of coating the ductility and Mesnager impact value of four-layer deposits increase as the ignition loss increases.

Common coating constituents used for forming a slag, according to Mathias,[12] are SiO_2, MnO_2, FeO, amphibole (complex silicate of magnesium, calcium and iron), rutile (95% TiO_2) and feldspar (chiefly aluminum silicate). Five coatings based on feldspar were used by Keidansky[139] to produce 90° V butt welds in mild steel. A coating consisting of feldspar with waterglass binder did not melt rapidly enough; possibly additional waterglass would have lowered the melting point. If 6% aluminum was added with or without a wrapping of cotton cloth, the slag was voluminous and viscous, and the weld was porous and weak. Less aluminum (0.3%) might have been more effective. Weak brittle welds were produced by a coating containing 39% feldspar, 21% ilmenite, 9% ferromanganese, 31% waterglass. On the other hand, a coating consisting of 24% feldspar, 18% ilmenite, 14% asbestos, 9% ferromanganese, 28% waterglass, 7% water produced strong, ductile welds (tensile strength = about 60,000 lb./in.^{2}, reduction of area = 50%). Garriott[129] added MnO_2 or $MnCO_3$ to coatings as a slipping agent and to remove FeO from metal to slag. $MnCO_3$ also vaporizes in the arc, the Mn forming a reducing atmosphere. Garriott believes that the Mn in some way reduces CO_2 to CO, the latter constituent being the most efficient as a reducing atmosphere surrounding the weld metal. Ferromanganese is added as deoxidizer, whereas ferromolybdenum is added solely to alloy with the weld metal. Coatings based on FeO, MnO and SiO_2 have been studied a great deal in Russia. A good coating[30] for an electrode containing 0.25 C was said to contain 22–34 Fe_2O_3, 64–57 pyrolusite (60 Mn, 20 SiO_2, 4 CaO), 13–15 $CaCO_3$, 1–1.4 Al_2O_3. Another Russian coating[31] contained 15 ferromanganese, 12.5 waterglass, 30–20 ilmenite, 15–25 pyrolusite, with 25 feldspar or kaolin. Tests on twelve different coatings of this type by Jerochin[32] showed that weld quality (strength and ductility) decreased in direct proportion as MnO was increased, and as FeO and SiO_2 were decreased. A discussion of the FeO–MnO–SiO_2 system led to the conclusion that the best slag contains 44 SiO_2, 30 FeO, 26 MnO (melting point below 1200° C.). As H. Groebler pointed out (private communication, Dec. 1938), under other circumstances an increase in manganese content, whether as oxide or metal in the coating may not lower the mechanical properties. Garriott[129] used talc as a slipping agent in extrusion and to produce a light, friable slag. Alumina is sometimes substituted for MnO, as in Okamoto's[33] coating: 45 SiO_2, 25 FeO, 20 Al_2O_3, 5 MnO, 3 CaO, 2 MgO. Blast furnace and other metallurgical slags[26] have also been used in coatings. Diegel[25] was of the opinion that deoxidizers may do more harm than good. Without supplying details, Leitner[34] stated that Al, Si, Zr and Mn decrease the number of globules per second transported through the arc space, which is a disadvantage.

Leitner's results apply only to the cored electrodes which he investigated.

Early tests by Neese[35] in which coated electrodes yielded disappointing results showed that the coating may fulfill all the requirements believed to be important yet fail in the test. Neese used CaO, Al_2O_3, B_2O_7, SiO_2, NaCl, Na_2CO_3, CaF_2, C, Si, Al, Mn, Mg and other substances on mild steel core rods 0.16 inch diameter, 150 amp. A coating containing potassium dichromate was not successful in Keidansky's[139] tests. The coating contained 60% $NaHCO_3$, 38% borax glass, 2% potassium dichromate, with waterglass as binder. The arc was wild, and a coating of red oxide surrounded the brittle weld, the oxidation being due to the dichromate.

Regè[36] compounded coatings for nickel and manganese steels and 18–8 from different proportions of sodium silicate, talc (hydrated magnesium silicate), pumice (70 SiO_2, 15 Al_2O_3, soda, etc.), iron and manganese oxides, ferromanganese, ferrosilicon and graphite. For plain carbon steels, coatings containing 30 $CaCO_3$, 35 CaF_2, 20 Na_2SiO_3 and different proportions of metallic manganese were proposed. A double coating was said by Inglis and Andrews[37] to be successful for heat resisting Cr-Ni and Si steel. The rod (0.20 inch diameter) was dipped in a mixture of 15 parts waterglass, 50 parts CaF_2, 10 parts potassium chromate, 5 parts ferromanganese (75% Mn), 5 parts ferrosilicon (75% Si), 10 parts water and was dried at 150–160° C. The first coating which is 0.020–0.028 inch thick is covered with a second mixture consisting of 25 parts $CaCO_3$, 15 parts CaF_2, 5 parts ferromanganese, 6 parts $Ca(OH)_2$, 30 parts waterglass, 10 parts water.

For vertical welding, Alov[38] found that a good coating contained 2 parts by weight sodium acetate, 1 part ($NaOH + Ca(OH)_2$). Contrary to Adams'[18] statement that slag covered electrodes are unsuitable for overhead welding, E. Helin and H.Groebler (private communications, Dec. 1938) observed that in Europe nearly all overhead welding is performed successfully with slag coated electrodes. However, other factors also are important, as Esslinger[39] points out.

Instead of coating the electrode, a Russian welder[141] feeds a bare steel electrode through a layer of flux 0.50–0.79 inch deep consisting of 57% titanium concentrate, 23% kaolin, 19% ferromanganese, 1% charcoal in welding thick steel at 30 ft./hr. The arc is well stabilized, the metal is protected from oxygen and nitrogen pickup, and the slag is uniformly distributed over the weld.

MANUFACTURE OF COATED ELECTRODES

The manufacture of coated electrodes has not been given much publicity. Sull coatings are formed, according to Rose,[40] by spraying (not dipping) the rod with water to obtain a coating of ferric hydroxide, followed by dipping in lime and baking at 150° C. The baking removes hydrogen, which causes acid brittleness. An extrusion machine for coating rods is described by Redline.[41] Cleanliness of the rods before coating is essential. Pressures up to 14,000 lb./in.^{2} are exerted by the extrusion press mentioned by Eisheuer.[42] A detailed description of dipping and extrusion processes for asbestos covered electrodes was given by Jackson,[43] who found that stranded wire for reeled coated electrode wire was unsatisfactory. The strands often broke. A flow sheet has been prepared[125] for the manufacture of covered electrodes involving blue asbestos cord and extruded flux. Davy[126] illustrates the contact stamping machine, extruding press and machine for cleaning, straightening and cutting used in manufacturing covered electrodes 8 ft. long for automatic welding.

The gassing of electrode coatings by air desorbed from the fine particles and by acetylene generated by the ferromanganese and ferrosilicon was overcome by Bliss[13] through the medium of non-gassing ferroalloys. The method of preparing the non-gassing material was not explained. The best treatment to secure a firm coating of a mixture of 40 $CaCO_3$, 40 CaF_2, 20 Na_2SiO_3 (35–36° Be) consisted of holding the coated rod in a gas rich in CO_2 or in a blast of air heated to 80–90° C., Regè[36] found.

METALLURGY OF COATINGS

As in steel making, so in welding every factor influencing the raw materials and melting operation must be considered in discussing the metallurgy of the process. Unlike steel making, however, in which time, temperature, hearth and agitation factors are vastly different from welding, the terms acid and basic, killed and rimming, melt down, boil and refining have little significance from the standpoint of the weld puddle. Too little is known of the mechanism of globule transfer in arc or gas welding to substitute terms more appropriate to welding. From the metallurgical viewpoint, for example, it is important to know whether the globule is coated with slag during passage through the arc, and whether it passes through a layer of slag before merging into the deposit. The methods of physical chemistry have been applied to the study of coatings with negligible success by Helin and Svantesson,[44] Diatlov[45] and Erokhin.[46] Miller[47] attempted to employ Brinell's ternary diagram showing the required Mn, Si and Al contents for deoxidized ingots (top cast, acid steel, cold molds, walls 2 inches thick) in studying bare electrode welds, but the results were not convincing despite allowances for loss by volatilization in the arc.

Aside from protecting the metal, Osterle[48] believes fluxes play no metallurgical rôle in welding. Since protection implies oxygen and nitrogen contents, upon which the content of carbon, silicon, manganese and other elements are dependent to some extent, Osterle's extremist view is self contradictory. Furthermore,

Table 2—Composition of Weld Metal Deposited by Coated Electrodes Having the Same Steel as Core Rod. Llewellyn[49]

		Composition of Weld Metal				
	Core Rod	Bare Electrode	Ferrous Silicate Coating	Blue Asbestos Coating	Iron Silicate & Metallic Alloys	Calcium Oxide
C	0.09 –0.12	0.05	0.05	0.06	0.13	0.09
Mn	0.34 –0.49	0.012	0.004	0.008	0.48	0.25
Si	0.02 –0.03	0.002	0.040	0.007	0.02	0.003
P	0.022–0.034	0.018	0.007	0.012	0.015	0.011
S	0.034–0.040	0.025	0.022	0.028	0.034	0.023
N_2	0.005–0.008	0.113–0.128	0.095–0.106	0.028–0.053	0.010–0.011	0.014–0.018
O_2	0.006–0.009	0.115–0.167	0.111–0.123	0.038–0.067	0.009–0.019	0.021–0.033

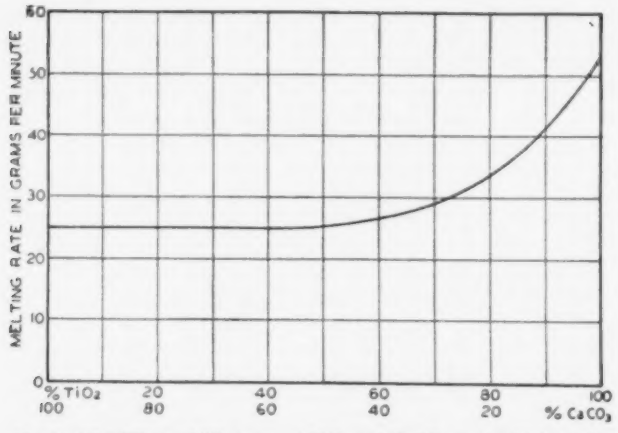


Fig. 1—The Effect of Mixtures of TiO_2 and $CaCO_3$ on the Melting Rate of a Light Flux Coated Electrode, 5/32 Inch Diameter, 190 Amp. Mathias[1]

investigations by Hoyt and others discussed in other reviews show that the type of coating determines to a large extent the type of inclusion in the deposit, and Llewellyn's[49] results, Table 2, illustrate the well known fact that the composition of the deposit also changes with the coating. The same core rod and plate were used throughout. The ferrous silicate coating without alloys afforded little protection from the atmosphere and, contrary to expectations, dephosphorized the core rod. The silicon content was unchanged. Metallic alloys (no details) added to the ferrous silicate had a remarkable effect in reducing oxygen and nitrogen pick up. The manganese and carbon contents were sustained, doubtless by the ferroalloys. Blue asbestos, which is itself iron silicate, as well as lime did not prevent loss of silicon. The action of lime is not necessarily associated with its basic character, because there was complete loss of silicon from the bare electrode. Were silicates present, the addition of lime would slightly increase the manganese content of weld metal. The effect is small, however, and it is difficult to account for the good recovery of manganese from the lime coated electrode, unless lime hinders burning of the manganese. In view of the incomplete information on the composition and thickness of coating as well as on current and arc length it is hazardous to draw conclusions from the investigations made by Llewellyn and many others.

In general, Moressée's[50] results, Table 3, confirm Table 2. The second is a typical acid Bessemer slag. The high MnO content preserved the manganese content of the rod and may have deoxidized the melt, although the results of two Russian investigators,[51] Table 4, fail to substantiate fully the deoxidizing effect of manganese. The manganese was added to the coating as ferroalloy; TiO_2 was added as such. No ferrosilicon was added to the coatings. The slags contained up to 6% CaO, 0.7% MgO. The core rods were 0.16 to 0.20 inch diameter, 160–220 amp. The sulphur and phosphorus contents of core rod were each 0.01–0.05% and were higher in weld metal than in core rod. To a limited extent oxygen and Mn contents of weld metal were proportional to FeO, and MnO content of slag, respectively. The best static tensile ductility and notch impact value were secured with 12 to 25% TiO_2 in the coating. An automatic arc-welding wire designed by Buschtedt and Diatlov[52] was cross shaped in section, the grooves being filled with a mixture of 10 TiO_2, 29.5 SiO_2, 6 Al_2O_3, 14.5 FeO, 10 MnO, 6 K_2O and Na_2O, 5 Mn, 2 Fe, 17 organic material. As the content of organic material was increased from 5 to 40% the composition of weld metal changed from 0.11 C, 0.44 Mn to 0.13 C, 0.77 Mn.

There is no rule for the thickness of a coating. From the standpoint of recovery of alloying elements, the efficacy of a coating increases rapidly as its thickness is increased until, a critical thickness corresponding to high recovery having been attained, further increase in

Table 3—Composition of Weld Metal Deposited by Coated Electrodes. Moressée[50]

Composition of Rod					Composition of Coating						Composition of Weld Metal				
C	Mn	Si	P	S	FeO	SiO_2	CaO	MnO	CO_2	Al_2O_3	C	Mn	Si	P	S
0.06	0.46	0.018	0.033	0.038	26.8	26.4	8.3	24.5	3.5	8.6	0.16	0.74	0.018	0.045	0.048
0.07	0.41	0.023	0.018	0.026			Bare				0.02	0.06	0.018	0.020	0.023
0.07	0.41	0.023	0.018	0.026	12.7	45.8	1.4	28.4	...	12.8	0.04	0.30	0.050	0.042	0.028

Table 4—Composition of Weld Metal Deposited by Coated Electrodes. Kusmak and Abramova[51]

Composition of Core Rod			Composition of Slag						Composition of Weld Metal				
C	Mn	Si	SiO_2	FeO	Fe_2O_3	MnO	Al_2O_4	TiO_2	C	Mn	Si	N_2	O_2
0.10	0.43	0.009	27.7	52.2	4.3	2.3	6.5	0	0.06	0.01	0.005	0.035	0.187
0.10	0.78	0.06	35.2	16.9	6.5	35.3	4.8	0	0.15	0.51	0.02	0.037	0.062
0.06	0.35	0.009	31.3	18.0	3.1	26.7	9.9	6.7	0.18	0.56	0.04	0.047	0.111
0.12	0.41	0.009	23.0	25.5	3.6	21.0	6.1	13.6	0.10	0.44	0.04	0.036	0.052
0.19	0.54	0.01	20.7	22.3	5.0	18.0	4.3	24.7	0.12	0.33	0.46	0.035	0.118
0.14	0.49	trace	32.8	5.9	10.0	14.9	0	31.5	0.13	0.28	0.09	0.012	0.039

Table 5—Effect of Coatings on Time and Power to Deposit an Electrode. Paterson[3]

Coating	Polarity of Electrode	Arc Voltage	Arc Energy, Kw.	Time for Run, Sec.	Length of Run, Inch	Length of Crater, Inch	Time Crater Remained Molten, Sec.	Kw.-Hr. per 100 Grams of Core Rod
Bare	−	21	2.94	83	5.75	0.30	4.33	0.158
$CaCO_3$	+	20.5	2.87	90	10.75	0.50	4.38	0.167
"Standard Coating"	+	24.0	3.36	118	10.75	0.40	4.39	0.256
Ditto plus 15% cellulose	+	28.0	3.90	105	9.75	0.46	4.95	0.266

thickness is unnecessary. In the same way exclusion of nitrogen and oxygen from the weld likewise varies with thickness. For example, Helin,[125] showed that as the thickness of coating was increased from 0.033 to 0.049 inch, the nitrogen content of the weld decreased from 0.055 to 0.025%, the ductility rising (no details of coating). On the other hand, if arc stability is the prime requisite an extremely thin coating may be as effective as a thick coating. Sekiguchi[142] found that ductility and toughness increased as the thickness of coating increased in testing 55 commercial covered mild steel electrodes. Arc voltage and energy to melt unit length of electrode also increased.

The rate at which metal is melted from an electrode at constant power may depend on the coating. According to Mathias,[12] TiO_2 has a pronounced effect on the melting rate, Fig. 1. The coating was extremely thin, only 0.0001 inch thick, on a low carbon steel electrode $^5/_{16}$ inch diameter, D.C., 190 amp., electrode negative. The melting rate increased from 25 gm./min. with $CaCO_3$ to 53 gm./min. with TiO_2. Mathias stated, without proof, that the depth to which base metal is penetrated by the arc is inversely proportional to the melting rate. It seems that TiO_2 alters the distribution of thermal energy in the arc. Bliss[13] stated that the presence of titanium in the slag reduces the content of sulphur and metallic oxides in weld metal (no details).

The viewpoint that coatings decrease the rate at which metal is melted from an electrode at constant power was upheld by Paterson,[9] Table 5. Single runs of weld metal were deposited on mild steel $^3/_8$ inch thick, 12 inches wide. Arc length, inclination (60°) of electrode and speed of travel were the same in all tests. The electrodes were 0.160 inch diam., 140 amp. and $16^1/_2$ inches of electrode were deposited. Column 7 was calculated from Columns 4, 5 and 6. The $CaCO_3$ coating was extremely thin (2.0 grams $CaCO_3$ per electrode 18 inches long). The so-called standard coating consisted of 70 parts Fe_3O_4, 30 parts SiO_2 (both finer than 120 mesh) mixed with sodium silicate (1 Na_2O, 2 SiO_2) and water, the binder having density of 1.250 at 150° C. The coating consisted of 500 parts by weight Fe_3O_4–SiO_2 mixture and 250 parts by weight silicate solution. After three dips and grinding for concentricity the rod contained 1.24 grams of coating per inch. The coating softened at 1100° C. and the slag had a specific gravity of 4.18, which was said to be within the range of slags from commercial coatings, namely 3.6 to 4.35.

Table 6—Length of Sleeve Has No Effect on Arc Voltage. Paterson[9]

Coating	Length of Sleeve, Inch	Arc Voltage
"Standard Coating"	0.03	24
Ditto plus 15% wood flour	0.07	29
Ditto plus 15% wood flour and 10% $CaCO_3$	0.08	24.5

The results showed that, far from hastening the melting of the electrode, the coating retarded the rate of deposition and increased the power required to deposit the metal. Tests at 160 amp. yielded similar results. It was estimated that about $1^1/_2$ times as much energy is absorbed by 1 lb. of slag forming material as by 1 lb. of weld metal. Furthermore, as the arc energy is increased the amount of heat per pound of metal increases because radiation, conduction and volatilization losses increase. For a given arc energy the bare electrode weld metal remained molten longer than the covered electrode weld metal. It was believed that the hydrogen from the cellulose raised the arc voltage of the last electrode in Table 4. The belief that the projecting sleeve of coating from some covered electrodes lengthened the arc and raised the arc voltage was disproved by Paterson, Table 6. The core rod was 0.16 inch diameter, 140 amp.

In 1927 Brophy[53] tested an electrode consisting of a mixture of powdered iron and slag (no details) drawn down inside a seamless tube. Although the core had a higher melting point than the tube, the core melted first during welding leaving a concave end. Brophy believed that carbonaceous coatings provide a gaseous shield and flux coatings a liquid shield (no details).

SLAGS

The slag in welding is a non-metallic substance that covers the surface of the solidified weld. If the weld is made in a neutral atmosphere with a bare rod, no slag is present. A covering of iron oxide gathers on the surface of bare electrode welds deposited in air. In flame cutting thin iron, Schneider[54] found that the slag consisted of 50% FeO, 35% Fe_2O_3 + Fe_3O_4, 15% Fe. The slag on welds made with covered electrodes may resemble the composition of the coating, excluding volatile or combustible constituents, and may be enriched in iron oxidized from the electrode. An acid slag melted from an electrode coated with Japanese paper, special clay and metallic manganese, according to Okamoto,[55] contained 44.3 SiO_2, 20.9 Al_2O_3, 16.2 FeO, 9.4 Fe_2O_3, 5.0 MnO, 1.9 MgO, 3.1 CaO. Booer[56] pointed out that the slag should dissolve iron oxide readily and should consist preferably of silica with lime, soda or magnesia. The best welding slag, in Jerochin's[82] opinion, contains 26 MnO, 30 FeO, 44 SiO_2.

The physical behavior of the slag during welding is not understood. The slag seems to pile up just behind the arc with covered electrodes, according to Groebler,[57] but whether the globules pass through the slag or are covered with slag during passage through the arc space, or whether the slag is stirred into the metal is not known. Any constituent of a slag that remains solid throughout the welding process must exert very little beneficial effect.

Table 7—Weld Metal Deposited by Two Electrodes Coated with the Same Flux. Shepherd and Moritz[10]

	Composition of Core Rod			Composition of Weld Metal		
	C	Mn	Si	C	Mn	Si
Rod 1	0.15	1.5	0.17	0.06	0.6	0.07
Rod 2	0.1	0.5	0.02	0.06	0.35	0.07

The terms acid and basic have not been of much use in the study of welding slags. In the first place, weld metal is not deposited on a refractory hearth. Secondly, as P. L. Roberts pointed out in discussing a paper by Sinfield,[15] a flux that may have been basic before welding may become acid during welding as a result of high-temperature reactions. Furthermore, Shepherd and Moritz[10] showed, Table 7, that a flux that may be acid with respect to one core rod (Rod 2) may be basic with respect to another (Rod 1). It was stated that the basic rod with acid coating yielded better mechanical properties than the acid rod (Rod 1) with a basic coating. For this reason rimming steel is almost universally used[6] for electrodes containing 0.13–0.18 C. H. Groebler (private communication, Dec. 1938) restricts the terms acid and basic to characterize the slag after welding. Highly basic slags containing low silica and high FeO and

MnO are compact and quiet during welding. As acidity is increased the slag tends to form, while still retaining a composition yielding a suitable viscosity and softening temperature. Thus, Groebler uses the terms "compact" and "foamy" to characterize welding slags.

The acid coating recommended by Shepherd and Moritz should have little effect in increasing the silicon content. Consequently, the silicon content remains low enough to prevent contraction cavities, the weld metal undergoing presumably a little rimming during solidification. Probably this effect accounts for the statement by Sonderegger[23] and others that silicon in the core rod has an injurious action, whereas silicon in the coating is beneficial. Overhead welding was said to be favorable with weld metal containing oxide because the metal then has a low carbon content and solidifies quickly. For vertical welding too heavy a coating is undesirable because freezing is delayed.

In Notvest's[6] opinion, iron oxide always acts as a basic slag producer. Without defining terms, he stated that in an oxidizing arc iron oxide does not form silicates, whereas the reverse is true in a reducing arc, especially if MgO is present. The silicates, including calcium iron silicate, combine with alumina to form a fluid slag. For some reason Notvest believed that iron oxide catalyzed the reaction $2\ CO \rightarrow C + CO_2$ and that Ti and Mg have a basic action on SiO_2 (no explanation offered). Durham and Austin[1] and Swinden[58] pointed out the possibility of reducing silicon from slag to metal by using coatings high in silica. Swinden, like Shepherd and Moritz, referred to the effect of silicon in promoting contraction cavities and added that silicon in weld metal helps to prevent hydrogen blow by preventing the evolution of hydrogen from solution. Silicon killed core rods yield more consistent results than rimming steel core rods with a basic coating. Nevertheless, the core of rimming steel rods, which is less pure than the skin, melts faster than the skin, focuses the arc and produces better penetration than killed rods. With several others, Swinden stated that gases dissolved in the core rod during pickling must be removed by bluing, otherwise penetration may be inadequate and the weld metal porous.

Although Shepherd and Moritz favored an acid slag useful in securing good mechanical properties, Polson and Sinfield,[15,59] were of the opposite opinion. The advantage of acid slags was said to lie in easy manipulation and easy removal after solidification. It was conjectured that acid reactions (no details) generate heat which facilitates solution in the slag of rust on the plate surface, notwithstanding the fact that basic slags have a higher solubility for FeO.

INCLUSIONS IN ROD (SPATTER)

Inclusions of gas or non-metallic material, such as silicates and sulfides, have been blamed on a number of occasions for the erratic behavior of an electrode. Non-metallic inclusions produce undesirable effects in bare electrodes, according to Rapatz,[60] and are the worst defect that an oxyacetylene filler rod may have, Diegel[61] stated. Rapatz went so far as to claim that non-metallic inclusions may affect the voltage distribution in the welding arc. Although these effects attributed to non-metallic inclusions may seem strange in view of the deliberate addition of non-metallics to weld metal in the form of fluxes, coatings and cores, it seems reasonable to suppose that sulphide inclusions are always undesirable, if equally inconsequential, and silicate or oxide inclusions may exert effects which have yet to be analyzed. Green's[62] statement that non-metallic inclusions emit infra-red light (any visibly hot object does) exhausts our stock of information on the behavior of inclusions in their passage from rod to weld puddle.

One hypothesis of spatter is based on the explosion of non-metallic inclusions in the arc space.[63] S. W. Miller[64] found that sparks are iron burnt to oxide, Granjon[65] adding that metallic iron usually is present beneath the oxide crust on the spattered particle. From the standpoint of spatter, Notvest[6] regarded sulphides as worst, oxides as less undesirable and silicates as least harmful. Inclusions of MnS explode to SO_3 in some unexplained way at arc temperatures. Stringers of Fe_3O_4, FeO, carbon and MnO also were said to explode. Notvest was of the belief that gases and metal vapors collect into one large bubble which explodes and practically empties the pool. Since the solubility of iron for hydrogen and other gases increases with rise in temperature, the effect of gas inclusions in the rod might be expected to be small in comparison with the opportunity open to weld metal to absorb gases during deposition yet hydrogen absorbed by filler rods during pickling operations has often been said to be the cause of brittleness or other undesirable consequences in the weld. No proof has been adduced and the usual remedies for hydrogen absorption affect the skin of pickling products on the rod. Until evidence that inclusions can explode under any conditions has been advanced, the explosion hypothesis may be dismissed as lacking plausibility, as Doan[121] emphasizes.

Streb[66] concluded that loss by spatter in oxyacetylene welding with rods containing 0.02 to 0.085 C did not depend on composition or method of welding, although 0.10 Si was better than 0.00 Si, and 0.50 Mn was better than 0.02 Mn. Spattery rods always contained more non-metallic inclusions in the microstructure than quiet rods. Without supplying details, Fuchs[67] stated that spatter depends primarily on type and amount of inclusions in bare electrodes. The electrodes contained 0.186 to 3.16% inclusions, estimated from microstructure. High silicon content (0.20% metallic Si, 0.40% Si as SiO_2) eliminated spatter in Streb's tests but the silica made the melt viscous. It was surmised that spatter resulted from an explosion of a mixture of oxygen and hydrogen formed through reduction of FeO by the flame. An alternative explanation was proposed by Stursberg.[68] Under the influence of the sudden temperature rise in the rod, FeO and Fe_3C react with explosive violence to form CO, the immediate cause of spatter. Boiling of the weld metal and porosity[15] also were explained in this way.

The effect of electrode coatings on spatter has been investigated by Green[69] and Roberts.[70] Green applied coatings of customary thickness (1926, no details) to rods (0.16 C, 0.42 Mn, 140 amp. d. c.) $^5/_{32}$ inch diameter and deposited them vertically downward on a steel stud $1^1/_4$ inches diameter. The deposit efficiency was the ratio of weight of deposit to weight of electrode consumed. The highest deposit efficiency (83–85%) was obtained with calcium sulphide, the lowest with silica (56–65%) and boric acid (59–64%). Coatings containing oxides, sulphides, or carbonates of Pb, Sb, Zn, Al, Mn, Ca and mixtures thereof were intermediate. Soap-drawn rods had better characteristics (longer arc length) but lower deposit efficiency (83–84% instead of 85–87%) than grease-drawn rods. Bare rods were 77–79%. In general, deposit efficiency improved as the arc length was shortened. Deposit efficiencies over 100% were said to be obtained by forming oxides and nitrides. The addition of calcium carbonate to coatings high in

TiO_2 and wood flour greatly reduces spatter, according to Garriott.[129]

Roberts[70] deposited parallel beads on boiler plate 8 x 2 x 3/16 inch, 120 amp., d. c., using electrodes (0.10 C, 0.10 Si, 0.45 Mn) 1/8 inch diameter. Deposit efficiencies depended on polarity. Bright bare electrodes had efficiencies of 81 (+) to 86% (−); rusted bare electrodes were 86 (+) to 91 (−). Asbestos covered electrodes spattered excessively on the negative pole. The effect of rust on bare electrodes is to stabilize the arc (Brophy[53]), or in combination with oil, to cause porosity (Horn and Tewes[71]). The stabilizing effect is the more important (see section on Arc Stabilizers). According to Notvest,[6] mill scale rolled into the surface of a rod forms a highly gaseous electrode (no details).

Spatter depends on the type of electrode, according to Esslinger.[39] All-position electrodes generate CO and CO_2 from the coating (20–35% organic material) which smother or extinguish the arc. The loss by spatter may be 19.7% (175 amp.) to 34.0% (225 amp.) of the total weight of all-position electrode 3/16 inch diameter (coating constitutes 10.7% by weight of the electrode). Only 15–16% was lost by spatter with fillet electrodes at a deposit rate of 3.85 to 4.44 lb./hr., straight or reversed polarity. The low content (14%) of organic materials in the coating of fillet and down-hand electrodes was said to account for the low spatter loss. Taran[72] believed he had proved that the spattered particles from metal arc welding come from the electrode, not from the weld puddle, because cementite needles in austenite were observed in particles from a weld in hyper-eutectoid steel made with an electrode of the same composition, whereas when the electrode contained only 0.17 C with hyper-eutectoid base metal, the particles had the same structure as those from a low carbon steel (0.21 C) welded with an electrode of the same composition.

Spatter loss from bare electrodes was increased by cold working (up to 67% reduction) in tests made by Kessner and Specht.[73] It appeared that the decreased thermal and electric conductivity due to cold working increased the temperature of the globules, which in some way increased spatter loss. Rose[40] found that annealed, sull coated electrodes flow faster than similar electrodes that had been heavily cold drawn. The Association of Railway Electrical Engineers,[74] however, believed that hard drawn electrodes (0.16 C, 0.56 Mn) provided a steadier arc and better penetration than annealed electrodes (no explanation was offered). In overhead welding the globules tended to drip down annealed electrodes. Atkins[75] found no difference in oxyacetylene welding between an annealed rod and a rod drawn cold from 3/4 inch to 0.16 inch diameter. In arc welding the hard drawn rod crackled a little more than the annealed. Without supplying details of his tests, Swinden[58] stated that there was no difference between hard drawn and annealed electrodes. Notvest[6] reported vacuum fusion analyses of the core rods of 13 different industrial electrodes which contained 5.2 to 109.3 cu. cm. gas per 100 grams of rod. The gas content seemed to increase with increasing degrees of rolling (no details).

INCLUSIONS IN BASE METAL

Inclusions in base metal, it may be supposed, exert the same effects as inclusions in the filler rod. Since the base metal may be heated more or less rapidly than the filler rod, the effects may be more or less pronounced. The gas content of mild steel determines the extent of boiling during oxyacetylene welding, according to Thum,[76] and accounts for cracking, according to a French writer.[77] The latter effect is hypothetical (see review of literature on The Effect of Hydrogen on the Welding of Steel). Kinzel[78] states that segregation of constituents in the mushy region during welding is not serious.

In fusion welding, the non-metallic inclusion streaks in base metal coagulate at the junction with weld metal, as Moore[79] and others have shown. If welding is done under favorable conditions, the non-metallic inclusions are absorbed by the slag, and the weld metal is relatively free from non-metallic inclusions, which occur as globules rather than as streaks. Miller,[64] however, believed that inclusions are to be found at the grain boundaries (no details) in weld metal. Early welders[80,81] (1922–24) stated that non-metallic inclusions of any sort increase the difficulty of welding. At the present time inclusions in base metal do not appear to be an important factor, but it is not clear whether sulphide inclusions are more troublesome than silicates, for example. Fusion welds in wrought iron usually contain less slag than base metal because the inclusions in base metal swim to the top of the weld metal. According to Egloff,[82] non-metallic inclusions in base metal make the arc unstable. The fillet weld impact test is recommended by Vogel and Hess[83] for detecting laminated base metal.

In forge and water gas welding, inclusions in base metal are damaging to the quality of the weld (tank plate) as Speller[84] pointed out. A number of investigators[85] have observed splits in segregated areas of water gas welded boiler plate. In flash welding mild steel 3/8 × 7 inches Torgler[120] traced pitted areas near the seam in chipped welds to lines of secondary piping and segregated impurities that opened up at the surface on account of upset.

An instance of seamy steel (0.17 C, 0.40 Mn, 0.03 Si) producing honeycombed pressure welds whereas non-seamy steel of the same analysis did not, is cited by Thum,[76] Nead and Kenyon.[86] Fuse-shaped pieces of both steels were enameled and heated by electric resistance until a slight sag indicated melting. Gas evolved from the seamy steel raised blisters in the enamel; there were no blisters in the enamel coating of the non-seamy steel. The inference is that inclusions gasify when the steel melts. The nature of the inclusions and the gas was not stated, nor was it clear whether the gas was evolved from the metal or the inclusions. Goldmann[87] believed it was iron oxide scale formed during welding that created enamel blisters owing to a reaction between scale and enamel. Slag inclusions were believed to exert a similar effect, and other factors also were mentioned. Water drawn by capillary action into a pipe cavity in a rolled section on cooling after hot bending was gasified during a subsequent welding operation, according to Rötscher,[124] and created a huge blister of 34 cu. in. capacity.

Wiss[88] observed that flame cut surfaces tend to be rough in segregated steel.

ARC STABILIZERS

Constituents often are added to coatings to stabilize the arc. Strictly speaking, the stability of an arc of given length is the ratio of the distance the arc can be lengthened before being extinguished, to the given arc length. Stability of a welding arc often is meant to include such additional factors as smoothness and sputter, and is affected by circuit characteristics, magnetic blow, globule transition and gas evolution in the weld puddle and electrode tip, as well as by the motion of the electrode. Of the influences that increase stability, the more important are increase in open-circuit voltage,

decrease in arc length and decrease in cathode and anode drops and in voltage gradient in the arc column.

The decrease in drops and gradient is affected by coatings in two ways. First, the coating may form a solid sleeve around the arc column in the vicinity of the electrode, which may reduce wandering of the arc on the tip of the electrode[14] or may offset dilution of the ionized arc column by surrounding atmosphere. Second, the coating may contain materials exhibiting a high thermionic emission which increases the number of ions or electrons[89] in the arc column at a given voltage. Arc stabilizers in the coating, therefore, decrease the arc voltage (decreased drops and gradient) for a given length and current. Non-stabilizers may be inert or may raise the arc voltage by diluting the arc column with un-ionized gas.

For example, Newell[89] states that hydrogen and hydrocarbon vapors increase the arc voltage, whereas oxides generally decrease it. Weber[122] found that the arc from a bare mild steel electrode was more stable in CO_2 than in air, whereas Alexander[122] found the reverse, the explanation offered by Alexander being that the dissociation of CO_2 in the arc core into CO and O_2 absorbs a large amount of energy.

Sodium and potassium compounds stabilize the arc, according to Leitner,[34,90] sodium compounds increasing the rate of melting of the electrode. In cored electrodes potassium compounds did not permit an arc to be held at so high welding speeds as sodium compounds. Calcium compounds and iron oxides steady the arc but do not promote high welding speeds. In cored electrodes the compound 2 $FeO.SiO_2$ was favorable for ionization, $FeO.SiO_2$ was not. Magnesium compounds did not decrease the welding speed, but were indifferent from the standpoint of stability. Titanium compounds enabled a longer arc to be held. Potassium oxalate was the stabilizer preferred by Henrion,[140] who stated that fluorspar—all fluorides for that matter—was a powerful non-stabilizer. Sodium titanate (presumably $Na_2Ti_3O_7$, decomposes to TiO_2 and melts at about 1125° C.) also deserved consideration as a stabilizer in Henrion's opinion. Bliss,[13,91] who points out that the maximum efficiency of an arc stabilizer increases as the particle size decreases but that there is difficulty in extruding material finer than 325 mesh, provides the following list of materials that have been used as arc stabilizers in coatings: ferrotitanium, titanium oxide (titanalba = pure white TiO_2, rutile = 94% TiO_2, precipitated (very finely divided) TiO_2 = 98.8 TiO_2, 0.03 Fe, 0.041 S), and cerium rare earth hydrates. Since Green[28] found that thorium oxide (ThO_2) reduces the arc voltage, it may be classed among arc stabilizers. Okada[92] stated that arc voltage of coated electrodes is raised by silica, clay, borax and sodium carbonate, but is lowered by lime and iron oxide. In manual welding with mild steel electrodes 0.20 inch diameter, Davidenko[137] found that basic coatings required 20 volts, acid and organic coatings 40 volts under the same conditions. As the arc voltage increased the rate of deposition and power factor of the transformer increased. In the absence of oxygen Suits[132] found it impossible to establish a steady state arc between clean iron electrodes in the 100 volt- 50 ampere range in vacuum. If the cathode was oxidized, a stable arc was obtained. The oxide supplied electrons more readily than clean iron. On the other hand, Newman[133] could strike an arc between iron electrodes in air at low pressures (0.1 mm. mercury) only if the surfaces of the electrode were not covered with iron oxide. Clean surfaces or surfaces coated with potash, soda, sodium chloride or sodium carbonate were suitable for arc formation (no details).

Arc stabilizers in Tichodeev's[93] tests were FeO, CaO, MnO and FeS. Non-stabilizers were found to be Al and Al_2O_3 (even as little as 0.01% in coating), SiO_2 (over 0.15 Si or over 0.40 SiO_2) and $FeSO_4$ derived from pickling. The hydrogen content of the pickled rod was not determined. Non-stabilizers decreased electron emission and the electric conductivity of the slag in Tichodeev's opinion. Shibata[94] found that the arc voltage of a bare electrode depends on the composition of the electrode. The arc voltage of coated electrodes depended only on the composition of the coating. Most of the materials studied by Shibata gave smaller arc voltage with straight polarity than with reversed. Anhydrous boric acid, anhydrous silica, and Al_2O_3 were exceptions. According to Okamoto,[95] a mild steel electrode coated with an acid slag (44.3 SiO_2, 20.9 Al_2O_3, 16.2 FeO, 9.4 Fe_2O_3, 5.0 MnO, 3.1 CaO, 1.9 MgO) required a higher arc voltage (30 volts) than the same core rod coated with a basic slag, using a.c. (60 cycles). Although one Russian welder[143] maintained that metals of the fourth series in Mendeleef's System were arc stabilizers, other investigators[144] found that Cs_2O, Rb_2O, K_2O, Na_2O, Li_2O, BaO, SrO and CaO stabilize the arc because they are easily ionized. FeO, MnO, NiO CuO, and TiO_2 are less readily ionized and exert little influence on stability. Al_2O_3, Cr_2O_3 and SiO_2 make the arc less stable. Excess of ionizing material in the coating is indicated by too large an aureole, decreased arc voltage, and decreased rate of deposition, It was believed that K_2O was the best stabilizer for a.c., Na_2O best for d.c.

Sull coatings stabilize the arc by reducing the resistance of the arc vapor, according to Rose.[40] Rust also stabilizes the arc, Brophy[63] and others found, and grease, soap and lime likewise stabilize the arc, according to Green.[96]

THE ARC ATMOSPHERE

The composition of the atmosphere in the arc column is important in understanding the metallurgical influences exerted upon the weld metal and in predicting the probable behavior of coating constituents. However, it is extraordinarily difficult to sample the atmosphere of a welding arc and to infer from the sample the composition prevailing at arc temperature. According to Holland,[97] the composition of the gas in weld metal made with bare electrodes in air is commonly 50–60% H_2, 33–39% CO, up to 8% N_2, 2–6% CO_2, up to 5% CH_4. No explanation was proposed for the origin of hydrogen in the bare electrode arc. Green[28] and others[98] believed that carbonyls were present in the welding arc, which is unlikely in view of the low decomposition temperature of carbonyls.

Since there is practically no information about the atmosphere in the arc column of bare electrodes, it is not surprising that the composition of the atmosphere in the arc column of covered electrodes is not known completely. Hoyt[99] and Durham and Austin[1] stated that the arc atmosphere with cellulose covered electrodes consists of CO, H_2 and H_2O. The volume of gas (reduced to 100° C., 760 mm. mercury) evolved from one centimeter (0.5 cu. cm.) of a cellulose coated electrode was found by Larson[100] to be 1.9 cu. cm. CO_2, 70.3 cu. cm. CO, 85.4 cu. cm. H_2, 9.3 cu. cm. H_2O. Doubtless CO_2 and H_2O were decomposed at arc temperature, and H_2 and O_2 may have undergone dissociation or excitation. The gas evolved by organic coated electrodes was said by Armstrong[101] to be more readily trapped in the deposit than the gas evolved by mineral coated electrodes.

VISCOSITY

Viscosity (internal friction; its reciprocal is called fluidity) is the force required to move one of two parallel plates of known area at unit velocity, the plates being separated by a unit thickness of the fluid under consideration. In welding the viscosities of both the metal and the slag are important and are independent. A slag of high viscosity is difficult to manipulate. On the other hand, too low viscosity is undesirable in vertical and overhead welding. Although slags of low viscosity increase the velocity of chemical reactions between slag and metal, it is doubtful in view of the great effect of temperature on slag viscosity, whether viscosity measured at a given temperature is a useful index to the rate at which the slag may react with metal and atmosphere. There is no relationship between melting point and viscosity.

In oxyacetylene welding, according to Kinzel,[102] the viscosity should be such that the molten pool is maintained in place by the flame. In Dobson's[103] opinion, one of the most important factors in the selection of an electrode is that the covering should melt and flow at the same temperature as the core, and the viscosity of the flux should be such that the flux is maintained around the molten pool. Slag with too low viscosity tends to flow ahead of the arc in fillet welding, according to Joublanc,[121] and so causes undercutting. If its viscosity is too high, the slag impedes uniform solidification and causes depressions and uneven surface. Shepherd and Moritz[10] stated that a slight increase in MnO content of the slag to lower the FeO content may so lower the slag viscosity that the slag become unmanageable and loss of manganese is increased. Oxyacetylene welding slags are made viscous by silica, Streb[66] found, which prevents the gas in the weld metal from rising through the slag. A porous weld is the result, unless welding is done extremely slowly.

There is no information available on the effect of coating and flux constituents on the viscosity of welding slags, except Zverev and Kaufman's[119] determination of the viscosity of 23 typical welding slags between 1050 and 1400° C., in which TiO_2 was found to decrease the viscosity of acid slags. The coatings contained 20–40 SiO_2, 0–30 TiO_2, 10–20 FeO, 6–30 MnO, 5–15 Al_2O_3. There was a large increase in viscosity as the silica content was increased from 34 to 40 SiO_2. Investigators, such as Notvest,[6] have relied upon viscosity determinations for steelmaking slags made at steelmaking temperatures. These determinations, besides being based on methods whose accuracy is not beyond question, have been made primarily on basic slags, which are rarely used for coatings. Laboratory tests (torsion viscometer method) showed no simple relationship between viscosity and the amount of a constituent added to a given slag. Whether or not exceptionally low viscosities coincide with compound formation is a debated problem. Foaming of steel furnace slags was observed only if the viscosity of the slag was within well-defined limits. A particularly effective agent for reducing the viscosity of acid slags was found to be strontianite ($SrCO_3$), which has not been found useful in electrode coatings.[129] Henrion[140] mentions that fluorspar is effective in increasing fluidity of welding slags.

SURFACE TENSION

Surface tension, the force required to break a unit film, has been considered sometimes an important factor in welding.[104] In forming the crater, states Slocum,[105] the surface tension at the cooler edge of the hot spot is stronger than at hotter regions and pulls the surface metal from the center toward the edge, the irregularity in the cooling action at the edge being the chief factor in creating the ribbed appearance of the weld. Surface tension must be important also in globule formation. Since manganese lowers the surface tension of steel, according to Notvest,[106] the rod for overhead welding should be low in manganese. Notvest did not refer to experiments on surface tension, nor is it clear what influence surface tension exerts in overhead welding. As a matter of fact, it seems reasonable that the lower the surface tension of the molten globule, the greater is its tendency to wet the steel. Joublanc[121] states that welding slags having a high surface tension form islands which result in noticeable depressions in the surface of the weld metal.

In Tichodeev's[93] opinion, FeO decreases the surface tension of weld metal and the size of the globules transported through the arc. Other Russian welders[144] believed TiO_2 had a similiar effect, which was beneficial for automatic arc welding. Doan[130] also referred to the fact that the mass of the globule in welding is directly proportional to its surface tension. By adding 0.1 to 1% antimony, a metal of low surface tension, as an electrode coating to mild steel weld metal he increased the number of globules per second from 20 to 48, as revealed by oscillograms. There was a corresponding increase in rate of metal deposition at constant current and arc length (voltage not stated) of 25 to 50%. The smaller the globule, the thinner is the heat insulating molten layer at the end of the electrode and the more rapid is the rate of melting of the electrode.

MISCELLANEOUS FLUXES

In the almost complete absence of systematic research on steel welding fluxes and in the presence of an overpowering assortment of proprietary compositions, little need be said of fluxes for gas and other types of welding. Giroux[107] determined the fluidity of mixtures of borax and boric acid, and Green[96] found that a copper coating on steel filler rods for oxyacetylene welding improved the flowing qualities but lime and iron oxide were disadvantageous. A paper string wound around the filler wire was recommended by Lincoln[108] for automatic carbon arc welding. Makarov[109] used a slag-forming flux consisting of 75 parts waterglass, 15 parts dehydrated borax, with additions of MnO and charcoal. The flux is used for low carbon steel 0.02–0.06 inch thick welded with carbon electrodes 0.24–0.47 inch diameter, 120–160 amp. Bliss[91] observed that small amounts of yttrium enhance the stability of a carbon arc.

For successful pressure (forge) welding Fry[110] emphasized that the alloying elements in the steel must form a fluid slag with the flux or vaporize. The following elements are undesirable for forge welding because their oxides have a high melting point: Si, Al, B, Cr and W, yet quartz sand has been used as a flux[111] in forge welding. Roeckner[111] also mentioned powdered glass, other silicates and borax as forge welding fluxes. Schäfer[112] added sodium or ammonium chloride, barium sulphate and potash to which a carbonaceous material may be added. A forge welding flux is described by Dufour[127] which carburized the surfaces to be welded, lowered the required welding temperature, and created a reducing atmosphere (no details). In 1917 burnt borax was used as a flux in Germany[113] for resistance butt welding. Resistance bar welds in rails were made with the aid of borax glass flux.[20]

The function of bronze-welding fluxes, Thomas[114] found, was to prevent oxidation of the steel. Calcium silicide, which decomposes to amorphous silicon at elevated temperatures, was a good deoxidizer, calcium silicate possessing valuable slagging properties. Aluminum and titanium were mentioned as being constituents of some brazing fluxes. Fluxes for use in dip brazing stainless steel turbine blades to mild steel retainers were studied by Turnbull.[115] Borax was found to be satisfactory, if temperature and blade spacing were correct. A flux consisting of 40% borax, 30% sodium carbonate, 30% potassium carbonate was found to have lower viscosity than borax. Aside from preheating the segments and protecting the brass, the sole function of the flux was to clean the surfaces to be brazed, borax having a high capacity for dissolving oxides and silicates.

Fluxes for silver soldering are listed by Leach[116] and for soft soldering by Eyles.[117] For galvanized iron HCl is used, for terne plate, $ZnCl_2$, for tinned steel resin or $ZnCl_2$. Organic phosphate fluxes are successful for soft soldering stainless steel, according to Willstrop, Sidery and Sutton,[118] but an aqueous solution of ammonium phosphate was unsuccessful in Arthur's[128] test with iron. Phthalimide was recommended by Suzuki and Oma[134] as a non-corrosive organic soldering flux.

ACKNOWLEDGMENTS

Contributions to the review have been received from:

H. Groebler, Finsterwalde, Germany.

E. Helin, Elektriska Svetsnings Aktb., Gothenburg, Sweden.

E. Henrion, Brussels, Belgium.

M. Okada, Osaka Imperial University, Japan.

BIBLIOGRAPHY

1. Durham, G. E., and Austin, J. B., *Ind. & Weld.*, **5** (6) 12–15, 22–28 (1933).
2. Strelow, W., *Z. VDI*, **79**, 1080–1084 (1935).
3. Ogden, H., *Trans. Faraday Soc.*, **20**, 159–162 (1924).
4. Hensel, F. R., and Larsen, E. I., *Trans. Amer. Soc. Steel Treating*, **19**, 639–664 (1932).
5. Lincoln, J. F., Iron & Steel Inst. Welding Symposium, **II**, 437–443 (1935).
6. Notvest, R., *Wire & Wire Prod.*, **11**, 527–551 (1936).
7. Green, J. B., *Weld. Engr.*, **7** (8) 37–38 (1922).
8. Hurme, E. A., *Iron Age*, **132**, 20–21, 55, Nov. 9, 1933.
9. Paterson, J. H., *Welding Ind.*, **3**, 152–154, Iron & Steel Inst. Welding Symposium, **II**, 453–462 (1935).
10. Shepherd, T. C. R. and Moritz, M. R., Iron & Steel Inst. Welding Symposium, **II**, 561–567 (1935).
11. Simon, W. H., The German Patent Situation of Arc Welding Electrodes (1937); The American Patent Position of Arc Welding, **I**, *Electrodes* (1939).
12. Mathias, D. L., *Foote Prints*, **9** (2) 1–12, Dec. 1936; American Welding Society Journal, **16** (2) 20–24 (1937).
13. Bliss, L. G., *Welding*, **5**, 203–206, 208, 242–243, 255 (1934); *Foote Prints*, **7** (1) 12–25, June 1934.
14. Goodger, A. H., *Weld. Engr.*, **13** (2) 39–43 (1928).
15. Sinfield, D. G., *Welding Jnl.* (London), **30**, 102–108 (1933).
16. Zeyen, K. L., *Kruppsche Monatsh.*, **12**, 121–139 (1931).
17. Baumgärtel, K., *Forscharb. Geb. Ingwes.*, **336**, 33 pp. (1930).
18. Adams, C. A., *Trans. A. S. M. E.*, **56**, 21 (1934).
19. Kinzel, A. B., American Welding Society Journal, **15** (11) 12–14 (1936).
20. Spraragen, W., American Welding Society Journal, **3** (4) 36–47 (1924); *Trans. Faraday Soc.*, **20**, 168–175 (1924).
21. Jeffries, Z., *Trans. A. I. M. E.*, **62**, 657–659 (1920).
22. Armstrong, P. A. E., *Weld. Engr.*, **2** (1) 42–45 (1917).
23. Sonderegger, A., *Bull. Assn. Suisse Electriciens*, **28** (1) 10–11 (1937).
24. Okada, M., American Welding Society Journal, **14** (6) 10–15 (1935).
25. Diegel, C., *Stahl u. Eisen*, **42**, 1309–1315 (1922).
26. Hobart, H. M., *Gen. Elec. Review*, **22**, 445–460 (1919); *Trans. A. I. M. E.*, **62**, 569–613 (1920).
27. *Electrician*, **118**, 291–292 (1937).
28. Green, J. B., *Weld. Engr.*, **12** (1) 29–31 (1927).
29. *Welding Jnl.* (London), **23**, 310, 316 (1926).
30. Gusseltchikov, M., *Vodnyi Transport* (11) 26–27 (1934).
31. Diatlov, V. I., and Kasimirov, O. A., *Acad. Sci. Kiev, Russia, Report on Effect of Frost on Welds*, 18 pp. (1937).
32. Jerochin, A. A., *Avtog. Delo*, **7** (12) 12–15 (1936).
33. Okamoto, T., Nishimura, H., and Katayama, H., *Yosetsu-Kyokwai-Shi*, **7**, 403–405 (1937); Abstract in *Metals & Alloys*, MA 98, R 9, Feb. 1938.
34. Leitner, F., *Elektroschwg.*, **5**, 121–127 (1934).
35. Neese, H., *Stahl u. Eisen*, **42**, 1001–1013 (1922).
36. Regè, F., *Metallurgia Italiana*, **27**, 493–503 (1935).
37. Inglis, N. P., and Andrews, W., *Novosti Tekniki Mashinostroenie*, (30) 12–13 (1935).
38. Alov, A. A., *Avtog. Delo*, **6** (2) 17–19 (1935).
39. Esslinger, F. J., *Foote Prints*, **10** (1) 13–18, June 1937.
40. Rose, G. S., *Iron & Steel Engr.*, **8**, 227–229 (1931).
41. Redline, R. H., *Welding*, **4**, 352–353 (1933).
42. Eisheuer, H., *TZ prakt. Metallb.*, **46**, 136–138 (1936).
43. Jackson, J. G., *Machinery Market*, **211** (1934).
44. Helin, E., and Svantesson, S., Iron & Steel Inst. Welding Symposium, **II**, 373–390 (1935).
45. Diatlov, V. I., *Acad. Sciences, Kiev, Russia, Report on a Characterization of Existing Electrode Coatings for Welding Plain Carbon Steel*, 64 pp. (1937).
46. Erokhin, A., *Metallurg*, **12**, 82–92, May 1937.
47. Miller, L., *Trans. Manchester Assn. Engrs.*, 81–111 (1931–32).
48. Osterle, J. F., *Weld. Engr.*, **17** (7) 28–29, 39 (1932).
49. Llewellyn, D., *Welding Ind.*, **1**, 281–284 (1933).
50. Moressée, G., *Rev. Univ. Mines*, 8th Series, **11**, 345–361 (1935).
51. Kusmak, E. M., and Abramova, E. V., *New Methods of Welding and the Strength of Welded Joints;* edited by G. A. Nikolaev, Techno-Scientific Press, Russia, 66–134 (1937).
52. Buschtedt, P., and Diatlov, V., *Acad. Sciences, Kiev, Russia, Report on Production of High Grade Automatic Arc Welds*, 1–57 (1937).
53. Brophy, G. R., American Welding Society Journal, **6** (9) 67–76 (1927).
54. Schneider, R., Proc. Twelfth Intnl. Cong. Acetylene, 1436–1443 (1936).
55. Okamoto, T., American Welding Society Journal, **10** (7) 33–37 (1931).
56. Booer, J. R., *Chem. Trade Jnl.*, **74**, 189–191 (1924).
57. Groebler, H., *Maschinenbau*, **15**, 677–680 (1936).
58. Swinden, T., Iron & Steel Inst. Welding Symposium, **II**, 593–601 (1935).
59. Polson, J. M., and Sinfield, D. G., *Welding Ind.*, **4**, 238–252 (1936).
60. Rapatz, F., *Stahl u. Eisen*, **51**, 245–253 (1931).
61. Diegel, C., *Forschungsarb. Geb. Ingwes.*, **246**, 44 pp. (1922).
62. Green, J. B., *Weld. Engr.*, **12** (2) 25–29 (1927).
63. Ronay, B., American Welding Society Journal, **12** (5) 22–23 (9) 21 (1933).
64. Miller, S. W., American Welding Society Journal, **4** (9) 29–45 (1925).
65. Granjon, R., *Welding Jnl.* (London), **24**, 146–147 (1927).
66. Streb, E., *Autog. Metallb.*, **25**, 52–61 (1932).
67. Fuchs, J., *Z. VDI*, **72**, 1151–1155 (1928).
68. Stursberg, E., *Autog. Metallb.*, **30**, 353–361, 369–380 (1937).
69. Green, J. B., *Weld. Engr.*, **11** (4) 21–24 (1926).
70. Roberts, P. L., *Weld. Engr.*, **11** (9) 25–27 (1926).
71. Horn, H. A., and Tewes, K., *Autog. Metallb.*, **25**, 88–92 (1932).
72. Taran, W. D., *Avtog. Delo*, **8** (9) 17–19 (1937).
73. Kessner, A., and Specht, H., *Arch. Eisenhüttenwes.*, **9**, 231–239 (1935/-36).
74. *Proc. Assn. Rwy. Elec. Engrs.*, **17**, 124–131 (1926).
75. Atkins, E. A., *Iron & Coal Trades Rev.*, **109**, 713 (1924).
76. Thum, E. E., American Welding Society Journal, **6** (5) 38–43 (1927).
77. *Rev. Soud. Autog.*, **25**, 2922–2924 (1933).
78. Kinzel, A. B., American Welding Society Journal, **15** (11) 12–14 (1936).
79. Moore, R. W., *Oil & Gas Jnl.*, **34**, 85–88, June 13, 1935.
80. Thompson, F. C., *Weld. Engr.*, **9** (7) 17–20 (1924).
81. Miller, S. W., *Weld. Engr.*, **7** (7) 19–23 (1922).
82. Egloff, G., Morrell, J. C., and Leonhardy, E. C., *Ind. & Engg. Chem.*, **24**, 1264–1275 (1932).
83. Hess, W. F., American Welding Society Journal, **15** (12) 18–19 (1936).
84. Speller, F. N., *Trans. A. S. M. E.*, **44**, 399–407 (1922).
85. Vigener, K., *Wärme*, **54**, 481–492 (1931).
86. Nead, J. H., and Kenyon, R. L., American Welding Society Journal, **1** (10) 9–25 (1922).
87. Goldmann, F., *Elektroschwg.*, **1**, 136–139 (1930).
88. Wiss, E., *Autog. Metallb.*, **22**, 46–53, 58–66, 74–79 (1929).
89. Newell, E. F., *Elec. Welding*, **6**, New Series (35) 161–164, June 1937.
90. Leitner, F., Iron & Steel Inst. Welding Symposium, **II**, 427–435 (1935).
91. Bliss, L. G., *Foote Prints*, **8** (1) 10–15, June 1935.
92. Okada, M., *Jnl. Soc. Mech. Engrs.*, Japan, **37**, 705–710 (1934).
93. Tichodeev, G. M., American Welding Society Journal, **15** (2) 13–17 (1936).
94. Shibata, H., *Trans. Soc. Mech. Engrs.* Japan, **1**, 247–254, S72–S73 (1935); *Bull. Japan Dept. Railways*, **22** (44) 17 pp., Dec. 25, 1934.
95. Okamoto, T., *Proc. World Engg. Cong. Tokio*, **22**, 255–278 (1929).
96. Green, J. B., *Weld. Engr.*, **11** (6) 38–40 (1926).
97. Holland, R. A., *Mech. & Weld. Engr.*, **6**, 201–207 (1932).
98. *Chemider Ztg.*, **58**, 18 (1934).
99. Hoyt, S. L., *Trans. Amer. Soc. Metals*, **23**, 61–84 (1935).
100. Larson, L. J., American Welding Society Journal, **15** (10) 14–20 (1936).
101. Armstrong, T. N., *Trans. Amer. Soc. Metals*, **23**, 90–91 (1935).
102. Kinzel, A. B., Iron & Steel Inst. Welding Symposium, **II**, 419–425 (1935).
103. Dobson, N., *Sheet Metal Inds.*, **11**, 566 (1937).
104. Darling, C. R., *Engineering*, **119**, 112 (1925).
105. Slocum, A. W., *Trans. Electrochem. Soc.*, **39**, 282–283 (1921); Booer, J. H., *Engineering*, **117**, 221 (1924).
106. Notvest, R., *Weld. Engr.*, **20** (6) 32–35 (1935).
107. Giroux, F. J., *Welding*, **3**, 161–162 (1932).
108. Lincoln, J. C., *S. A. E. Trans.*, **27**, 148 (1932).
109. Makarov, G., *Avtog. Delo*, **6** (3) 8 (1935).
110. Fry, A., *Kruppsche Monatsh.*, **12**, 201–214 (1931).
111. Roeckner, M., *Stahl u. Eisen als Werkstoff*, **11**, 75–79 (1928).
112. Schäfer, *Die Konstruktionsstähle*, Springer, Berlin, 125–129 (1923).
113. *Stahl u. Eisen*, **37**, 384 (1917).
114. Thomas, D. J., *Mech. World & Engg. Record*, **92**, 430 (1932).
115. Turnbull, J. M. G., *Engineer*, **162**, 622–625, 648–650 (1936).
116. Leach, R. H., American Welding Society Journal, **13** (12) 23–27 (1934).
117. Eyles, A., *Metal Ind.* (London), **40**, 3–6 (1932).
118. Willstrop, J. W., Sidery, A. J., and Sutton, H., *Jnl. Inst. Metals*, **59**, 53–58 (1936).
119. Zverev, L. V., and Kaufman, D. L., *Metallurg*, **12** (5) 76–81 (1937).
120. Torgler, E. R., American Welding Society Journal, **10** (9) 29–36 (1931).
121. Doan, G. E., American Welding Society Journal, **12** (8) 33 (1933).
122. Weber, L. J., *Trans. Amer. Soc. Steel Treating*, **11**, 425–440 (1927); discussion by P. P. Alexander, *Ibid.*, 441–446.
123. Rosenthal, D., and Mathieu, M., *Comptes Rend. Acad. Franc. Paris*, **194**, 1244–1245 (1932).
124. Rötscher, F., *Stahl u. Eisen*, **51**, 521 (1931).

125. *Mech. World & Engg. Record*, **100**, 109–110 (1936).
126. Davy, C. H., *Trans. Instn. Chemical Engrs.*, **13**, 131–151 (1935).
127. Dufour, P., *Chimie et Ind.*, **19**, Special Issue, 4 bis, 343, April 1928.
128. Arthur, W., *Trans. Amer. Inst. Metals*, **7**, 317–321 (1913).
129. Garriott, F. E., AMERICAN WELDING SOCIETY JOURNAL, **17** (3) 12–16, March 1938.
130. Doan, G. E., AMERICAN WELDING SOCIETY JOURNAL, **17** (1) Research Suppl., 15–19, Jan. 1938.
131. Joublanc, J. C., *Metal Working Press*, **2** (5) 6–7, May 1938.
132. Suits, C. G., AMERICAN WELDING SOCIETY JOURNAL, **17** (10) Research Suppl., 35–38, Oct. 1938.
133. Newman, F. H., *Phil. Mag., Ser. 7*, **2**, 796–799 (1926); 26 (173) 187–188 (1938).
134. Suzuki, T. and Oma, K., *Bull. Inst. Phys. Chem. Research* Japan, **11** (9) 1060–1061 (1932).
135. Helin, E., *Blad för Bergshandteringens Vänner*, **22**, 307–314 (1936).
136. Ônisi, Y., *Jnl. Japanese Welding Society*, **8**, 472–480 (1938).
137. Davidenko, I. D., *Avtog. Delo*, **9** (1) 6–9 (1938).
138. Yukimura, Y., *Jnl. Japanese Welding Society*, **8**, 5–23 (1938).
139. Keidansky, B., *The Effects of Weld Coatings and Their Tests;* Thesis (B. Met. E) Dept. Metallurgy, Ohio State Univ. (TN, 1937, K27) 26 pp.
140. Henrion, E., *Eclairage & Force Motrice*, **24**, 495–503 (1937).
141. Shakanovitz, V. Y., *Avtog. Delo*, **9**, (6) 2–8 (1938).
142. Sekiguchi, H., *Journal Japanese Welding Society*, **8**, 411–440 (1938).
143. Russanevitz, N. F., *Avtog. Delo*, **9**, (4) 27–29 (1938).
144. Buschtedt, P. P., Diatlov, V. I., and Frumin, I. I., *Avtog. Delo*, **9**, (4) 30–32 (1938).

SUGGESTED RESEARCH PROBLEMS

1. There are many distinct types of successful covered electrodes. The basis for assigning an electrode to a given type may be composition or welding characteristics (position or type of current). Stricter classification of existing covered electrodes should be based on the A. S. T. M. usability tests or on such characteristics as melting rate and penetration.

2. If the same coating is used on electrodes of different diameters implying different power conditions, the thickness of the coating must be adjusted accordingly. How do the characteristics of a successful covered electrode vary with the thickness of the coating? By characteristics is meant: (1) Recovery of carbon and alloying elements, (2) Pick up of foreign elements, nitrogen and oxygen, (3) Porosity, cracks and inclusions in weld metal, (4) Melting rate of electrode, (5) Penetration, (6) Shape of bead, (7) Ease of removal of slag, (8) Arc voltage and atmosphere, (9) Globule size.

3. To what extent does the flexibility of successful covered electrodes vary? In other words, to what extent can optimum welding conditions (position, welding speed, arc length, current, type of current) be altered without injuring mechanical properties beyond a specified amount?

4. In what form is slag and metal carried across the arc gap in welding with inorganic covered electrodes?

5. The composition of welding slags has not been studied comprehensively. A mild steel electrode should be covered with different thicknesses of elementary coating materials and deposited under the same sets of conditions, so far as possible. For example, a waterglass coating might be deposited at several currents. The slag from each bead should be analyzed chemically and petrographically. The weld metal also should be examined. The quantities of iron oxide in the slag under different conditions of coating thickness, rate of forward motion of electrode and current might provide some insight into the speed of reactions during arc welding. A similar study might be made for stainless steels and for oxyacetylene welding. A different initial flux would be needed for oxyacetylene welding, possibly borax.

THE EFFECT OF NORMALIZING FINE GRAINED Low-Alloy Steel (St 52) Before Welding*

By ROLAND WASMUHT

SUMMARY

THE results of bend tests on specimens with a bead of weld metal deposited on one surface, called "bead bend test," on a steel containing 0.18 C, 0.4 Si, 1.0 Mn, 0.3 Cr, 0.5 Cu, 0.79 to 2.0 inches thick showed that:

1. Normalizing the fine grained steel before welding considerably improves the results even without preheating and stress relieving.
2. Cracking in the bead bend test depends to a great extent on the location of the bead; higher bend angles are attained if the bead is deposited on the edges or if a special profile is used.
3. Brittle fracture in the bead bend test is avoided by using a normalized, fine grained steel.
4. Normalizing is particularly effective if the plate is thicker than about 1.18 inches.

The results agree with experience that normalized low-alloy steel over 1.18 inches thick can be fabricated into heavy welded structures without difficulty. Normalizing also improves the mechanical properties of the steel.

THE UNWELDED STEEL

The notch impact value at +50 to −20° C. of specimens cut from the center of rolled sections of the low alloy steel 2 inches thick, Fig. 1, was vastly improved by normalizing and was higher for steel that had been specially refined than for ordinary steel. The special refining treatment to which the melt was subjected resulted in a fine grained steel as revealed by the McQuaid-Ehn test and removed the sensitivity of the normalized steel to cold working. The special refining treatment consisted merely of careful control of refining and pouring conditions. Aside from its effect on notch impact value, normalizing raised the yield ratio about 10% and slightly increased the ductility in tensile and bend tests.

BEAD BEND TESTS

The bead bend tests were performed according to the method described by Bierett.[1] The results on specimens 1.18 and 2.0 inches thick, Fig. 2, showed that as-rolled material was prone to cracking suddenly in a brittle fashion, especially without preheating. The normalized steel, on the other hand, generally withstood a higher bend angle than the as-rolled steel before the appearance of the first crack (in the weld metal), and withstood considerable additional bending afterward.

The shape of the specimen was found to have considerable effect on the bead bend test, Fig. 3. The milling of grooves in the specimen before or after depositing the bead increased the bend angle by lowering the shrinkage

* Extended abstract of "Einfluss des Normalglühens auf Festigkeitseigenschaften und Schweissverhalten von Baustahl St 52" published in *Stahl u. Eisen*, **59**, (8) 209-212 Feb. 23, 1939.

[1] Bierett, G., AMERICAN WELDING SOCIETY JOURNAL, **18** (1) Suppl., 17-21, Jan. 1939.

stresses. Hence, if welding is done near the edges of a plate the shrinkage stresses will be low and the bend angle high. A nose-profile flange, specimen 9, yielded high bend angles in the bead bend test, showing that the thin projections serve to equalize the shrinkage stresses. No cracks have been observed in welded, nose-profile flanges of any thickness that are in service.

MINIMUM THICKNESS REQUIRING NORMALIZING

The notch impact value at 20° C. of specimens cut from the center of as-rolled sections of the specially refined low-alloy steel was 16 mkg./cm.² for sections up to 1.18 inches thick, but was less than 10 mkg./cm.² for thicker sections. At −20° C. the notch impact value was only 3 mkg./cm.² for all sections thicker than 0.79 inch. Normalizing raised the notch impact value of all sections to 15–19 mkg./cm². Bead bend tests of the specially refined steel, Fig. 4, led to similar results.

The low-alloy steel refined in the ordinary way had coarse grain structure (A.S.T.M. Grain Size 1 to 3) whereas the specially refined steel was fine grained (A.S.T.M. Grain Size 5 to 7). The steel that was not specially refined had coarse structure and poor bead bend ductility whether or not normalized, Table 1. All bead bend fractures were of brittle type. The specially refined, fine grained steel in the as-rolled condition also fractured in a brittle fashion in the bead bend test at small bend angles. After being normalized, all specimens of the fine grained steel yielded over 20° bend angle before developing a crack in the bead bend test, and the cracks were not of the brittle type. In other words, the specimen continued to bend after the first crack appeared.

The conclusion to be drawn is that a normalized, fine grained steel is essential for the successful welding of low-alloy steel over 1.18 inch thick containing 0.18 C, 0.4 Si, 1.0 Mn, 0.3 Cr, 0.5 Cu.

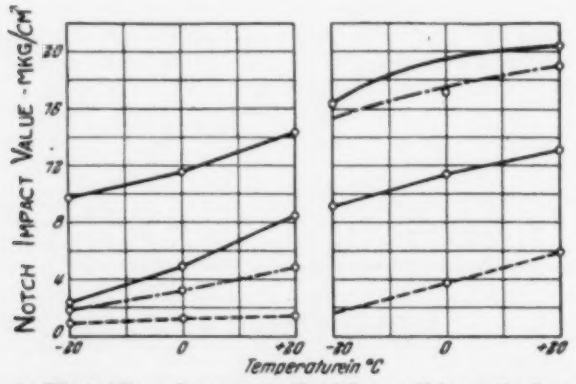


Fig. 1—Effect of Normalizing on the Notch Impact Value in the Center of a Section 2 Inches Thick of Low-Alloy Structural Steel St 52 Containing 0.18 C, 0.4 Si, 1.0 Mn, 0.3 Cr, 0.5 Cu. DVMR Notch Impact Specimens Were Used, 10 x 10 x 55 Mm. with Notch 3 Mm. Deep, 2 Mm. Diameter

normalized — normalized and cold worked 10%
as-rolled — as-rolled and cold worked 10%
Left-hand graph—refined in the ordinary way
Right-hand graph—special refining treatment

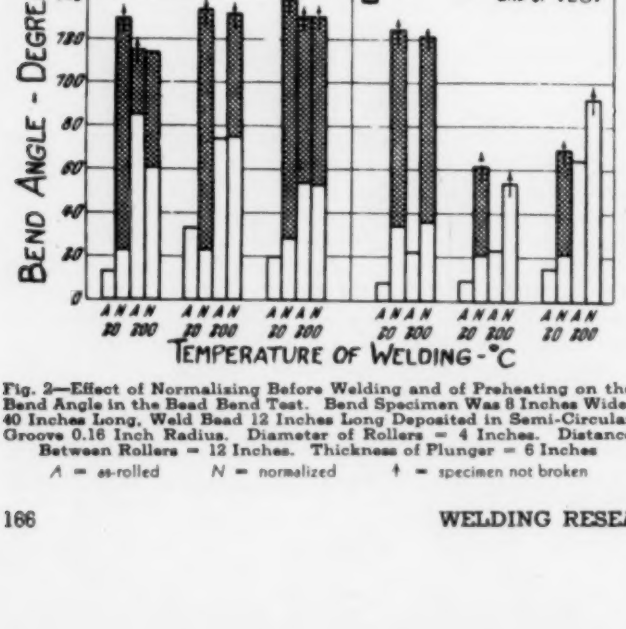

1.18 IN. THICK
2.0 IN. THICK
BEND ANGLE - FIRST CRACK
" " - END OF TEST
BEND ANGLE - DEGREES
80
60
40
20
0
A N A N A N A N A N A N
20 200 20 200 20 200 20 200 20 200 20 200
TEMPERATURE OF WELDING - °C

Fig. 2—Effect of Normalizing Before Welding and of Preheating on the Bend Angle in the Bead Bend Test. Bend Specimen Was 8 Inches Wide, 40 Inches Long, Weld Bead 12 Inches Long Deposited in Semi-Circular Groove 0.16 Inch Radius. Diameter of Rollers = 4 Inches. Distance Between Rollers = 12 Inches. Thickness of Plunger = 6 Inches

A = as-rolled *N* = normalized † = specimen not broken

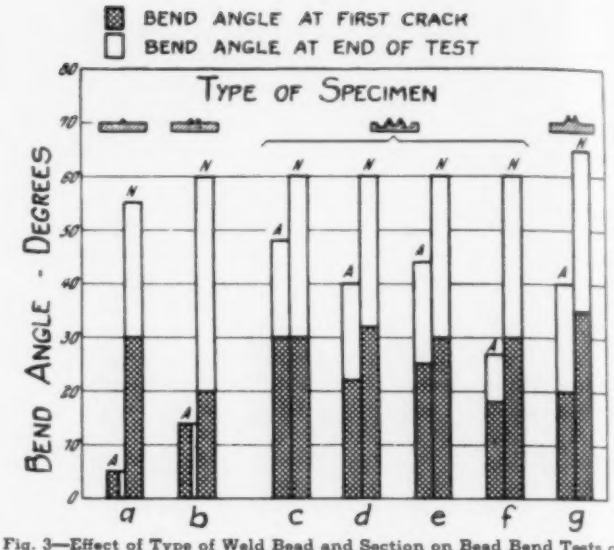


Fig. 3—Effect of Type of Weld Bead and Section on Bead Bend Tests on Low-Alloy Steel St 52, 2 Inches Thick

A = as-rolled *N* = normalized

Specimen c—Welding was done after the middle groove 0.24 inch deep had been planed.
Specimen d—Welding was done before the middle groove 0.24 inch deep had been planed.
Specimen e—Welding was done after the middle groove 0.47 inch deep had been planed.
Specimen f—Welding was done before the middle groove 0.47 inch deep had been planed.

Table 1—Relation Between Grain Size, Heat Treatment, and Bead Bend Ductility of Low-Alloy Steel St 52

Thickness, Inch	Type of Refining	Treatment	Total Number of Specimens	Number of Specimens Broken	Number of Specimens Unbroken	McQuaid-Ehn Grain Size	Bend Angle at First Crack, Degrees
1.58	ordinary	as-rolled	9	9	0	1 to 3	14
1.58	"	normalized	9	9	0	1 to 3	12
1.58	special	as-rolled	9	6	3	5 to 7	10
1.58	"	normalized	9	0	9	5 to 7	22
2.0	ordinary	as-rolled	9	9	0	1 to 2	15
2.0	"	normalized	9	9	0	1 to 2	11
2.0	special	as-rolled	9	9	0	5	11
2.0	"	normalized	9	0	9	5	26

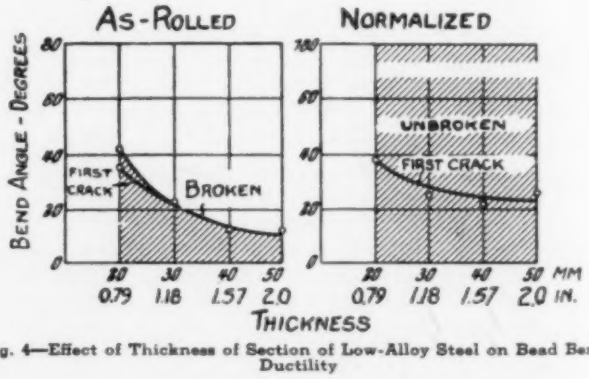


Fig. 4—Effect of Thickness of Section of Low-Alloy Steel on Bead Bend Ductility

AN INVESTIGATION OF Unsymmetrical Loading on Lightweight Steel Studs*

By HERBERT L. LYELL

INTRODUCTION

THE use of lightweight welded steel frames in building construction has now reached the point where it is becoming generally accepted by the building industry.

The acceptance in October 1937 of tentative specifications for this type of construction by the Pacific Coast Building Officials Conference should give new impetus to the use of lightweight welded steel frames on the Pacific Coast.

The increase in the use of lightweight steel members in the past few years and the evidence that this increase will continue has made necessary the determination of more exact knowledge concerning the strength of the members and the manner in which they act when under load.

The open web construction of the members and the rigid welded joints between the members precludes the use of the standard column formulas in determining the strength of the studs in a welded steel frame.

The object of this investigation is to determine the relative strengths in compression of various types of steel studs used in lightweight welded building frames.

GENERAL TEST PROCEDURE

In designing the general type of frame for these tests the attempt was made to approach actual building conditions as nearly as possible. The frame was designed to represent the conditions existing in the outside wall of a building where the joists are not carried beyond the outer frame of the studs.

A $^1/_4$-inch plate was welded in the bottom of the studs to act as a sill. Across the face of the studs a $2^1/_2$ x $2^1/_2$ x $^1/_4$-inch angle was welded so as to give the studs a nine foot clear height to the bottom of the joists. The angle welded across the studs in this manner for the joists to rest upon allows the use of the studs in two-story lengths if desired. Because of the large concentrated load that was required on the loading beams, open web lightweight joists could not be used. Channel sections were used for loading beams in all of the frames. The channels were welded to the supporting angle and to both flanges of the studs, thus making a very rigid joint between the members. The channels were tied together to prevent rolling by plates welded at the quarter and midpoints of the eight-foot span. Lateral support was given the studs by welding 1 x 1 x $^1/_8$-inch angles to both flanges of the studs on three-foot centers.

The frame was placed in the testing machine with the sill plate clamped to the weighing table. The free end of the loading beam was supported by a 6 x 16-inch timber column which was also bolted, by means of clip angles, to the weighing table of the machine. The reaction at the free end of the loading beams was carried to the timber column through a four-inch roller thus assuring a vertical reaction at this point. The lateral bracing angles were supported by a timber frame. Two 2 x 6-inch planks were bolted securely to the studs so that they would be about two inches from the floor and would not affect the reactions on the weighing table. On each side of the stud 2 feet 6 inches from the center of the outside studs 4 x 6-inch timbers were bolted to the horizontal 2 x 6-inch planks at the bottom and to the $2^1/_2$ x $2^1/_2$ x $^1/_4$-inch angle at the top. The 1 x 1 x $^1/_8$-inch lateral bracing angles were then bolted to the 4 x 6-inch timbers. This made a very rigid support for the studs in the lateral direction. Guy wires were run from the top of each 4 x 6-inch to anchors in the adjacent walls to keep the frame from rolling sideways.

The load was applied to the loading beams through a roller at a distance such that the maximum allowable bending moment was produced in the channels. The application of the load in this manner makes possible a certain amount of eccentricity with a consequent bending moment being produced in the stud.

Each specimen was tested through several preliminary runs in which the stresses were kept below the elastic limit to make certain that all of the gages were in working order and that the studs were carrying approximately equal loads. In the final test the studs were tested to destruction with gage readings being taken as long as it was safe to leave the gages on the specimens.

After the tests two tensile test specimens were cut from each type of stud and tested to determine the physical properties of the steel in the studs used in the tests.

TEST NO. 1

The studs used in the first test were manufactured by the Soule' Steel Company from 6-inch J & L Junior I beams.

The frame used in this test consisted of three studs and three channel beams. The details of the frame are shown in Fig. 1. Strain gages were attached to the inside and outside flanges of the outside studs, measuring the stress 45 inches below the top of the studs (Figs. 2, 3 and 4). In the discussion the studs have been designated as Right stud and Left stud according to their position on the drawing. Gages were also placed so as to measure the deflection of the loading beams at each end and under the load. Gages were arranged to measure the movement in a longitudinal direction at the centers of the three studs and at the top of the center stud.

The load was applied 20 inches from the face of the studs. This made the direct load on the studs equal to 79.8% of the total load on the frame, while the bending moment in the studs in inch-pounds equaled 65% of the total load on the frame in pounds.

* Abstract of a thesis supervised by Prof. J. B. Wells, submitted to the Department of Civil Engineering of Stanford University in partial fulfilment of the requirements for the Degree of Engineer, December 1937. Contribution to Fundamental Research Division.

Stresses computed from the strain gage readings in the preliminary tests indicated that the load was fairly evenly distributed between the three studs. The preliminary tests also showed that the proportion of the load carried by the inside flanges of the studs increased as the load increased.

The specimen was tested to destruction July 15, 1937. The maximum load on the frame at failure was 118,200 lb. This made a direct load on the studs of 94,320 lb. plus a bending moment of 76,830 inch-pounds. The resulting direct load on a single stud was 31,440 lb. while the bending moment in a single stud was 25,610 inch-pounds. The maximum stress in the stud at failure caused by these loads was 41,700 lb. sq. in. The curves for the deformation of the inside flange of the Right stud show a definite elastic limit at a measured stress of 30,450 lb. sq. in. and a computed stress of 30,000 lb. sq. in. at the point where the gages were attached. This is lower than the elastic limit of the material as obtained from the tensile specimens. The tensile tests showed an elastic limit of 36,000 lb. sq. in. and a yield point of 42,000 lb. sq. in. Although the elastic limit appears to be passed in the stud before the elastic limit of the material is reached the stud failed at a stress practically equal to the yield point stress as obtained in the tensile test. The Left stud collapsed suddenly without any preliminary yielding. This lack of similarity in the stresses after the Right stud began yielding was caused by the rolling of the loading beams. The left channel rolled to the left as the load was increased thus relieving the Left stud of some of the load and throwing a larger proportion on the Right stud. With the collapse of the Right stud the load was thrown on the two remaining studs causing their sudden failure without gradual yielding. The photographs of the studs after failure, Figs. 5, 6 and 7 show how the left channel rolled.

The effect of the eccentricity of the load due to the fact that the channels are welded onto the sides of the stud is cut down somewhat by the use of the angle, upon which the channels are resting, that is welded to the inside flange of the stud. Just how much effect the lateral eccentricity has upon the stresses in the studs is indeterminate. It does, however, affect the manner in which the studs fail. The studs failed in lateral buckling as was expected from the difference in the L/r about the two axes. The direction of the buckling was determined by the eccentricity of the loading beams. Examination of the photographs, Figs. 5, 6 and 7 show how the studs buckled away from the beams.

The value of the 1 x 1 x $^1/_8$-inch angles as lateral supports is also shown by the photographs taken after failure. The buckling of the angles show that they were acting as columns in preventing the buckling of the studs. Their effectiveness as lateral supports is more clearly shown by the fact that practically all of the failure has taken place in the top third of the studs. Very little buckling took place below the top lateral supports and none at all is apparent in the bottom third of the studs.

The L/r of these studs between girts was 89.3. The safe axial load as given by the A.I.S.C. column formula is $13{,}000 \times 1.015 = 13{,}200$ lb. For a direct load of 13,200 lb. the stress in the inside flange of the Right stud was 15,000 lb./sq. in. while the stress in the outside flange was 13,000 lb./sq. in. These stresses are well below the yield point of the material. The direct load at failure was 2.36 times the safe load as given by the A.I.S.C. formula. The bending moment in the stud at failure caused one-third

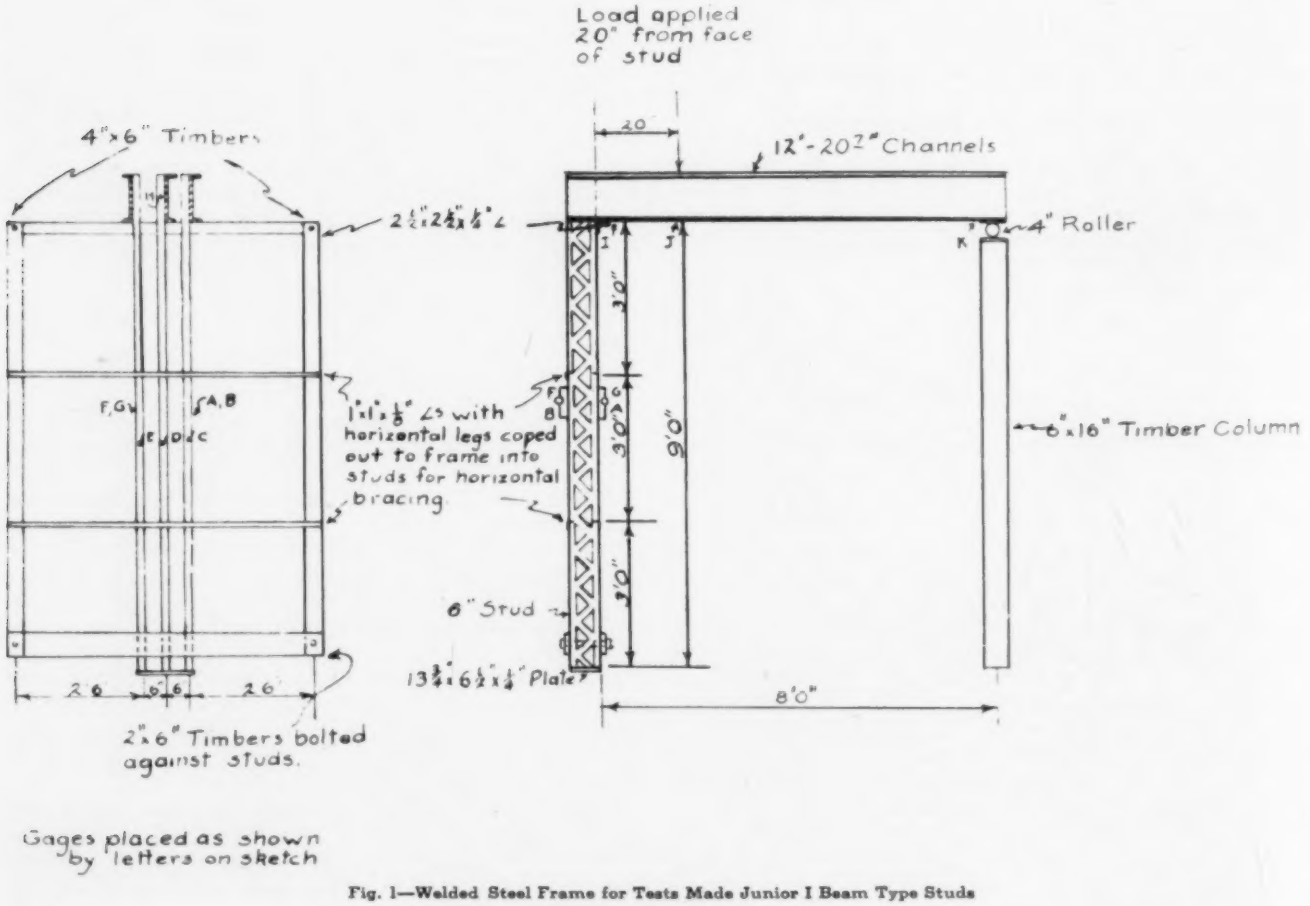
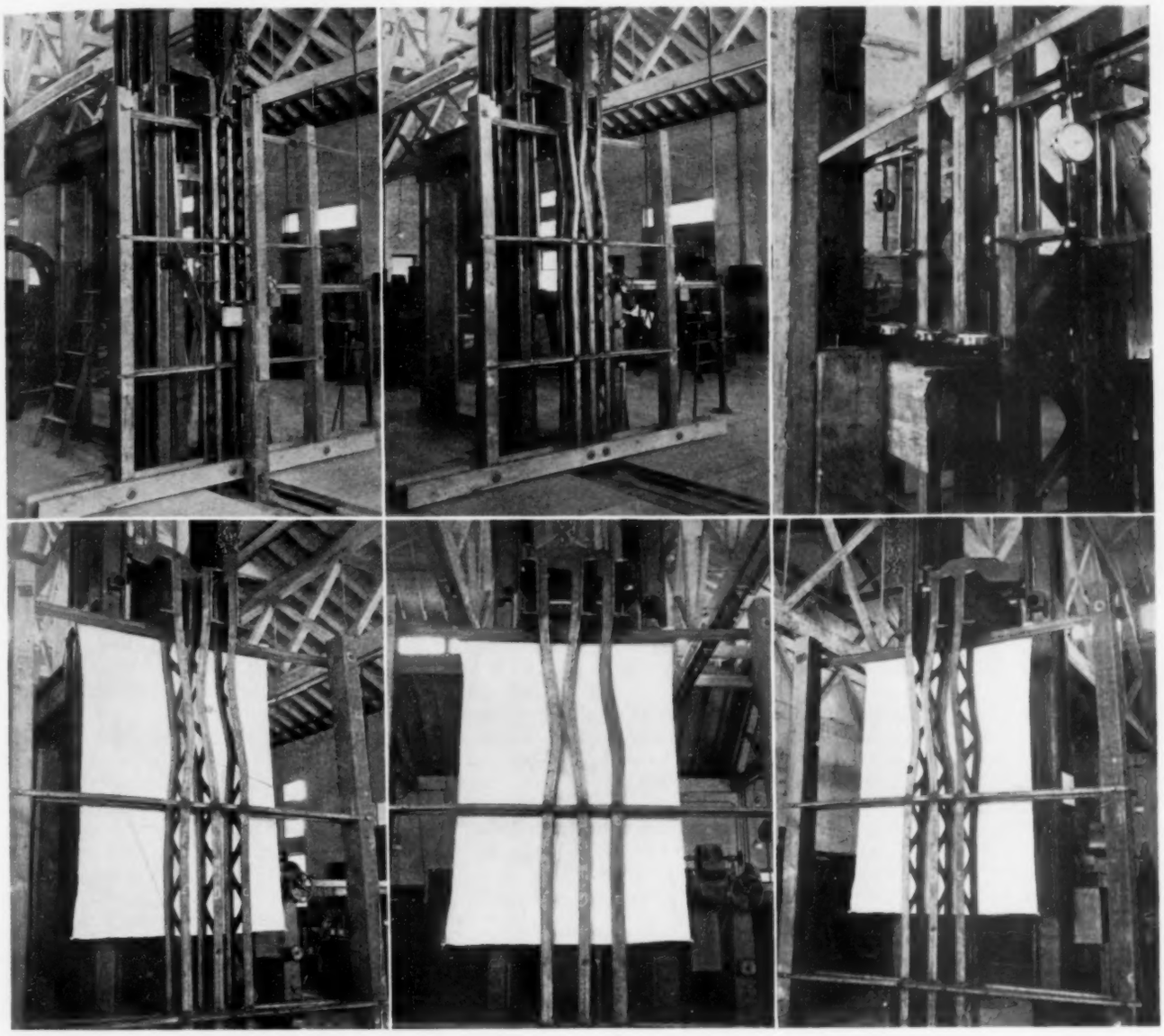


Fig. 1—Welded Steel Frame for Tests Made Junior I Beam Type Studs

Figs. 2 and 3—Junior I Beam Type Studs—Arrangement of Frame in Testing Machine

Fig. 4—Location of Strain Gages on Stud Flanges



Figs. 5, 6 and 7—Junior I Beam Type Studs After Testing

of the stress in the stud. It is obvious that the rigid connection causes considerable bending moment. However, within the working range of the studs as they were used in this test the bending moment does not cause excessive stresses.

TEST NO. 2

The studs used in the second test were Bethlehem lightweight steel studs manufactured by the Bethlehem Steel Company, Fig. 10. The frame for this test consisted of two studs and two beams. Gages were attached to the inside and outside flanges of the studs at the center of the studs. Gages were also attached to the flanges at the top of the Right stud. These gages measured the stress at a point 8 inches below the top of the studs. The deflection of the beams at each end and under the load point was also measured by means of gages. No gages were placed at the back of the studs as was done in the first test because no data of any real value was obtained from them.

The specimen was tested to destruction August 11, 1937. The load was applied at the middle of the span, 48 inches from the face of the studs. The maximum load on the frame at failure was 47,160 lb. This made a direct load on each stud of 12,000 lb. plus a bending moment of 22,640 inch-pounds. The resulting computed stress in the stud was 39,620 lb./sq. in.

The studs were clamped to the table of the testing machine by two bolts through the bottom plate. This imparts a certain amount of rigidity to the joint. After the frame was set up in the testing machine it was rocked back and forth by hand to determine how rigid the bottom joint was. Even though the plate was clamped to the table it rocked slightly back and forth. Besides this rocking there was some bending taking place in the plate itself. The sill plate that was used on this frame was $^1/_2$ inch instead of $^1/_4$ inch, consequently, the joint was more rigid than that in the previous test. However, the joint still seemed to approach more closely to that of a hinged end rather than a fixed end. The true end condi-

tion of the studs is probably between a third and a half of the way between a hinged end and a fixed end.

Calculations using the hinged end conditions give a value of the direct load on the studs in pounds as 50.9% of the total load on the frame and the bending moment in the studs in inch-pounds at 96% of the total load on the frame in pounds. Corresponding figures for the frame using fixed end conditions are 51.25% for the direct load and 120% for the bending moment. These values give a value for the stress in the stud at failure of 43,800 lb./sq. in. Considering the fact that the end conditions approach that of a fixed end and that the difference in the two conditions is not very great it appears that the end conditions can be assumed as a hinged end without too great an error.

For a load of 25,000 lb. on the frame the computed stress in the studs at a point eight inches below the top of the studs is 20,380 lb./sq. in. The stress at this point obtained from the gage readings is 17,800 lb./sq. in. in the inside flange of the Right stud. At the center of the stud the computed stress for a load of 25,000 lb. on the frame is 16,760 lb./sq. in., while the stress computed from the strain gage reading in the inside flange of the Right stud is 14,680 lb./sq. in. The stress in the stud as determined from the strain gage readings is about 2000 lb./sq. in. less than the computed stress as long as the stresses are kept below the elastic limit. The deformation at the top of the stud is greater than that at the center and as the load is increased this difference becomes larger. Failure occurred before the stresses in the flanges at the center of the studs had reached the elastic limit.

The failure of the studs was very sudden. There was no gradual movement visible in any part of the stud before complete failure occurred. Failure was so rapid that it was impossible to discern just where the first failure took place.

Examination of the studs after failure led to the conclusion that the initial failure took place in the web. The web members evidently buckled first causing the failure of the flanges. The photographs Figs. 8 and 9 show how the web members buckled and twisted.

The L/r of the web member is shown to be 154.4 in the lateral direction: i.e., about the 2–2 axis. This is assuming that the center is not supported laterally. As the only support that the center has is the tension of the cross web members this appears to be a logical assumption. The total length of the member would then be 11.12 inches. Since the ends of the members are rigidly fixed, half of this length is used as the effective length thus securing the L/r of 154.4. The manner in which the web buckled supports the theory of no lateral support at the center of the members. The maximum buckling of the web took place at the center. Near each end there was a point of inflection due to the rigidity of the ends.

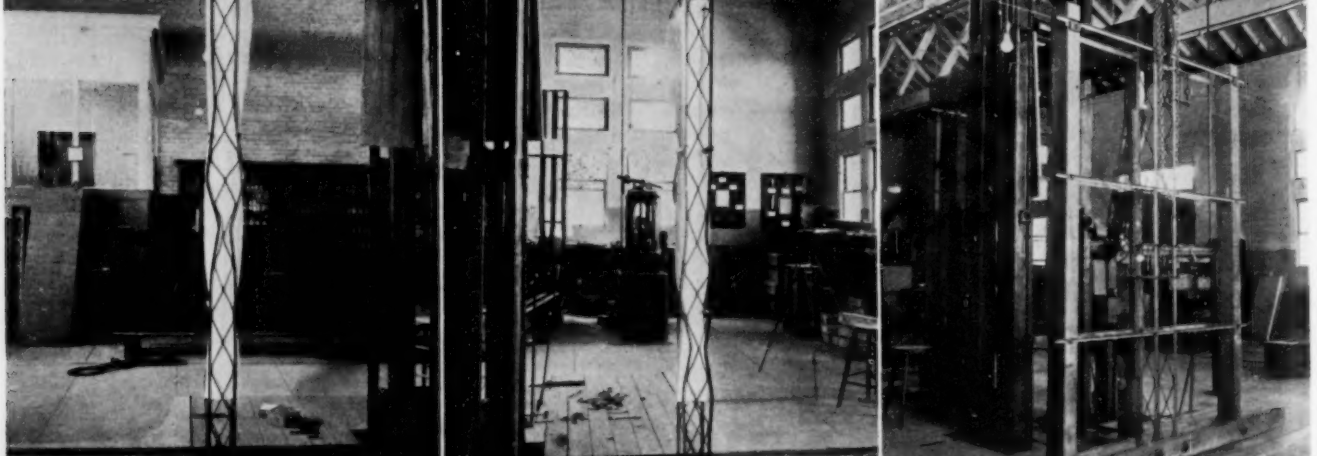
The L/r of the flanges between supports is 96.2 which is smaller than the L/r of the entire stud. Considering the properties of the component parts of the stud the web seems to be the most logical place for the failure to occur. The appearance of the studs after failure supports this theory.

The eccentricity of the connection of the channels to the studs had no apparent effect in the failure of these studs. There was no lateral movement of the stud flanges at all. The photographs, Figs. 8 and 9, taken after failure showed the lack of any lateral movement except in the web members.

The fact that the stress in the stud was larger at the top due to the bending moment apparently had no effect upon the type of failure. There was no failure apparent in the top third of the Left stud while the bottom third as well as the center third collapsed both in the web members and in the flanges. The Right stud acted just the opposite, all of the failure being confined to the upper and middle thirds, while there was no sign of failure in the bottom third.

The L/r between lateral supports of the studs as they were used in this test was 117.6. The safe load as given by the A.I.S.C. formula is $0.5094 \times 10{,}200 = 5200$ lb. The direct load at failure was 2.38 times the safe load from the A.I.S.C. formula. Besides this direct load the stud was carrying a moment of 22,640 inch-pounds. This moment caused 40.5% of the stress in the stud at failure.

More deflection of the loading beams and hence more bending moment in the studs was obtained in this test than was obtained in the first test.



Figs. 8 and 9—Bethlehem Studs After Testing

Fig. 10—Arrangement of Frame in Testing Machine

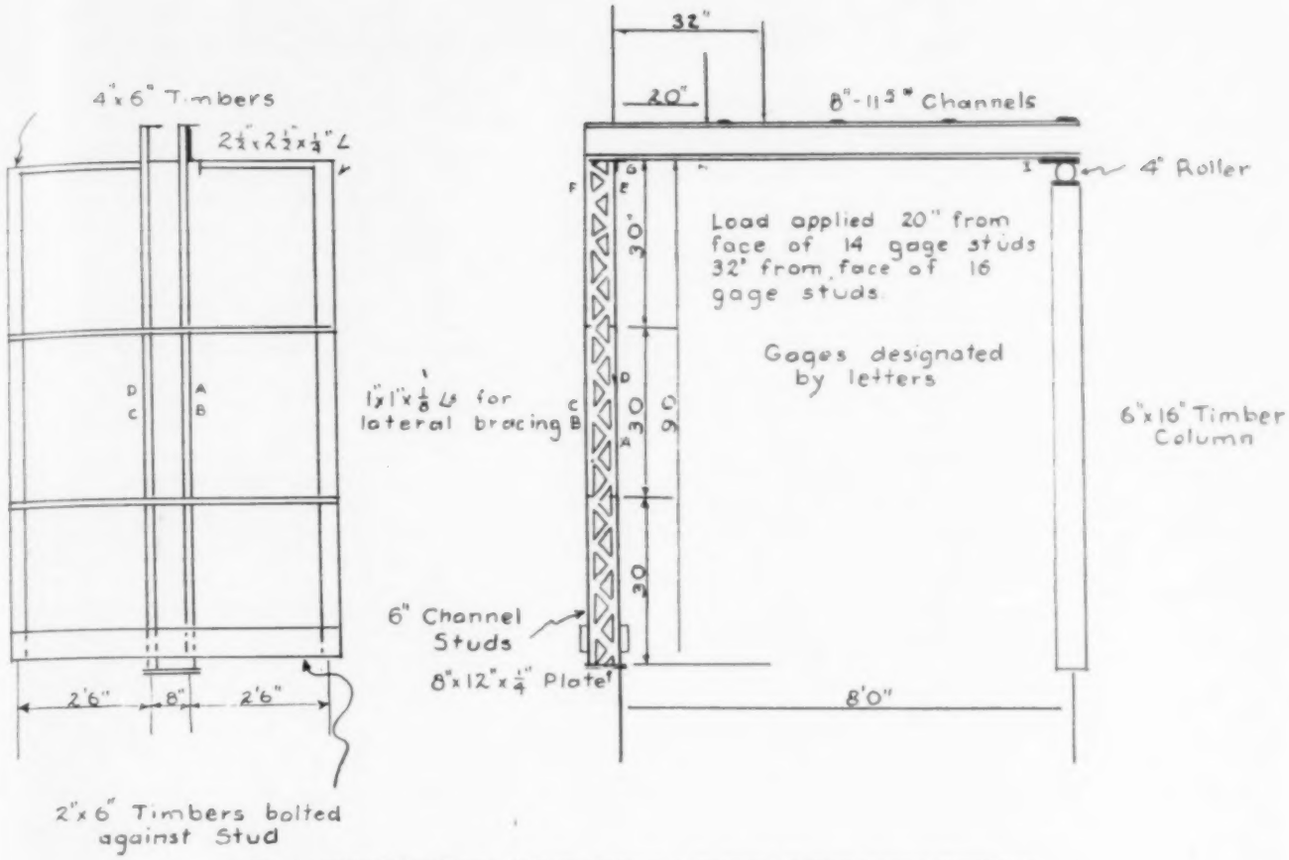


Fig. 11—Welded Steel Frame for Tests Made on 14-Gage and 16-Gage Channel Type Studs

TEST NO. 3

The studs used in this test were manufactured from 14-gage flat rolled steel by the Soule's Steel Company. The computed properties of the studs are shown in Fig. 11.

The frame used in this test consisted of two studs and two beams. Strain gages were attached to the inside and outside flanges of both studs at the center of the stud. Gages were also attached to the inside and outside flanges of the Right stud, so that they measured the stress at a point 8 inches below the top of the stud. The deflection of the joists was measured by gages under the load point and at each end of the span.

The specimen was tested to destruction August 23, 1937. The maximum load on the frame, which was applied 20 inches from the face of the studs was 28,920 lb. This made a direct load on each stud of 11,580 lb., plus a bending moment of 13,730 inch-pounds. The resulting stress at failure was 42,350 lb./sq. in. This stress was also figured on the basis of a hinged joint at the bottom of the studs. The method of clamping the sill plate to the weighing table was the same as in the previous test. The sill plate on these studs was only ¼ inch thick, however. The difference in the thickness of the plate could be readily noticed when the studs were rocked back and forth. There was no bending in the plate used with these studs. Consequently, there was less rigidity in the joint. There did not appear to be a rigidity of more than 25% in this joint. There is enough rigidity in the joint to make the actual stresses at the top of the stud probably a little larger than those that have been computed on the basis of a hinged end.

The stress as measured by the strain gage placed 8 inches below the top of the Right stud becomes slightly larger than the computed stress at the top of the stud as the load is increased. If the stress is computed for a point 8 inches from the top of the studs the difference between the measured stress and the computed stress becomes larger due to the reduction of the bending moment in the stud. This fact indicates the possibility of a greater degree of rigidity in the joint than was assumed. The inside flange at the top of the Right stud began to yield when the stress measured by the gage at this point was 38,000 lb./sq. in. The elastic limit of the material in the studs as shown by the tensile tests was 37,500 lb./sq. in. The failure of the stud occurred at a computed stress of 42,350 lb./sq. in. The yield point stress of the material was found to be 43,500 lb./sq. in. The stress at which the stud began to yield and the stress at which the stud failed are practically equal to the elastic limit and the yield point of the material as shown by the tensile tests.

The failure took place suddenly with little preliminary warning. The failure was confined to lateral buckling in the upper third of the studs with consequent crimping of the flanges at the point of buckling. The direction in which the studs buckled was determined by the location of the joist channels. In this test the studs buckled away from the loading beams just as they did in the first test. There was no failure of the web members as can be seen in Fig. 12. The buckling occurred either across the hole in the web or along the web member from one flange to the other.

The L/r between the lateral supports of the studs in this test was 125.0. The safe load as given by the A.I.S.C. formula is $0.430 \times 9636 = 4150$ lb. For this load the stress in the inside flange at the top of the stud is 14,500 lb./sq. in. and in the outside flange at the top the stress is 6600 lb./sq. in. These stresses are well below the elastic limit of the material and give a factor of safety for the studs as used in this test of almost 3.

TEST NO. 4

The studs used in this test were manufactured from 16-gage flat rolled steel by the Soule's Steel Company.

The stud frame used in Test No. 4 was identical to that used in Test No. 3 with the exception of the studs.

The specimen was tested to destruction August 27, 1937. The load was applied 32 inches from the face of the studs. The maximum load on the frame under which the studs failed was 30,980 lb. This made a direct load of 10,480 lb. on each stud plus a bending moment of 15,640 in. lb. The resulting unit stresses in the stud was 52,810 lb./sq. in. These figures are again based on the assumption of a hinged end at the bottom of the studs as the end conditions in this test were the same as those in the previous test.

Curves of the deformation in the studs in relation to the total load on the frame and the computed load on a single stud show that the deformation at the center of the outside flange of the Right stud is greater than the deformation at the top. This does not follow the results of the previous tests nor is there any apparent logical reason why such a condition should exist. Curves for the locations on the outside flange run parallel until near the ultimate load when the deformation at the top becomes quite large.

In these studs, as in those of Test No. 3, the measured stress at the top of the studs is greater than the computed stress at the same point, when the computations are based on a hinged end at the bottom of the stud.

In this test the studs began to yield when the stress at the top as shown by the gages was about 35,000 lb./sq. in. while failure occurred at a stress of 52,810 lb./sq. in. These stresses are well below the elastic limit and the yield point of the material in these studs as shown by the stress-strain diagrams for specimen No. 7. Specimen No. 7 was cut from the flange of the 16-gage stud and tested in tension to determine the properties of the steel. The elastic limit as determined in this test was 55,000 lb./sq. in. while the yield point stress was 65,000 lb./sq. in. Specimen No. 8 was also cut from the flange of the same 16-gage stud. The properties of this specimen are nowhere near as high as those of specimen No. 7. The flanges of the 16-gage stud had several spots upon them where they had been heated for straightening kinks. One of these heated spots was contained in specimen No. 8. The failure occurred at this burned spot and necking down began in the burn at a very early load. The weakening of the material in the flanges by the heating no doubt cut down the stresses at which the studs failed.

The outstanding characteristic of this test was the action of the studs for the loading just before failure occurred. When a load of 20,000 lb. had been placed on the frame the outstanding legs of the stud flanges had begun to buckle at the points at which complete failure later occurred (Fig. 13). These points at which the buckling had started were in every case a point where the material had been heated for straightening. It is significant that the stress in the studs at this load was approximately 35,000 lb./sq. in. which is slightly under the stress at the elastic limit of the heated material as shown by the test of specimen No. 8. This buckling of the flanges did not, however, seem to have an immediate effect upon the action of the studs in carrying the load. The load was increased from 20,390 lb., at which point the upper gages were removed, to 30,980 lb. before complete failure had taken place in the stud. At the heat-weakened sections most of the load was apparently carried by the unpunched section of the web throughout the final 10,000 lb. of the loading.

The failure of the entire stud was of the same type as that of the 14-gage studs in Test No. 3, the studs failing due to lateral buckling of the flanges in the upper third of the stud without any failure in the web.

The L/r between lateral supports of these studs was 123.2. The safe load from the A. I. S. C. formula is $0.337 \times 9770 = 3300$ lb. The unit stress for this load is 17,500 lb./ sq. in. in the inside flange and 9000 lb./sq. in. in the outside flange. These stresses are well below the elastic limit of the material and give a factor of safety of 3 for the studs as used in this test.

TEST NO. 5

The studs used in this test were manufactured by the Bethlehem Steel Company. These studs had a slightly heavier web than those of the same type tested in Test No. 2.

The frame used in this test was identical to that used in Test No. 2 (Fig. 14).

The specimen was tested to destruction December 7, 1937. With a load of 48,750 lb. on the frame the flanges of the studs began to buckle outward. At this point the

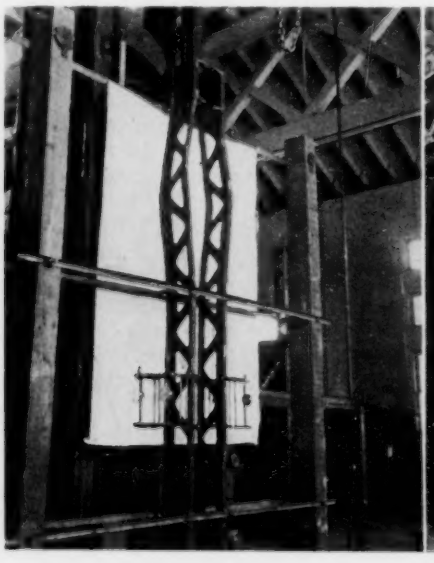


Fig. 12—14 Gage Channel Type Studs

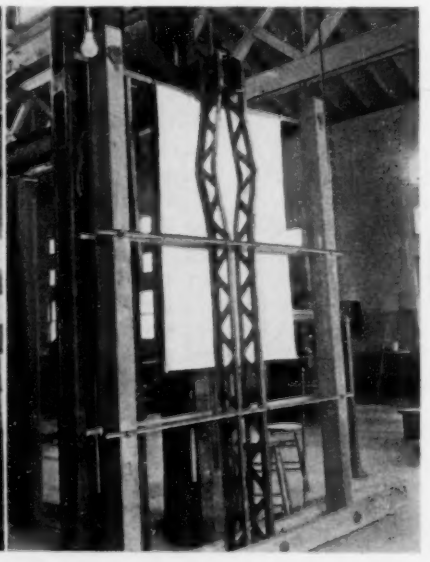


Fig. 13—16-Gage Channel Type Studs

direct load on each stud was 12,400 lb. while the bending moment was 25,000 in. lb. This made a stress in the stud of 39,150 lb./sq. in.

In this test as in the previous ones hinged end conditions at the bottoms of the studs were assumed although this was not strictly true.

These studs showed the same characteristics as the others in that the inside flange carried the larger proportion of the load.

These studs did not fail suddenly as did the lighter Bethlehem studs but failed through a gradual buckling of the stud flanges. As these studs failed in the flanges and the lighter ones failed in the web the increase in the size of the web members has evidently swung the point of weakness from the web to the flanges. There appears to be a delicate balance of strength in the stud between the web and the flanges as there is approximately equal strength in each (Fig. 15).

The manner in which the studs failed indicates that the rigidity of the ends may have been more than was assumed and that consequently there was a bending moment at the bottom of the studs that raised the stress considerably. The failure in both studs was confined to the bottom third rather than to the top as would be expected from the type of loading and the results of previous tests.

The $\frac{L}{r}$ of these studs as used in this test was 114.0. The safe load given by the A. I. S. C. formula is $0.548 \times 10{,}800 = 5980$ lb. The maximum stress in the studs at this load was 15,000 lb./sq. in. This is well below the elastic limit of the material.

RESULTS AND CONCLUSIONS

The results obtained in this series of tests lead to the following conclusions:

1. The rigid connection between the joists and the studs produces a bending moment in the studs making the stress in the inside flanges of the studs larger than that in the outside flanges.
   (*a*) The difference in the stresses becomes larger as the load on the joists increases.
   (*b*) The stress varies along the stud with the maximum stress at the top.
   (*c*) The bending moment in the stud varies with the deflection of the joists. As the joist members become smaller and less rigid the increasing deflections produce increasing bending moments in the studs.
   (*d*) This increase in stress due to bending moment in the studs is not serious within the working range of the studs as they were used in these tests.
2. The lateral eccentricity in the connection between the joists and the studs affects the manner of failure of the studs.
3. Complete failure of the studs took place when the stress at the top of the inside flange of the stud passed the yield point stress of the material in the stud.
4. The web of the Junior I Beam, 14-gage channel, and 16-gage channel type studs are stronger than the flanges of the studs.
5. The web of the light Bethlehem studs is not strong enough to develop the full strength of the flanges when subjected to combined axial load and bending.
6. The web and the flanges of the heavier Bethlehem studs are delicately balanced as to strength, there being approximately equal strength in each.
7. Application of heat to the studs, without adding metal as in welding, definitely weakens the material in the studs and consequently weakens the studs.

DISCUSSION OF RESULTS

All of the tests showed conclusively that the inside flange of the studs received the largest stress. The variation of the stresses in the flanges of all of the studs were similar in that the stresses in the inside flanges became larger in proportion to the stresses in the outside flanges as the load on the joists is increased.

There was no attempt made to measure the stress at the top of the studs in the first test. The manner of fail-

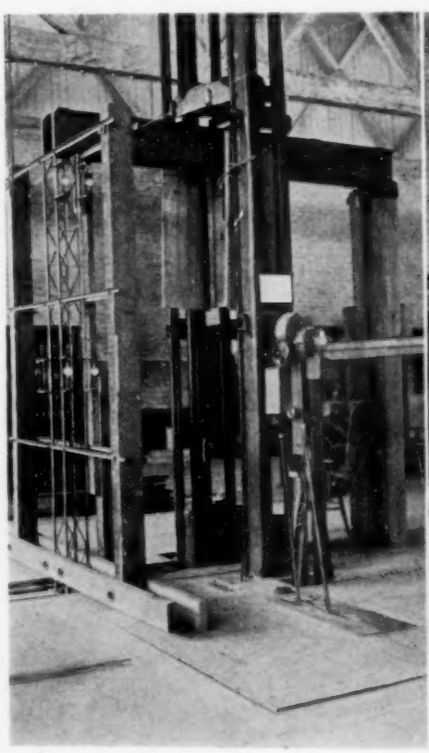


Fig. 14—Heavy Type Bethlehem Studs in Testing Machine

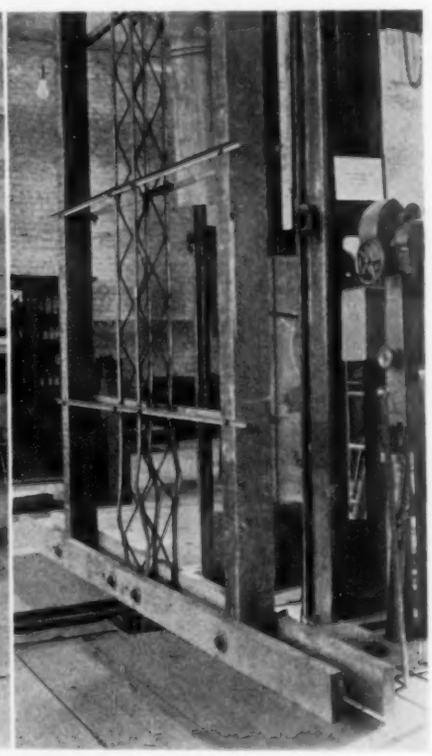


Fig. 15—Bethlehem Heavy 6-Inch Stud Showing Manner of Failure

ure, in which the failure was confined almost entirely to the portion of the studs above the upper lateral supports indicated that the stress was a maximum at the top of the studs. This supposition was confirmed in the remaining tests by securing gage readings near the top of the studs. The difference in the deformation at the center and the top of the studs clearly showed that the maximum stress occurred at the top of the stud.

As the size of the loading beams used with the studs decreased the deflection obtained in the beams increased. The stiffness ratio between the beams and the studs was also lowered by the use of small beams. Consequently, a larger bending moment occurred in the beams and a larger percentage of this bending moment was transferred to the studs. None of the beams used in these tests were as flexible as a long lightweight joist. When such a joist is used with studs of this kind the fact that the stiffness ratios will be more nearly equal and that there will be a fairly large deflection in the joists may produce a bending moment in the stud large enough to be a factor in the design of a building frame.

In these tests the bending moment produced in the studs, although evident in the distribution of the stresses, has not been large enough to have much effect upon the design of a building frame. The fact that all of the studs has a factor of safety of 2.33 or better when compared to the safe load as given by the A.I.S.C. formula emphasizes this point.

Throughout the discussion of these tests the reference to computed stress has been used as referring to the maximum stress as computed from the formula $P/A \pm Mc/I$. The close agreement of the measured stresses and the computed stresses has been obtained by comparing the measured stress in the inside flange with the maximum computed stress. Theoretically the minimum stress should occur in the outside flange and the measured stress should be closely equal to the computed stress computed from the formula $P/A - Mc/I$. If the data sheets for the various tests be examined it will be seen that this theory does not hold at all. The measured stress in the outside flange is, in all cases, much larger than the computed minimum stress for the stud. This is very likely caused by the welding of the loading beams to the sides of both flanges of the studs. The angle welded across the face of the studs upon which the loading beams rest causes a great deal of the load to be carried by the inside flange, but the size of the beams and the fact that they were welded to the outside flange of the stud make it probable that they transferred more of the bending moment to the outside flange of the stud than would be considered in using the formula $P/A - Mc/I$ in finding the stress at the outer face of the studs. The eccentric nature of the connection between the joists and the studs should also affect the stress in the outside flange. Just what effect this connection has in the actual stress was not determined. If the formula is expanded to be $P/A + Mc/I_{1-1} + Mc/I_{2-2}$ the resulting computed stress is much higher than that actually obtained in the tests. That this eccentric connection does have a large effect upon the action of the studs is evident by the manner in which all of the studs, with the exception of the Bethlehem studs, failed.

All of the studs began to yield somewhat without increase in load when the stress at the top of the inside flange of the stud reached the elastic limit of the material in the studs. Complete failure occurred when the stress reached the yield point stress with the exception of the 16-gage channel type studs which failed at a stress considerably below the yield point of the material.

The failure of the light web Bethlehem studs was due to the buckling of the web and the consequent buckling of the flanges. There was no lateral buckling of the studs as the web failed before the load was great enough to cause lateral buckling of the flanges.

The failure of the heavy web Bethlehem studs was caused by the buckling of the flanges between the points of support by the web members. There was no lateral buckling of the studs.

The three other types of studs failed by the lateral buckling of the flanges.

The 16-gage channel type studs had been heated in several spots along the flanges to straighten kinks. Buckling took place at these heated spots, the outstanding legs of the angles having buckled at a load considerably lower than that at which the entire stud buckled. The heating of the flanges undoubtedly caused the early failure of the studs. The change of physical properties of the material due to heating was clearly shown by testing tensile specimens cut from the studs, one of which contained one of these burned spots. It should not be construed by this lowering of the physical properties of the metal by heating that welding of the joints weakens the studs. There was no sign of any failure around the welds. The excess metal that is deposited on the weld evidently more than compensates for the weakening of the base metal. The joints of the frames used in these tests had a great deal more welding than was required to produce the strength necessary to transfer the stress from the beams to the studs. The difficulty of welding

Table 1—Comparison of Test Results

Type of Stud	Area, Sq. Inches	Weight per Foot, Lb.	$\frac{L}{r}$	Direct Load at Failure, Lb.	Bending Moment at Failure, Inch-Lb.	Maximum Unit Stress at Failure, Lb./Sq. Inches	Safe Load, Lb.	Direct Load Carried by Stud per Lb. per Ft., Lb.	Type of Failure
Junior I Beam	1.015	3.79	89.3	31,440	25,610	41,735	13,200	912	Lateral buckling
Bethlehem Light Web	0.5094	2.04	117.6	12,000	22,640	39,620	5,200	652	Web buckling
14-Gage Channel	0.430	1.85	125.0	11,580	13,730	42,350	4,150	625	Lateral buckling
16-Gage Channel	0.337	1.40	123.2	10,480	15,640	52,810	3,300	750	Lateral buckling
Bethlehem Heavy Web	0.548	2.40	114.0	12,400	25,000	39,150	5,980	574	Flange buckling

Table 2—Physical Properties of Steel in Studs

Properties	Type of Studs: Junior I Beam	Bethlehem	14-Gage Channel	16-Gage Channel
Elastic Limit lb. sq. in.	37,500	30,000	37,500	55,000
Yield Point lb./sq. in.	42,000	36,000	43,500	65,000
Ultimate Strength lb./sq. in.	63,300	65,250	63,200	75,600
Modulus of Elasticity lb./sq. in.	28.6×10^6	28.6×10^6	30×10^6	29×10^6

the 8-inch channels to the very thin 16-gage material necessitated the heating of the thin material until it was almost burned through. As this occurred at the top of the stud where the stress was a maximum it might be expected that this metal around the weld would show signs of failure, but such was not the case. The welds did not appear to be affected in any way by the stress that caused the failure of the studs.

COMPARISON OF STUDS

Table 1 presents in tabular form the results obtained in the separate tests. In comparing the direct loads that each stud carried, it must be borne in mind that the bending moments in the studs varied. The safe load given in the table is computed from the A. I. S. C. formula.

QUALITY OF MATERIAL

Table 2 summarizes the results of tests on tensile specimens, two of which were cut from each type of stud. The specimens were cut from the flanges of the studs near the bottom where the metal had not been overly stressed.

The results of the two specimens of each type of stud checked very well with the exception of the 16-gage material, the heating of which has been explained in connection with test No. 4 and in the discussion of the results.

All of the specimens fulfilled the requirements of the American Society for Testing Materials.

STRENGTHENING OF THE Austerlitz Bridge (Paris) by Electric Arc Welding*

By M. FAUCONNIER†

THE Paris Metropolitan Railway line No. 5, "Gare du Nord—Etoile" crosses the River Seine, not far from the "Jardin des Plantes," over a structural steel bridge 458 ft. in span, called the Austerlitz Bridge (Fig. 1).

This remarkably fine and bold structure is a parabolic three-hinged arch. The side hinges are at the end of the lower extensions or brackets, which overhang by nearly 55 ft. This arrangement gives the bridge a neat appearance, but lends itself very badly to a material increase of traveling loads.

Fig. 1—The Austerlitz Bridge, Paris

During the last 40 years, all Metropolitan Railway Companies have had to increase the capacity, the power, and consequently the weight of their trains.

The Austerlitz bridge had been designed in 1903 for carrying light trains weighing 135 tons‡ and measuring 163 ft. in length. The typical Metropolitan train now measures 342 ft. in length and weighs 470 tons; the traveling overload has been more than trebled.

The problem of strengthening a bridge spanning about 450 feet, in constant service and carrying a daily traffic of more than 700 trains, was not an easy one.

STRENGTHENING

The method of reinforcement which was finally chosen consisted in:

(*a*) Increasing, up to 60 per cent, the working section of all weak members in the structure, by means of plates and profiles fastened by electric arc welding.

(*b*) Restoring the stability of the lower extensions by fixing, under the bottom flange of each unit, an extra shop-assembled framework placed in position in a single operation; this framework rests on a special bearing, the corresponding reaction being adjusted by means of jacks.

(*c*) Reinforcing the abutments by building a reinforced concrete block on the land side, and, on the river side, a reinforced concrete sill supporting the new abutment bearings.

*Paper read at a Joint Meeting of the Institution of Structural Engineers, the British Section of the Société des Ingénieurs Civils de France and the Institution of Civil Engineers, London, January 1939. Published in *The Structural Engineer*, 17, (New Series), No. 1, 61–95, January 1939.

†Directeur des Travaux Neufs du Metropolitain, Paris.

‡ Long tons have been converted to short tons throughout this article.

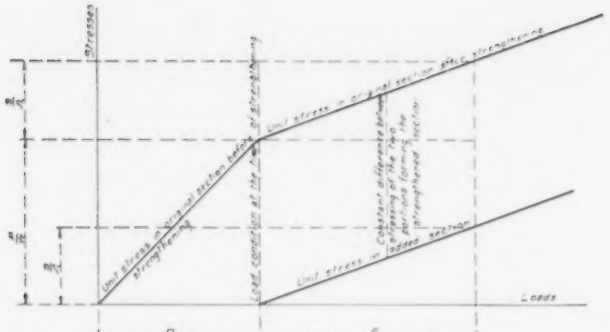

Stresses
Unit stress in original section before strengthening
Load condition at the time of strengthening
Unit stress in original section after strengthening
Coherent difference between the stressing of the two portions forming the strengthened section
Unit stress in lagged section
Loads
P
Load borne by original section alone
S
Load borne by strengthened section
W = Original section
A = Strengthened section

Fig. 2—Distribution of Stresses in a Strengthened Part

MATERIAL USED FOR THE AUSTERLITZ BRIDGE

Selection of Material for Reinforcement

(*a*) *Base Material.*—The Specifications of 1903 required the following physical properties for the bridge steel:

Ultimate tensile strength (R): 60,000–64,000 lb./in.²

Elongation (A%): 20–22% which pointed to mild steel.

Various chemical analyses and micrographic examinations made on samples taken from the bridge showed that the material in question was extra-mild open-hearth steel, containing approximately:

C	...	0.09%
Mn	...	0.36%
Si	...	0.01%
P	...	0.06%
S	...	0.04%

Such an analysis did not leave any room for doubt as regards weldability.

The specifications for the reinforcement steel were as follows:

Grade and Manufacturing Process:

Extra-mild open-hearth steel.

Chemical Analysis:

Carbon	Maximum 0.10%
Manganese	" 0.38%
Sulphur	" 0.04%
Phosphorus	" 0.04%
Silicon	" 0.20%

Mechanical Properties:

Minimum ultimate tensile strength 57,000 lb./in.²
Minimum elongation on failure 25%
Minimum resilience per sq. cm. (Mesnager test specimen) 10 kgm.

In addition, it was specified that if, in the case of large profiles, some incompatibility appeared between chemical and mechanical requirements, the latter only would be allowed a tolerance limited to 42,600 lb./in.² for the tensile strength.

Severe tests were carried out at the mills for checking the chemical analysis.

SELECTION OF ELECTRODES

For this purpose, a high grade coated electrode must be used. An extensive range of electrodes of very unequal merits can be found on the market. After comparative tests in order to determine the most suitable type, an electrode with a "semi-volatile" coating was chosen, the average characteristics of which are as follows:

Elastic limit	47,000 lb./in.²
Tensile strength	67,000 "
Elongation	21%
Resilience (Mesnager)	13 kgm. per cm.²

This comparatively high resilience indicates the quality of the deposited metal and, in particular, points to the absence of nitrides, which are a well-known cause of brittleness in steel. The molten metal is protected against the action of atmospheric oxygen and nitrogen by the gases produced by the combustion of cellulosic materials contained in the coating in the proportion of about 30 per cent.

The slag, which is very friable, is easily removed with a wire brush.

The constant standard of quality of the electrodes was carefully checked in the course of the work.

CALCULATION OF REINFORCEMENTS

1. *Principles of Calculation.*—The reinforcement was calculated on the basis of the French Rules of May 10, 1927, which fix the permissible working stresses at the rate of 18,500 lb./in.² (without wind) and 20,000 lb./in.² (with wind).

It has been assumed, for calculating the total stressing, that the original sections (before strengthening) alone withstood permanent loads and that the final (strengthened) sections worked in a homogeneous manner under overloads and wind action.

This assumption is expressed, in the case of plain tension, by Fig. 2. It has been experimentally checked and found correct.

The sections of the reinforcements calculated on this basis varied, for the arch flanges, between 60 and 70 per cent of the original section.

The above assumption may in some cases be somewhat inaccurate as regards the stressing of the original material. This should not be taken too seriously. Thanks to the ductility of the united materials, the added steel, which is stressed far below its capacity, will relieve the original metal, without allowing the latter to exceed yield point in the most unfavorable cases.

2. *Welds.*—The welds have been calculated in accordance with the French circular of July 25, 1935. In particular,

(*a*) The permissible unit stresses in welds are lower than the safety limits permitted for the base metal, the factor of reduction being 0.55, 0.75 or 0.85 according to the kind of load and the type of weld.

(*b*) In order to provide for the rapid stress variations due to traveling loads, the welds are calculated so as to withstand an extra stress equal to 0.5 $(d'_1 + d'_2)$, d'_1 and d'_2 being the value of the maximum and minimum stresses due to variable overloads.

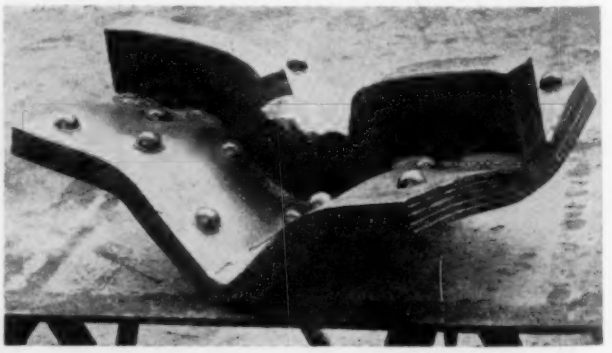


Fig. 3—Distortion of Reinforced Arch Section Under Pressure in the Cold Condition

The welds to be used could be divided into two classes according to the kind of stresses to which they were subjected:

(1) Longitudinal welds extending over the whole length of the reinforcement and uniting it with the original member. This applies, in particular, to the arch reinforcement welds.

These welds are subject to longitudinal shear. As in this case, calculations had indicated dimensions which were thought inadequate, bending tests were carried out on a trial assembly so as to determine a suitable weld section. After a few trials, a 0.55-in. weld was decided upon, reduced to 0.20 in. opposite flange rivets. The transition is gradual in order to avoid an undesirable "notch effect," which is particularly dangerous in the case of variable stresses.

The photograph (Fig. 3) shows that this weld proved satisfactory, as considerable distortion was attained without any separation of the riveted and welded parts.

It should be noted that this test is an interesting demonstration of the homogeneity of the strengthened assembly.

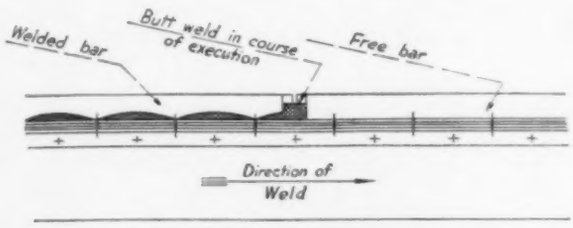


Fig. 4—Welding of Arch Reinforcement Bars

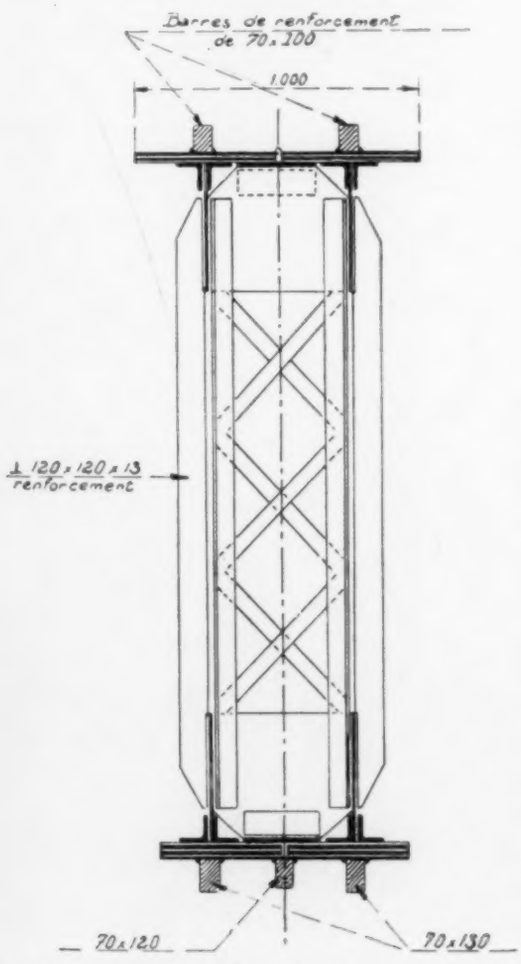


Fig. 5—Cross Section of an Arch. (Dimensions in Millimeters)

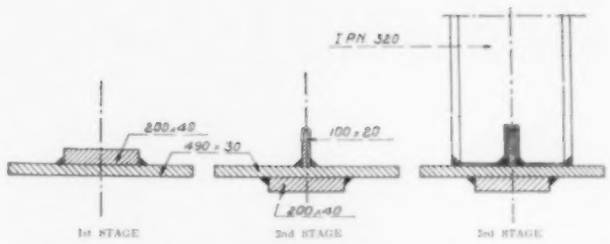


Fig. 6—Construction of Lower Extension Framework. (Dimensions in Millimeters)

(2) A more important part was played by another class of welds, those which contributed to hold structural members together and those used for joining two abutted parts.

The former are called upon to cooperate with existing rivets. As the rivets were already loaded under the action of the weight of the structure, no slip was to be feared on the part of the riveted joint, which might have transferred the whole of the load to the more rigid weld; the strength of the compound joint was therefore guaranteed by the existing load on the original riveted joint.

STRENGTHENING THE MAIN PARTS OF THE STRUCTURE

Arches.—The lower and upper arch members were reinforced by adding rectangular bars measuring from 2.8 x 4.0 to 2.8 x 5.5 in., arranged between the rivet rows and welded on edge (Figs. 4 and 5).

It was necessary to machine the existing and added parts very elaborately. The strengthening bars, averaging 19.5 ft. in length, were very rigid transversally. Moreover, they were delivered in the rough-rolled condition, which did not allow such a perfect contact between them and the bridge members as was essential for ensuring the homogeneity of the strengthened ensemble.

This result was obtained in the following manner:

(*a*) The flanges were ground where the strengthening bars were to be laid in order to ensure a perfectly flat bearing in the transversial direction. This operation was performed by means of grinding machines specially designed for this purpose.

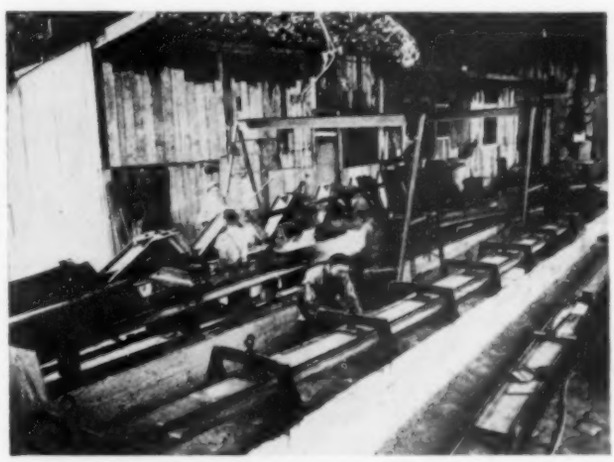


Fig. 7—Construction of Extra Framework for Lower Extensions. The Three States of Construction Are Visible. In the Foreground, Bending and Welding the Flanges, Welding the Vertical Webs, and in the Background Assembling the Lattice Work

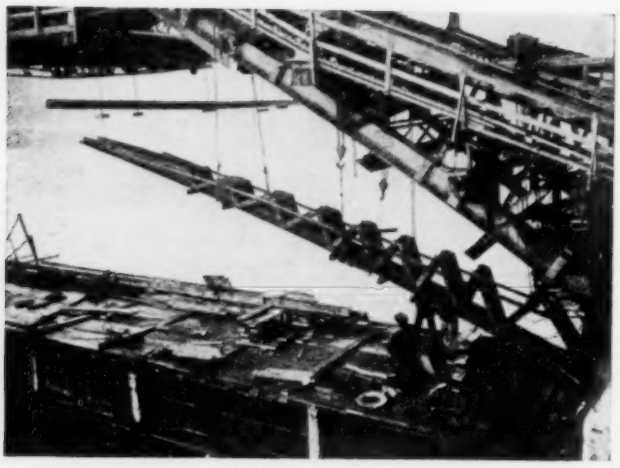


Fig. 8—Erection of Supplementary Lower Extension Framework

(*b*) Templates were then made, reproducing the longitudinal profile of the ground surfaces. Each template consisted of a 0.20 in. thick steel strip cut as required and bolted to a rigid lattice frame.

(*c*) The bars were forged on the forge and machined in the shop in accordance with the templates.

After these operations, the 19.5 ft. long bars were laid on the flanges without any retouching, the tolerance allowed being 0.020 in.

As regards the arch lattice work, the uprights and diagonals were reinforced by means of tees. In order to ensure proper jointing with the members, the tee flanges were machined at the ends and the webs welded on the web of the member (Fig. 5).

Lower Extensions.—The additional framework for the lower extensions was to be shop-built and delivered on site for erection. Due to the difficulty of handling this framework and especially of checking the curvature (as the framework was to fit the extensions very closely) this solution was abandoned and the construction took place on the banks of the river at the foot of the bridge.

The construction of this framework was performed in three stages (Fig. 6):

(1) Bending and welding the flanges together.
(2) Welding the vertical web.
(3) Assembling the lattice work.

The finished parts were checked with a template reproducing the bridge curvature, then coupled for erection.

This operation is shown on Fig. 8.

During the hoisting operation, every precaution was taken so as to avoid distorting the parts which formed a girder minus the upper member. They were temporarily held in position by means of cross ties and threaded rods until the scaffolding for their final welding was erected.

Arch Hinges.—The arch hinges consist of a steel pin 6 in. in diameter surrounded by two cast steel saddles. As it seemed unnecessary to strengthen these parts, samples were taken in order to determine the weldability of this material.

As a chemical analysis revealed high carbon and manganese contents, it was not thought advisable to apply the electric arc on to such vital parts of the structure before making sure that strengthening was absolutely necessary.

Calculation results were bound to be uncertain due to the complicated and unknown stress distribution in the ribbed sections; it was, therefore, decided that tests should be made on models built to scale.

The first crushing tests in a 2000-ton press was made on a half-size model (Fig. 9). This model, in addition to the hinge to be tested, also included an arch section and a lower extension section with such strengthening as was planned for these parts.

The measurements taken showed:

For the saddles, a slight collapse in the plane of symmetry passing through the pin, and a marked collapse on the edges.

For the arch sections, a marked collapse of the central portion, and a slight one on the sides.

The failure occurred under a load of 1400 tons, corresponding to a theoretical safety factor of 4.25.

Figure 10 shows that, in the saddles, the central rib does not work in a satisfactory manner, due to the weakness of the arch sections which transmit the load.

After fresh full-size tests, the arrangements shown on Fig. 11 was finally adopted. The arch reinforcements intended for transferring part of the load on to the central rib of the saddle consist of 2.4 in. thick plates cut in the shape of an A and welded on their periphery with 0.47-in. fillets.

A reinforcement of the cast-steel saddle was found unnecessary.

Roadway Parts.—The longitudinal beams and cross ties have been strengthened by adding flanges on their lower portions (0.79 x 0.32 in. for longitudinal beams and 1.58 x 1.58 in. for cross ties) and by welding their top portion to the floor plates, the latter being made to act as flanges (Fig. 12).

This plate, however, had a joint along the center line of each longitudinal beam and cross tie, which necessitated laying longitudinal straps on the floor plating, a dfficult operation which involved partially dismantling and temporarily supporting the tracks. This could only be done by night during the short interruption of train traffic.

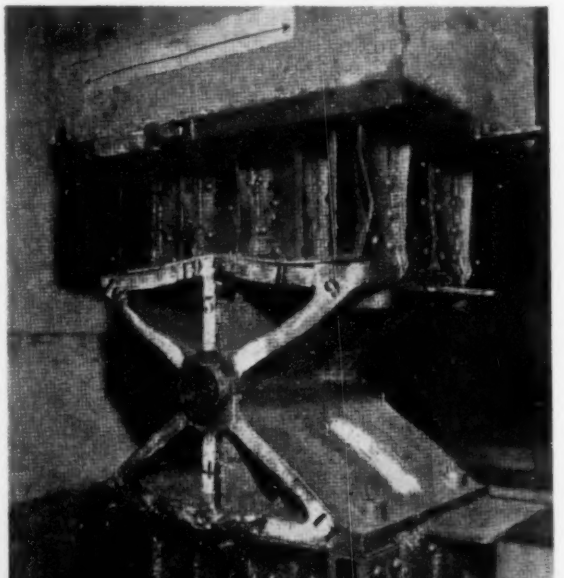


Fig. 9—Reinforcement of Hinges. First Test—General View After Collapse

ACCEPTANCE OF ELECTRODES, CHECKING OF OPERATORS AND SUPERVISION IN THE COURSE OF WORK

Methods for determining the soundness of a finished weld are as yet rather uncertain or difficult to apply in the case of a strengthening job. For instance, a systematic application of X-rays was practically impossible in the case in point.

Besides, although it may be interesting to find out hidden defects within a *finished* joint, it is no doubt still more desirable that proper measures should be taken so as to avoid such defects.

It was, therefore, considered that the best guarantee as to the soundness of welds lay in the selection of proper electrodes and of skilled welding operators, hence very severe acceptance tests, inspection and supervision in the course of execution.

As regards electrodes, these were only accepted after tests showing that the filling material possessed mechanical properties up to the required standard (tensile strength, elongation, resilience). For this purpose, tensile and resilience test specimens were prepared exclusively from the material deposited by means of the proposed electrodes.

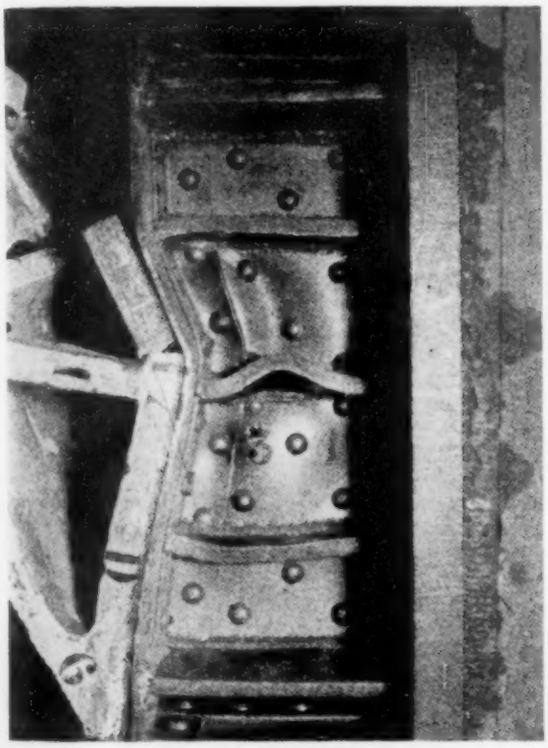


Fig. 10—Reinforcement of Hinges. First Test—Condition of Arch Section After Collapse

As regards operators, no welder was accepted unless he had satisfactorily passed a qualification test. This test was always made on the site under the normal conditions which the operator would meet in actual practice.

The test, which had purposely been chosen as simple and as conclusive as possible, consisted in butt welding two V-bevelled plates 0.32 in. thick, with a reinforcement weld on the underside. A tensile test specimen and two bending test pieces were then cut out.

A first weld was made in the flat position and a second overhead.

After this test, an individual card was filled up for each accepted welder, showing the results obtained, the

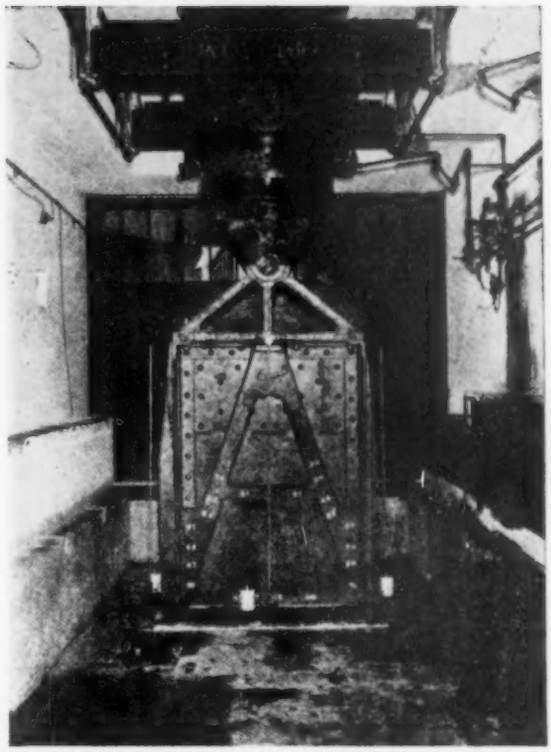


Fig. 11—Reinforcement of Hinges. Last Test—General View of the Test Model in the 2000-Ton Press

results of further examinations made in the course of the work being afterward added.

These examinations took place every six months for all members of the welding staff.

The work of some operators was checked in the meantime whenever the appearance of welds pointed to internal defects.

Inspection of Finished Welds.—The compliance of the welded joints with the program of work (position, length, section) was regularly checked.

Wherever welds appeared to be defective, and at regular intervals in sound-looking welds, soundings were made by means of the Schmuckler milling device.

This device (Fig. 13), which is easily operated, digs out, by means of an adjustable milling cutter, a semi-conical hole such as shown on Fig. 14. By means of a special device, both fillet welds and butt welds may be sounded in this way.

Before being examined, the walls of the excavation were first etched with a 20 per cent nitric acid solution.

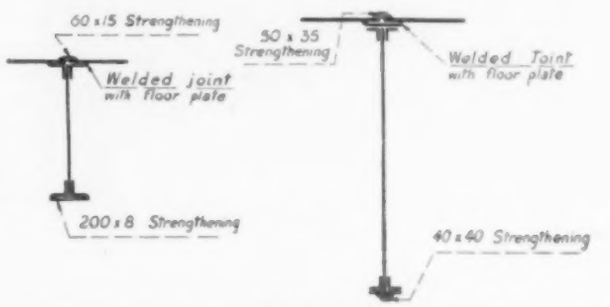

60 x 15 Strengthening
50 x 35 Strengthening
Welded joint with floor plate
Welded Joint with floor plate
200 x 8 Strengthening
40 x 40 Strengthening

Fig. 12—Arrangement of Strengthening for Longitudinal Beams and Cross Ties
Right: Longitudinal Beam
Left: Cross Tie
(Dimensions in Millimeters)

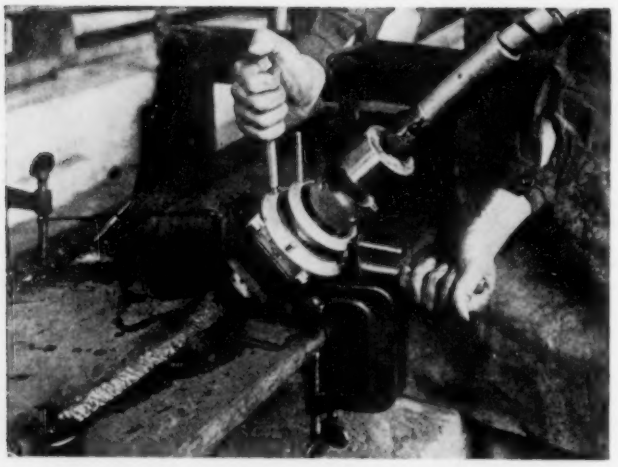


Fig. 13—Schmuckler's Apparatus for the Inspection of Welds by Milling

This inspection method provides useful information as to the texture of a weld. It shows up defects in the deposit (slag, inclusions, etc.), the depth of penetration in the base metal, proper or incomplete fusion at the bottom.

Combined with severe qualification tests, the Schmuckler process seems to be highly suitable for field inspection and checking. It is inexpensive in use and does not call for specialists. In addition, it supplies, on the internal condition of a weld, such positive information as does not leave any room for controversy with the contractors.

In France, the Austerlitz Railway Bridge will always be held up as a beautiful illustration of what can be achieved with that modern tool, electric arc welding.

Two hundred and eighty tons of steel, that is to say, more than one-third of the original weight of the bridge, fastened by 92,000 in.^{3} of weld material on a structure overrun by 700 trains daily; such figures express the magnitude of the job which has just been completed under remarkably safe conditions.

This fine result has been obtained at the cost of a great deal of care and patience; no precaution has been neglected in order to guarantee a perfect execution.

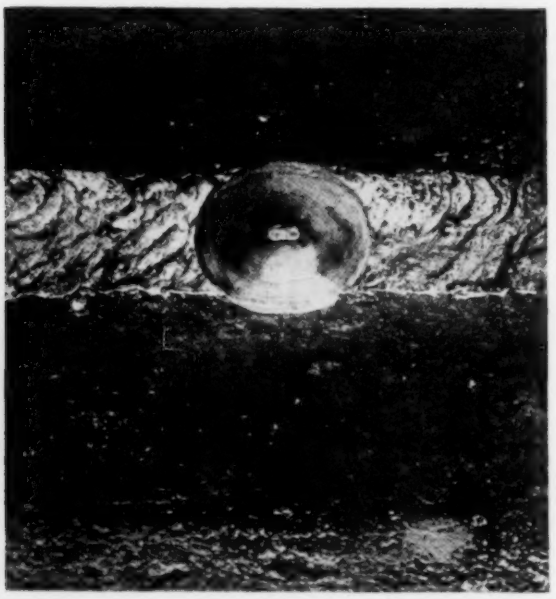


Fig. 14—Sounding Made with Schmuckler's Apparatus in a Welded Plate 0.79 Inch Thick

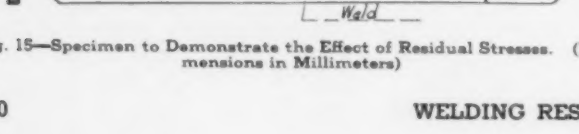


Fig. 15—Specimen to Demonstrate the Effect of Residual Stresses. (Dimensions in Millimeters)

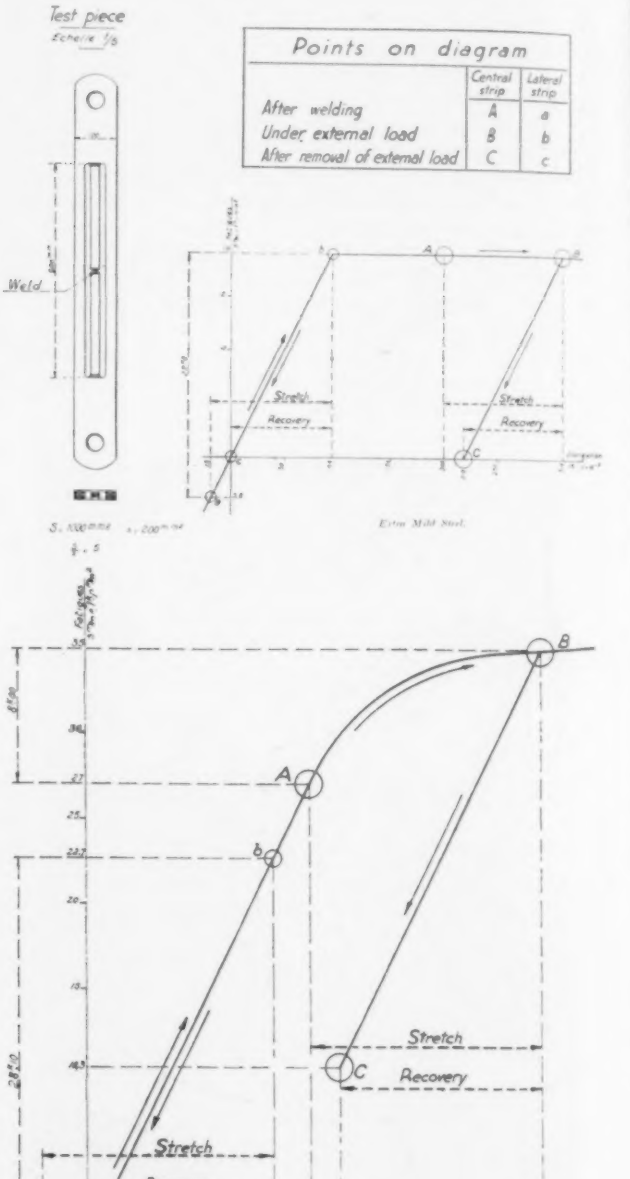


Fig. 16—Reduction of Residual Stresses Under External Load; Comparison of Extra Mild Steel and Ac 54 Steel

Dimensions in millimeters
Stresses plotted vertically; 1 Inch = 7100 lb./in.^{2}
Elongations plotted horizontally; 1 Inch = 0.0002 in.

Results

	Extra Mild Steel	Ac 54 Steel
Contraction, inch	0.043	0.032
Internal stresses, lb./in.^{2}	t = 27,000	t = 38,500
	c = 5,400	c = 7,700
External load, tons	23	30
External stress, lb./in.^{2}	27,000	50,000
Residual stresses, lb./in.^{2}	t = 0	t = 14,600
	c = 0	c = 2,900

RESIDUAL STRESS RELIEF UNDER EXTERNAL LOADS

The residual stress resulting from restrained distortion must logically disappear if a permanent distortion is artificially created, equal to or greater than the restrained distortion. It will, therefore, be sufficient to extend the part so as to produce a permanent distortion equal to the restrained contraction, in order to obtain, on removing the load, the elimination of any residual stress.

The steel grades which will best lend themselves to such a treatment will obviously be extra-mild steels of the No. 37 type, with a low yield point and an elastic period reaching 2 per cent without increase of load. Contraction rarely exceeds half this figure; a slight permanent stretch, on the plastic state level, will therefore compensate for such contraction.

This theoretical normalization process or stress relief under external loads was experimentally confirmed, in the case of a simple distortion (tension and compression) by means of three-branch test specimens.

Such a test piece, as shown in Figs. 15 and 16, consists of two lateral strips, with a total section S, and a central strip, with a section s, welded in the middle, the whole piece being machined and mounted on a pulling machine through cardan devices so as to avoid bending stresses. Position lines are marked on the lateral and central strips.

The contraction due to the butt welding causes a stretching of the central strip, which is in tension, and a shortening of the lateral strips under compression.

As to the contraction R, it is equal to the sum of the elastic distortions: δ of each central half strip (tension), δ' of each lateral strip (compression)

$$R = 2\delta' + \delta$$

If the whole stressed assembly is now extended under an external load applied by the pulling machine, the lateral strips will stretch elastically and the internal compression will first be relieved, then converted into tension. The central strip is elastically stretched and a certain stress is added to the existing tension. If the extension is carried beyond the elastic limit, the value R of the welding contraction is exceeded within the permanent distortion range (level portion of diagram curve).

If the external load is now removed, an elastic recovery of the assembly takes place. The central strip, which has undergone a permanent distortion greater than the contraction, remains in this stretched condition and, after the load has been removed, is no longer subject to any internal stresses. The same applies to the lateral strips. The new equilibrium thus obtained no longer implies any stressing.

Experimental Verification.—The measurements taken fully confirmed the theoretical considerations.

The test results will be found in Fig. 16.

In conclusion, it has been shown by these tests that the process of reduction of residual stresses under external loads is a general one, and the milder the steel the more efficient it becomes.

Any welded assembly in which internal stresses, even high ones, subsist *locally* and which is submitted to external loads (traveling overloads, for instance) will only be left, after recovery, with reduced internal stresses or none at all, as the stressed zones have undergone a permanent stretch which compensates for the contraction. Stresses will finally apply to a homogeneous assembly.

In particular, in the case of the Austerlitz bridge, made of extra-mild steel Ac 37, the possible presence of local residual stresses at certain points of the assembly does not in any way affect the strength of the structure and the efficiency of its strengthening.

Blowpipe-Annealing High-Chromium Steel

By W. D. WILKINSON, JR.*

THE use of the blowpipe for annealing weld zones in high-chromium steels of the air-hardening type has been recommended extensively in welding literature. For certain applications, this process of annealing permits uses of high-chromium steel that otherwise might not come into consideration. These include cases in which other forms of annealing are out of the question because of the size or immovability of the welded parts and in which the unannealed weld zone would be air-hardened so severely as to impair excessively the toughness, ductility and shock resistance of the metal. Many piping installations in refineries and chemical plants fall into this classification.

The average elongation and free-bend values for specimens of 4 to 6 per cent chromium steels, including some containing small amounts of molybdenum and titanium, have been doubled by blowpipe annealing.

Only those steels with less than about 24 per cent chromium and with enough carbon, nickel or other elements in them to make them air-hardening can be improved in ductility by annealing. Steels with a higher chromium content are usually not air-hardening and hence cannot be greatly improved.

In many cases, the success of a welded high-chromium steel installation depends largely on its being annealed for the right length of time at the correct temperature. The temperature range at which blowpipe annealing of the high-chromium steels should be carried out is from 1200° to 1400° F. (650° to 750° C.). Usually a few minutes of heating in this temperature range will be sufficient.

Because of the impossibility of measuring the temperature accurately in practice, the procedure for attaining the required temperature without overheating is to heat the metal until it just shows a red color in the relative darkness of a shop. If the blowpipe annealing must be done outdoors, the operator can build a shield around the work to shade it from the bright daylight. As an alternative, the operator can schedule his welding so as to complete the annealing phase of the work at twilight.

All parts being annealed should be heated as uniformly as possible. Foresight in this regard will help prevent uneven expansion. The cooling likewise should be uniform; and the part cooled should be protected from drafts.

* General Publicity Department, Electro Metallurgical Company, New York.

SPOT WELDING LIGHT METALS IN a German Aircraft Works*

By DR. K. REICHEL†

PRELIMINARY TESTS

THE first attempts to spot weld light metals in 1933 were not satisfactory The switch contacts of the welding machines quickly became fouled and it was impossible to produce uniform spots. Satisfactory tube controlled machines were developed in 1935. Aluminum alloys, Alclad and magnesium alloys 0.020 to 0.08 inch thick were spot welded successfully. For economic reasons seam welding was preferred to spot welding. The best current and pressure were determined for each thickness of each alloy.

The preliminary tests revealed that the surface finish of the parts to be seam welded has an important effect on the welding process. Oxide films and impurities on the surface increase the electric resistance between the electrodes to such an extent that the weld is overheated and metal may be expelled. Therefore, all sheets to be welded are rubbed with emery paper, the emery dust later being wiped off. The electrodes and rollers, which were made of electrolytic copper, also were rubbed with fine emery paper frequently. Electrolytic copper electrodes have little tendency to alloy with light metals and are not appreciably inferior to Elkonite, which has a slightly lower tendency to alloying. A slightly domed electrode was used for spot welding, because the domed shape is the most rapidly cleaned.

Radiographic examination showed that aluminum and Hydronalium (90–95% Al, 5–10% Mg) generally yielded good welds. Electron (1.8 Mn, 0.3 Si, rem. Mg) was difficult to weld. Alclad also yielded indifferent welds, perhaps because the oxide layer and cladding were not of uniform thickness. Hydronalium had the best welding characteristics and was selected for spot-welded structures.

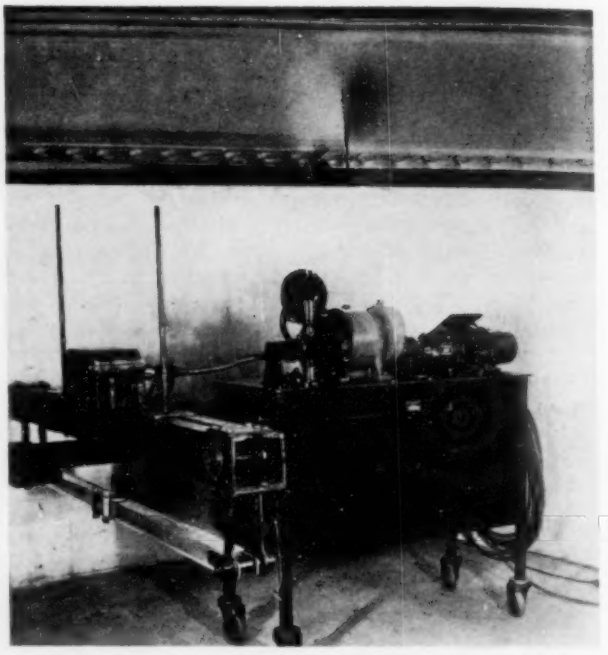


Fig. 2 (*a*)—Fatigue Fracture Near a Spot Weld in a Spar of Annealed Hydronalium 9, 0.039 Inch Thick, Heated 60 Min. at 330° C. After Welding (*b*) Fatigue Testing Machine

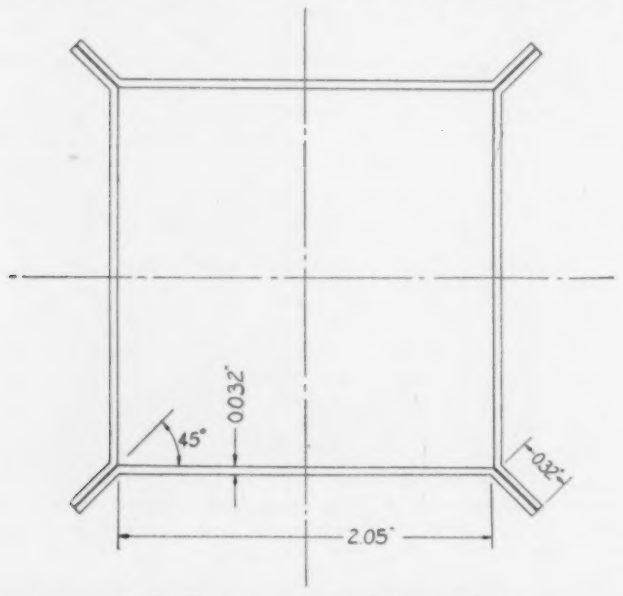


Fig. 1—Dimensions of the Experimental Spars

Specimens with one or two spots were tested in single shear. A single spot in Hydronalium 9, 0.039 inch thick, failed at 440 lb., in the same alloy 0.020 inch thick failure occurred at 260 lb., which is equivalent to a rivet 0.102 inch diameter.

FATIGUE TESTS

Fatigue tests, begun in 1935, were made on riveted and welded spars $6^1/_2$ ft. long, made of Alclad, Hydronalium 9 (9% Mg, annealed as well as heat treated), and annealed Hydronalium 7 (7% Mg) 0.039 inch thick, Fig. 1. The riveted spars were fabricated with rivets 0.102 inch diameter spaced 1.57 inches apart. Similar spars were welded. The spars were vibrated at natural frequency so that maximum stress occurred at the middle of the spar. An amplitude of 0.63 inch was used for the welded and riveted Hydronalium spars and 0.78 inch for the riveted Alclad spars. The results of the fatigue tests are summarized in Table 1.

The as-welded spars had low fatigue value, despite the care that was exercised in their fabrication. The spars were held in a fixture to avoid torsion during spot weld-

*Extended abstract of "Versuche und Erfahrungen mit elektrischer Punktschweissung von Leichtmetallen bei den Arado-Flugzeugwerken," published in *Jahrbuch 1938 der Deutschen Luftfahrtforschung*, 1, 538–548. Paper was delivered before the Lilienthal Aircraft Research Organization in Brandenburg, Germany, Aug. 3, 1937.

† Arado Aircraft Works, Brandenburg, Germany.

Table 1—Fatigue Tests of Riveted and Welded Spars

Riveted Spars

Material	Alclad	Half-hard Hydronalium 9	Annealed Hydronalium 9	Annealed Hydronalium 7
Thickness, inch	0.039	0.039	0.039	0.039
Type of Rivets	Duralumin Rivets	Hydronalium 7 Rivets	Hydronalium 7 Rivets	Hydronalium 7 Rivets
Rivet spacing, inch	1.57	1.57	1.57	1.57
Amplitude, inch	0.79	0.63	0.63	0.63
Number of cycles	1,949,000	819,600	1,208,000	1,173,100
	773,000	597,900	2,720,000	1,984,000
	1,547,000		1,940,600	1,542,200
	1,171,000		3,461,000*	
			1,354,600	

*The rivet spacing was 0.39 inch in this spar

Welded Spars (spot spacing = 0.39 inch; sheet 0.039 inch thick)

	As-Welded		Spar Heated 60 Min. at 330° C. After Welding	
Material	Annealed Hydronalium 9	Annealed Hydronalium 7	Annealed Hydronalium 9	Annealed Hydronalium 7
Amplitude, inch	0.63	0.63	0.63	0.63
Number of cycles	200,000	784,000	8,600,000[4]	8,625,000[4]
			9,411,300[4]	
	350,000		13,744,500[4]	6,226,600[4]
			8,094,200[4]	
	655,000		966,900[1]	8,855,600[4]
			Four spars with spot spacing of 1.57 in.	8,048,400[4]
			8,164,800[4]	5,984,800[4]
			9,123,800[4]	
			4,000,000[2]	
			2,300,000[3]	

[1] Premature failure due to lateral stresses.
[2] Premature failure in grips.
[3] Premature failure due to buckling of spar.
[4] At 4 million cycles the amplitude was increased to 0.79 inch; at 8 million cycles the amplitude was increased to 0.98 inch.

ing. Fine cracks were observed in the vicinity of the main fracture through the spot welds.

HEAT-TREATED SPARS

To remove columnar structure and shrinkage stresses the spars were heated 1 hr. at 330° C. The heat-treated welds had about the same structure as unaffected metal and had improved fatigue value. The amplitude had to be increased to 0.98 inch to cause fatigue failure of the heat-treated spars, which did not occur through the welds, Fig. 2. Although heat treatment raised the fatigue strength of welded spars, it had no effect on the static strength of single spots. The tensile strength of a single annealed spot weld in soft Hydronalium 9, 0.039 inch thick was 405 lb.; unannealed 425 lb.

CORRODED SPARS

To determine the effect of salt spray corrosion fifteen spars were tested:

5 riveted spars of annealed Hydronalium 9; thickness = 0.039 inch, rivet diameter = 0.102 inch, rivet spacing = 1.58 inches.

5 welded spars of annealed Hydronalium 9, not heat treated, spot spacing = 0.39 inch, Fig. 3.

5 welded spars of annealed Hydronalium 9, heat treated after welding, spot spacing = 0.39 inch.

Every hour for 134 days the spars were sprayed for 10 minutes with a 3% solution of NaCl. No unusual corrosion was detected at the welds. After the cleaned spars had withstood 4 million cycles at an amplitude of 0.63 inch, the amplitude was increased to 0.79 inch, Table 2, at which value it was maintained until failure. Corrosion reduced the fatigue value of both riveted and welded spars. The heat treated, welded spar had a higher fatigue value than the riveted spar. The unheat treated, welded spars had surprisingly low fatigue value.

STATIC COMPRESSION AND TORSION TESTS OF CYLINDERS

Cylinders 14 inches diameter were made of annealed Hydronalium 9 sheet, 0.032 inch thick. Riveted cylin-

Table 2—Summary of Fatigue Tests Made on Spars of Annealed Hydronalium 9, 0.039 Inch Thick

Welded (Spot Spacing = 0.39 Inch). Amplitude = 0.63 Inch				Riveted with Hydronalium 7 Rivets 0.102 Inch Diameter Spaced at 1.57 Inches
Unheat Treated		Heated 60 Min. at 330° C. After Welding		Amplitude = 0.63 Inch
Not Corroded	Corroded	Not Corroded	Corroded	Not Corroded / Corroded
200,000	262,000	8,600,000*	6,169,000*	1,208,000 / 1,220,000
350,000	216,000	9,411,000*	4,817,000*	2,720,000 / 1,870,000
	197,000	13,744,000*	4,824,000*	1,940,000 / 520,000
	360,000	8,094,200*	6,798,000*	1,354,000 / 1,361,000

*At 4 million cycles the amplitude was increased to 0.79 inch; at 8 million cycles the amplitude was increased to 0.98 inch.

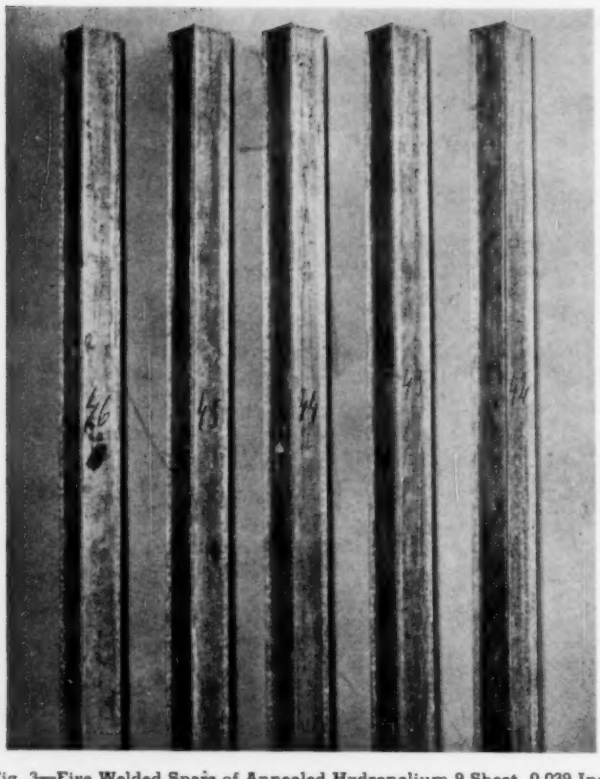


Fig. 3—Five Welded Spars of Annealed Hydronalium 9 Sheet, 0.039 Inch Thick After the Corrosion Test

ders were made with annealed Hydronalium 7 rivets 0.12 inch diameter at a spacing of 0.79 inch. The spot spacing in welded cylinders was 0.39 inch. The welded cylinders were heated 60 min. at 330° C. Three series of tests were made.

Series 1. The cylinders were stiffened by 15 axial stringers 3 inches apart, yielding a ratio of torsional to compressive stress of 0.6.

Series 2. Nine stringers 5 inches apart were used, the ratio of torsional to compressive stress being 0.4.

Table 3—Static Torsion and Compression Tests on Welded and Riveted Cylinders of Annealed Hydronalium 9, 0.032 Inch Thick

Series	1	2	3
Torsional Moment, riveted	73,500	49,000	...
Load at Failure, riveted	11,300	9,600	10,300
Torsional Moment, welded	63,000	45,500	...
Load at Failure, welded	10,900	9,200	8,900

Series 3. Six stringers $7^1/_2$ inches apart were used. These cylinders were tested only in compression.

The results, Table 3, show that the maximum moment and load were slightly higher for the riveted cylinders than for the welded. The heat treatment softened the cold-rolled sheets of which the welded as well as the riveted, cylinders were fabricated. Failure never occurred in the welds; instead, the stringers underwent deformation.

TESTS ON ALCLAD

Five spars were spot welded from Alclad 0.039 inch thick, spot spacing = 0.39 inch. The spars, which were not heat treated, failed after 200,000 cycles at an amplitude of 0.79 inch. Many cracks not visible before the test were observed in the vicinity of the welds after failure. The cracks were attributed to the stresses at the edge of the welds. The Alclad spars could not be heat treated (water quenched from 500° C.) on account of distortion. However, shrinkage cavities were not observed in welds in Alclad that had been heated 60 min. at 330° C. before welding.

Since heat treatment of the entire Alclad spar after welding is impossible, each spot must be heat treated in the welding machine. Heat treatment in the machine is necessary particularly for material 0.039 inch thick and over, in which cavities and edge stresses are more pronounced than in material less than 0.03 inch thick. Tests will be made to determine whether Alclad spars made of materials 0.032 inch thick are superior to spars 0.039 inch thick.

SPOT WELDING ELECTRON (AM 503)

The difficulties in spot-welding Electron are greater for material over 0.032 inch thick than for thinner material. Despite rubbing with emery paper, the thicker material yields poor welds. Besides, the high energy required for welding causes pronounced alloying of Electron with the roller. Difficulties were overcome by heating the sheets previously rubbed with emery paper to 300° C. for 5 to 10 min. prior to welding. The heat-treated sheets welded without difficulty and there was little pick up by the rollers. Single spots in Electron 0.039 inch thick failed in static shear at 124 lb. without prior heating and at 170 lb., if the sheets had been heated before welding. Unannealed Electron 0.039–0.047 inch thick was found to weld more readily, if the roller speed and distance between spot were reduced. Preheating the sheets enabled good welds to be made. With greater spacing between spots the sheets had to be specially annealed as well as preheated before welding.

FUNDAMENTAL RESEARCH IN WELDING

An Outline of the Objectives and Activities of

THE FUNDAMENTAL RESEARCH DIVISION OF THE
ENGINEERING FOUNDATION—WELDING RESEARCH COMMITTEE

The distinguishing thing about progress in welding, as in any other branch of engineering, is that we can always do much more with a given phenomenon, process or material after we have found out as much as we can about its nature. The primary discoveries in welding have been made very largely by men who had a broad grasp of all the scientific knowledge available in their time, and had the facilities to apply that knowledge. They applied the systematic principles of fundamental research, relying on cut-and-try methods only to the extent that their information was deficient. It is the principle of fundamental research to which the members of our Division are committed as a body in their search for new knowledge in welding.

THE welding profession, like any other, is proud of its history. Unlike others, however, we are proud of its briefness. Scholarly gentlemen exploring pyramids or caves, or digging up ancient cities have found no hoardes of welding torches, or water-cooled tips, or covered electrodes. To be sure, however, they have unearthed forge-welded finger rings and fusion-welded lead pipe made many centuries before our time. The rudimentary notion of welding remained little more than rudimentary up to the opening of the modern welding era, whose date may be set at about 1815 at the earliest, when the experiments by W. H. Pepys were made, in which iron wire was arc welded in diamond powder.

In the intervening centuries between the ancients and Joule, Wilde, Slavianov and the rest, the cut-and-try craftsman had ample opportunity to show what he could do to improve his craft. His efforts at improvement were almost entirely without result. Yet we must remember that his efforts to improve his art—some of the forge-welded work of the old craftsmen are show pieces of artistic workmanship in metals—were accompanied by as much forethought and optimism as our own, only to end in failure or fiasco, as, too often, our own still do.

Welding may be viewed from a large number of research angles. In the first place, there are the sources of welding heat: arcs, flames, sparks, electric resistance, induced currents, chemical (thermit) and electrolytic reactions and combinations thereof. Each of these has a scientific history, and each possesses a lively scientific interest at the present time.

Secondly, there are the metals used in welding. Practically all metals and alloys can be welded, bronze welded or soldered. From Al to Zn, from tin (melting point 232° C.) to tungsten (melting point 3400° C.), all may be welded singly or in combinations. The metallurgical factors encountered in welding any one of these metals range from the physical chemistry of melt and slag reactions, through the technology of casting, to the entire problem of the effects of all ranges of temperature on the base metal from the standpoints of structure as well as of strength, elasticity and residual stresses.

Then there are the applications of welding in engineering construction. Problems arise here for the structural and testing engineers—for the former in the evolution of designs utilizing all the possible but still unrealized advantages of welded construction, for the latter in determining what properties are actually inherent in welded construction.

Finally, there are the widespread applications of welding principles, equipment and methods in such processes as flame heat treatment, hard surfacing, flame cutting and metal spraying.

There are few who will gainsay the value of research, whether in welding or in other fields. The value of research to the teacher and student has been admirably outlined by Professor W. M. Wilson. "Advanced students should participate in creative thought, and the research work should be genuine. Simulating research by solving problems that have already been solved by others may develop technique and may be intensely interesting. Moreover, it may develop a love of knowledge. But such work should be supplemented by genuine research, the solution of problems, however small, to which neither the teacher nor any one else knows the answer. Otherwise, the spirit of intellectual adventure will be lacking. The student must experience the groping, the failures, the continued effort without apparent success, the receptive attitude of the conscious mind for suggestive glimpses from the subconscious mind, the alertness for significant phenomena, the consuming interest that will make continuation of the work more important than eating or sleeping, and finally, he must experience the exhilaration of success. All of these go into the training of a creative thinker. For the first time he will experience the satisfaction of collecting facts and arranging data so that their logical organization leads to a definite conclusion independent of any contribution from any one except himself and his teacher. It is a new experience in his intellectual life, and it enlarges his conception of his possibilities.

"Developing creative thinking is expensive teaching, partly because the number of creative thinkers among teachers is small so that their salaries are likely to be greater than that of the average teacher, partly because the ratio of students to teacher is likewise small for this type of teaching, and partly because, to retain his proficiency, the teacher must continue his research work."

In a recent address Dr. Frank B. Jewett said, "Early in my career, as one of his assistants, General J. J. Carty impressed on me the importance of getting facts before forming opinions or drawing conclusions if one wished to obtain a valid and acceptable answer. It was his belief, and one he operated on consistently, that the answer to almost any question was 90 per cent automatically self-evident if one took the trouble to assemble and scrutinize the known or ascertainable facts which bore on it. Any one who was ever involved with him in consideration on some knotty problem will never forget the interminable hours, days and even weeks or months which he devoted to fact-finding. It was soul- and patience-trying and it led into most unexpected places and to most unexpected individuals but it got results that were rarely wrong."

Prepared by H. M. Hobart, Chairman, and G. E. Claussen, Secretary, Fundamental Research Division. The Welding Research Committee is sponsored jointly by the AMERICAN WELDING SOCIETY and the American Institute of Electrical Engineers.

THE FUNDAMENTAL RESEARCH DIVISION

THE *Fundamental Research Division* directs its research efforts toward discovering "reasons why." In the past the Division has supplied engineers with (1) the most accurate information on the behavior and strength of welds and welded structures at low, normal and elevated temperatures; (2) the earliest information on shrinkage stresses secured in this country; (3) the products of scientific research on the questions: what happens during arc welding, flame cutting, spot welding, to name but a few. The Division has not been noted for developments in operating equipment or in methods of welding. In fact, the creation of these divisions was intended by the Committee to protect the individualistic type of researcher from the limitations imposed by a specific industrial objective, and to leave him free to satisfy his instinctive desire to dig deeper into the ultimate nature of the phenomena involved and to understand more of the reason why. Nevertheless, researchers of the Division have been responsible for the only progress worthy of the name in underwater welding and in welding by the proximity effect. Some understanding of the intricacies of copper welding has also been made possible through one of the Division's researchers. The Division has, of course, not been the only source of advance through research, but it has been an important source. It is interesting to read through past issues of THE WELDING JOURNAL and see the opportunities, though few, that our Division researchers have missed.

Although the Fundamental Research Division was formed in 1935 as a division of the Welding Research Committee, its origin is much earlier. Before the Welding Research Committee of Engineering Foundation was organized in 1935, the Division existed as the Fundamental Research Committee of the American Bureau of Welding, which has been dissolved. The American Bureau of Welding, in turn, was formed about 1920 as an outgrowth of the welding committee of the Emergency Fleet Corporation of World War fame. Fundamental research was one of the principal objectives of the American Bureau of Welding, an organization sponsored chiefly by the National Research Council and the AMERICAN WELDING SOCIETY. For example, in 1926 the Bureau declared: "It is perfectly natural that industrial organizations should spend money on the solution of practical problems, because on these solutions depends commercial success. The work on such problems is of necessity in the hands of the industry and generally in a few concerns. On the other hand, fundamental research into the reason why and into the fundamentals of various processes is actually beyond the means of any single company not only as to diversity of talent needed, but also as to money and time."

The statement was followed by the first list of suggested fundamental researches in welding. A literature survey on the strength of structural welds was completed by the Bureau as early as 1927. In 1927–1928 the San Francisco Local Section of the AMERICAN WELDING SOCIETY cooperated with Stanford University in a research directed by Prof. Moser on the strength of welds at elevated temperatures. The need and desirability of fundamental welding research thus grew in step with the progress of welding itself.

It was in October 1928, at the Annual Meeting of the AMERICAN WELDING SOCIETY at Cleveland, that Prof. Moser presented papers covering these San Francisco researches. In the course of the discussion, which appears on pp. 71 to 82 of the December 1928 issue of THE WELDING JOURNAL, Mr. F. M. Farmer (who was chairman of the session) called attention to the fact:

"That the work which is being reported in this paper was initiated and sponsored, I believe, by one of our sections. That kind of an activity I would recommend to the attention of our other sections." Then Mr. S. W. Miller who (as all of us know) devoted his professional life to the interests of welding and of the AMERICAN WELDING SOCIETY, and in whose memory we now have an annual awarding of the Samuel Wylie Miller Memorial Medal which was established by the SOCIETY after Mr. Miller's death in 1929, spoke as follows:

"Several years ago the parent body in New York thought it would be a very nice thing if the various sections throughout the country could be interested in taking up some particular line of research work, practical or theoretical, which might appeal to the local conditions in that particular section. That proposition was put up to all the sections of the SOCIETY, and there is only one of them that did anything, and that is the San Francisco Section. Mr. Laird, who is the Chairman of this Committee, is a man whom I admire greatly for his thoroughness and general all-around ability. I don't think that any of us fully realize the amount of work that has so far been done on this job, and I am sure that we don't realize, because I don't believe the committee themselves do, where it is going to lead. The question of the strength of any metal at high temperatures is extremely complicated and one of the most difficult things to get accurate results on, that has ever been attempted. When it comes to welds, the difficulties are increased many fold by the peculiarities of weld metal. I hope that, when Professor Moser goes back, he can take with him to the San Francisco Section a formal expression of our thanks for the work they have done, and wishing them Godspeed in the future conduct of the investigation. I also hope that Mr. Moser will convey my personal congratulations to Mr. Laird and the members of the Committee, and I extend them to him now, as my personal appreciation of the tremendous value and interest of this investigation to Welding."

Even now, eleven years later, we are finding it to be very uphill work to interest other local groups to emulate the 1928 activity of the San Francisco Section. It still remains almost unique, although the SOCIETY, in various ways and on various occasions, has done much to promote welding research. The remembrances of the accomplishments of a local section serve to strengthen our determination not to relax in our efforts to this end, as we believe that, in addition to our present research activities, it will, in many cases, be of great mutual advantage to enlist local interest and support through the cooperation of the Section.

The Fundamental Research Division consists of researchers who are investigating the fundamentals of welding and such allied processes as oxygen cutting, hard facing and flame heat treatment. The principles underlying the organization and activities of the Division are stated in the MEMORANDUM, pg. 199 of this Supplement. Any researcher who is interested in, or engaged in, research on welding and is prepared to devote to it a reasonable amount of his time and abilities has met the requirements of a researcher of the Division.

Researcher and Division assist each other. The researcher extends the boundaries of knowledge in the scientific and engineering aspects of welding by offering

the results of his research for presentation to the engineering public, preferably and usually in the *Research Supplement* of THE WELDING JOURNAL. The central staff of the Division, in turn, assists the researcher, if desired and practicable, in securing specimens, preparing bibliographies and translations, obtaining expert advice, and in the formulation of desirable research programs. The central staff of the Division maintains direct contact with the researchers not only through correspondence and the annual research conference, but by visits to the research laboratories. About 50 laboratories were visited by the Secretary since the last *Fundamental Research Supplement* was published in February 1938. A list of equipment of the laboratories has been prepared largely on the basis of his visits. In some instances the Division has granted financial assistance to meritorious research.

For the past six years the Division has held a conference at the time of the annual meeting of the AMERICAN WELDING SOCIETY. At this conference the research workers meet with one another and compare their research experiences, and, through correspondence, are helpful to each other after returning to their respective laboratories. The conference also enables them to make acquaintances with many people in the welding industry.

Cooperation with External Agencies.—The research performed by researchers of the Fundamental Research Division often can be facilitated by assistance from agencies external to the Division. Sometimes assistance with materials or specimens may be essential to the success of an investigation. There are many firms and organizations which are impressed with the desirability of assisting fundamental welding research. The Division apportions the task of providing assistance to its researchers as equitably as possible among its cooperators.

One or another of our cooperating corporations, or other organizations, have assisted the Division on many occasions without charge. A single instance of assistance may represent considerable financial outlay. Specific instances of generous cooperation might be mentioned, but it must suffice to say that there are many. Occasionally the Division has been refused, and it has been believed to be necessary for the Division to ask the Welding Research Committee for a grant to accomplish the object at the market price.

Probably it was to be expected that the Division should fail occasionally in these matters. But at the present time the cooperating firms have realized the value of fundamental research activities so that there appears to be no reason to anticipate future misadventures. The main consideration is to prepare a clear statement of needs, so that no one in the firm of whom the request is made, will misunderstand in authorizing the performance of the work. Descriptions and sketches of needed material should be clear and precise, stating dimensions, quantity, finish and other pertinent facts. The request often is made through the firm's representative on the Welding Research Committee.

Many firms maintain an Educational Department with whose personnel the Division aims to become acquainted. The departments seldom refuse any request for cooperation which they consider reasonable and advisable unless there are circumstances beyond their own control. But it is desirable to distribute the burden of these requests reasonably among the cooperating concerns.

These contributions *in kind* are rapidly growing to an aggregate amount representing the equivalent of a large sum. That the mutual efforts of many individuals and organizations in a region can be profitably directed toward fundamental welding research has been demonstrated on several occasions. For example, in a district that always has been known for interest in fundamental welding research, there are several researchers who, drawn together merely by their enthusiasms, are cooperating in their different welding researches, each researcher as keenly interested (informally) in the work of the others as he is in his own. Chiefly by themselves, but with frequent cooperation with several people in neighboring industrial departments of other kinds, and with other facilities, they produce photostats, type reports, secure references to literature, help one another in complicated photography (relating, for example, to photomicrographs and their reproduction), make translations for the benefit of others as well as themselves, and also promptly and liberally share copies of all their material with other researchers, and with cooperating and non-cooperating agencies indiscriminately. There are no rules, regulations or quid pro quo considerations. Each vies with the others in contributing useful things or acts which it is within his power to contribute.

It is believed that the policy guiding the activities of the Fundamental Research Division described above probably approximates reasonably to a realization of some of the hopes entertained by Ambrose Swasey, in 1914, when he provided a large sum of money to create Engineering Foundation "for the furtherance of research in science and in engineering, or for the advancement in any other manner of the profession of engineering and the good of mankind."

LIST OF RESEARCHERS

Their Affiliations and Research Subjects, revised April 1939

Name	Affiliation	Subject of Research
Dr. R. H. Aborn	Research Laboratories, United States Steel Corporation, Kearny, New Jersey	
Prof. D. E. Ackerman	Department of Metallurgical Engineering, Purdue University, Lafayette, Indiana	1. Viscosity and surface tension of welding slag. 2. Resistance welding.
A. Amatulli	Brooklyn Technical High School, Brooklyn, New York	
Prof. N. H. Barnard	Department of Mechanical Engineering, University of Nebraska, Lincoln, Nebraska	

Prof. M. L. Begeman	Department of Mechanical Engineering, University of Texas, Austin, Texas	Hard facing (*a*) Adherence of facing metal. (*b*) Relation between ductility and hardness. (*c*) Hardness as a function of both thickness of deposit and kind of base metal.
Prof. E. Bennett	Department of Electrical Engineering, University of Wisconsin, Madison, Wisconsin	Studies relating to the selective heating of large conducting bodies in strips by utilizing the "proximity effect" to control the distribution of heating currents of the audio frequency range.
Prof. H. Beresford	Department of Agricultural Engineering, University of Idaho, Moscow, Idaho	Application of wear resistant surfaces to agricultural equipment and tools.
Prof. R. K. Bernhard	Department of Mechanics, Pennsylvania State College, State College, Pennsylvania	
Prof. H. M. Boylston	Department of Metallurgy, Case School of Applied Sciences, Cleveland, Ohio	1. Metallurgical investigations on welds. 2. Factors influencing the welding quality of steel.
Prof. J. W. Breneman	Department of Mechanics, Pennsylvania State College State, College, Pennsylvania	Shear tests on butt welds in mild steel bars.
Prof. W. H. Bruckner	Department of Metallurgy, University of Illinois, Urbana, Illinois	Weld quench test.
G. H. Chambers	Foote Mineral Company, Philadelphia, Pennsylvania	Electrode coatings, fluxes and slags.
Prof. D. Clark	Department of Mechanical Engineering, California Institute of Technology, Pasadena, California	Stress-strain relations under tension impact.
Prof. G. M. Cover	Department of Metallurgy, Case School of Applied Sciences, Cleveland, Ohio	
J. J. Crowe	Research and Development Department, Air Reduction Sales Company, Jersey City, New Jersey	Oxygen cutting.
Prof. T. M. Curran	Webb Institute of Naval Architecture, Bronx, New York	
Prof. H. L. Daasch	Department of Mechanical Engineering, Iowa State College, Ames, Iowa	Oxygen cutting.
Dean R. P. Davis, Prof. G. P. Boomsliter and Asst. Prof. C. H. Cather	College of Engineering, West Virginia University, Morgantown, West Virginia	1. Welding mine rails. 2. Ductility of fillet welds.
Prof. A. V. DeForest	Department of Mechanical Engineering, Massachusetts Institute of Technology, Cambridge, Massachusetts	Vibration studies of welded structures.
Prof. D. J. Demorest	Department of Metallurgical Engineering, Ohio State University, Columbus, Ohio	Coatings for welding rods.
Prof. J. P. DenHartog	School of Engineering, Harvard University, Cambridge, Massachusetts	Influence of the welding heat on the structure of the adjacent metal.
Dr. A. DiGiulio	Department of Chemical Engineering, University of Detroit, Detroit, Michigan	
Prof. G. E. Doan	Department of Physical Metallurgy, Lehigh University, Bethlehem, Pennsylvania	1. Causes and conditions of crater formation in arc welding. 2. A study of the basic nature of the arc discharge. 3. The properties of welds made with pure iron in argon and other atmospheres. 4. The use of gamma rays for weld examination. 5. Acceleration of rate of welding by additions to the welding wire.
Prof. W. G. Dow	Department of Electrical Engineering, University of Michigan, Ann Arbor, Michigan	Welding with high frequency currents.
Prof. R. L. Dowdell	Department of Metallurgy, University of Minnesota, Minneapolis, Minnesota	Cold rolling tests for welds.
R. W. Emerson	Chemical and Metallurgical Division, Research Laboratories, Westinghouse Electric & Mfg. Co., East Pittsburgh, Pennsylvania	Metallurgy, heat treating, etc.
Prof. D. S. Eppelsheimer	Engineering Experiment Station, University of New Hampshire, Durham, New Hampshire	
Dr. C. C. Fair	Department of Chemical Engineering, New York University, University Heights, New York	Metallography of alloy steel welds.
Prof. R. L. Farabee	Department of Metallurgy, Purdue University, Lafayette, Indiana	
Prof. M. M. Frocht	Department of Mechanics, Carnegie Institute of Technology, Pittsburgh, Pennsylvania	1. New methods for the determination of principal stresses in two dimensions. 2. Fundamentals of three-dimensional photoelasticity. 3. Further work in stress concentrations.
Prof. S. L. Goodale	Department of Metallurgy, University of Pittsburgh, Pittsburgh, Pennsylvania	1. Annealing of welded joints. 2. Strain annealing of medium carbon steel (0.25 to 0.30%).
Prof. S. H. Graf	School of Engineering, Oregon State Agricultural College, Corvallis, Oregon	1. X-ray diffraction studies on stresses in welds. 2. Heat effects in weld metal.
H. F. Gray	Department of Mechanical Engineering, Massachusetts Institute of Technology, Cambridge, Massachusetts	Spot welding temperatures.
Prof. L. Grover	Department of Applied Mechanics, Kansas State College, Manhattan, Kansas	Effect of rest on fatigue strength of welds.
Prof. P. R. Hall	Department of Industrial Engineering, Pennsylvania State College, State College, Pennsylvania	Welding of cast iron.

Prof. R. H. Harcourt	Department of Mechanical Engineering, Stanford University, Stanford University, California	Flame cutting steel.
Prof. J. C. Hardgrave and H. Granberry	Department of Mechanical Engineering, Texas Technological College, Lubbock, Texas	The grain size problem in the welding of steel.
Dr. R. H. Harrington	Research Laboratory, General Electric Company, Schenectady, New York	Metallurgical aspects of resistance welding electrodes.
Prof. O. H. Henry	Department of Mechanical Engineering, Polytechnic Institute of Brooklyn, Brooklyn, New York	1. Effect of low temperature on the static and dynamic tensile strength of welded joints in some ferrous and non-ferrous metals. 2. Fatigue tests of welded joints at low temperatures.
Prof. W. F. Hess	Department of Metallurgy, Rensselaer Polytechnic Institute, Troy, New York	1. Spot welding. 2. Properties of gas and arc welds of fine and coarse grained steels. 3. Non-destructive testing of welds.
I. T. Hook	American Brass Company, Ansonia, Connecticut	
Prof. S. C. Hollister	Department of Civil Engineering, Cornell University, Ithaca, New York	1. Locked up stresses in welds. 2. Repeated stresses in welded connections.
Prof. T. P. Hughes	Department of Mechanical Engineering, University of Minnesota, Minneapolis, Minnesota	1. Relation of current and voltage to speed of welding. 2. Ductility values as obtained by the tensile and cold bend tests.
Prof. R. E. Jamieson	Department of Civil Engineering, McGill University, Montreal, Canada	Initial stresses caused by welding.
Prof. C. D. Jensen	Department of Civil Engineering, Lehigh University, Bethlehem, Pennsylvania	Welded girders with inclined stiffeners.
W. C. Johnson	Department of Electrical Engineering, Princeton University, Princeton, New Jersey	Spot welding.
Prof. Bruce G. Johnston and H. J. Godfrey	Fritz Engineering Laboratory, Lehigh University, Bethlehem, Pennsylvania	1. Fracture characteristics. 2. Welded building frames. 3. Allowable unit stresses in coated electrode welds.
Prof. J. B. Kommers	Department of Mechanics, University of Wisconsin, Madison, Wisconsin	1. Fatigue tests on notched specimens.
Prof. W. B. Kouwenhoven	School of Engineering, Johns Hopkins University, Baltimore, Maryland	1. Flux penetration tests. 2. Non-destructive tests for welds. 3. A study of contact resistance in resistance welding.
Prof. W. J. Krefeld	Department of Civil Engineering, Columbia University, New York, New York	Welded beam connections.
Dean A. S. Langsdorf	School of Engineering and Architecture, Washington University, St. Louis, Missouri	
Prof. T. R. Lawson	Department of Civil Engineering, Rensselaer Polytechnic Institute, Troy, New York	1. Quantitative studies of the stresses caused by welding operations. 2. Photoelastic studies of stresses in riveted and in welded joints. 3. Impact stresses in welded joints.
Dr. H. H. Lester	Watertown Arsenal, Watertown, Massachusetts	X-ray specifications for castings and welded structures.
Prof. J. Marin	Department of General Engineering, Rutgers University, New Brunswick, New Jersey	1. Deformations and stresses at yield and fracture of materials subjected to bi-axial and tri-axial stresses. 2. Experimental study of tubes loaded to fracture in tension and torsion. 3. Experimental study of materials subjected to torsion and bending fatigue.
Dr. J. W. Miller	Reid-Avery Company, Dundalk, Maryland	
Prof. J. L. Miller and Prof. Arthur H. Carpenter	Armour Institute of Technology, Chicago, Illinois	
Prof. H. F. Moore	Talbot Testing Laboratory, University of Illinois, Urbana, Illinois	Welded railroad rails.
E. H. Mount and H. J. Godfrey	Fritz Engineering Laboratory, Lehigh University, Bethlehem, Pennsylvania	1. Welded reinforcing bars. 2. Rigidity of welded structural connections. 3. All welded Vierendeel trusses.
Dean J. A. Needy	College of Engineering, Ohio Northern University, Ada, Ohio	Welding malleable cast iron to steel.
Prof. J. T. Norton	Department of Mining and Metallurgy, Massachusetts Institute of Technology, Cambridge, Massachusetts	1. Radiographic technic of examining welds. 2. X-ray diffraction method for investigating stresses in welds. 3. X-ray studies of age hardening.
Dr. W. A. Pearl	Department of Mechanical Engineering, Armour Institute of Technology, Chicago, Illinois	1. Corrosion of weld zone of wrought iron, structural steel, and low-alloy, corrosion-resisting steels. 2. Heat affected zone of arc-welded cast iron. 3. Fusion zone of high carbon steel and 18–8. 4. Weld zone of 14 Mn steel.
C. L. Pfeiffer	Western Electric Company, Hawthorne Station, Chicago, Illinois	Resistance welding.
Prof. F. A. Rappolt	Department of Civil Engineering, College of City of New York, New York, New York	Welded beam connections.
Prof. F. H. Rhodes	Department of Chemistry, Cornell University, Ithaca, New York	Chemical and metallurgical reactions in the electric arc.
Prof. R. J. Roark	Department of Mechanics, University of Wisconsin, Madison, Wisconsin	Study of plaster models of welded joints.
Prof. G. Sachs	Department of Metallurgy, Case School of Applied Science, Cleveland, Ohio	Residual stresses and stress relief in welded tubing.
Prof. M. F. Sayre and E. A. Winzenburger	Department of Civil Engineering, Union College, Schenectady, New York	1. Development of methods of testing welds. 2. Study of cracks in welds.

Prof. Gilbert S. Schaller	Department of Mechanical Engineering, University of Washington, Seattle, Washington	1. Development of an arc with a gaseous atmosphere. 2. Oxyacetylene welding of cast iron.
Prof. D. D. Sherrill	Machine Designing Department, Cornell University, Ithaca, New York	
Prof. A. H. Sluss and V. M. Smith	Department of Mechanical Engineering, University of Kansas, Lawrence, Kansas	Fatigue testing of fusion welded butt joints.
Prof. E. D. Soderstrom and W. H. Rice	Department of Industrial Arts, Oklahoma A. & M. College, Stillwater, Oklahoma	
Prof. A. Solakian	New York	Photoelastic studies.
Prof. W. A. Spindler	Department of Metal Processing, University of Michigan, Ann Arbor, Michigan	Welding cast iron.
Dean S. S. Steinberg	College of Engineering, University of Maryland, College Park, Maryland	
Profs. J. R. Stitt and J. A. Foust	Department of Industrial Engineering, Ohio State University, Columbus, Ohio	1. Warpage of mild steel caused by arc welding. 2. Shrinkage of mild steel caused by arc welding. 3. Study of procedures to overcome both warpage and shrinkage in arc welding mild steel. 4. Development of methods of testing welds.
Dr. F. J. Studer	Department of Physics, Union College, Schenectady, New York	Contact resistance in spot welding.
Dr. C. G. Suits	Research Laboratory, General Electric Company, Schenectady, New York	Physics of the arc.
Prof. J. G. Tarboux	Department of Electrical Engineering, University of Tennessee, Knoxville, Tennessee	Magnetic field intensities surrounding arcs.
Dr. Eric Therkelsen and G. J. Pesman	Department of Mechanical Engineering, Montana State College, Bozeman, Montana	Distortion of welded pipe structures.
R. D. Thomas, Jr.	Arcos Corporation, Philadelphia, Pennsylvania	
Malcolm Thomson	Welding Department, General Electric Company, West Lynn, Massachusetts	
Prof. G. E. Thornton	Department of Mechanical Engineering, State College of Washington, Pullman, Washington	Fatigue tests.
Prof. W. T Tiffin	School of Mechanical Engineering, University of Oklahoma, Norman, Oklahoma	Inspection by means of trepanning.
Prof. G. E. Troxell	Engineering Materials Laboratory, University of California, Berkeley, California	
Dr. A. E. Vivell	Department of Electrical Engineering, Princeton University, Princeton, New Jersey	Non-destructive tests.
Prof. N. F. Ward	Department of Mechanical Engineering, University of California, Berkeley, California	1. Creep tests of arc welds in steel. 2. Resistance welding thin-gage duralumin. 3. Stress distribution in welded Vierendeel trusses.
W. L. Warner	Watertown Arsenal, Watertown, Massachusetts	1. Heat effect of arc welding various alloy steels. 2. Effect of preheating on weldability of these steels.
Prof. K. F. Wendt	College of Engineering, University of Wisconsin, Madison, Wisconsin	Tests of low-alloy steels.
Dean A. E. White	Division of Engineering Research, University of Michigan, Ann Arbor, Michigan	1. Creep of metals and welded structures at high temperature. 2. Resistance to oxidation, embrittlement, and fatigue. 3. Stresses in annealed and unannealed welded pipe.
Prof. W. M. Wilson	Department of Civil Engineering, University of Illinois, Urbana, Illinois	Fatigue tests on large welded specimens.
Prof. S. F. Yasines	Department of Civil Engineering, New York University, New York, New York	Notch-impact tests of welds in low-alloy steel.
Dr. M. Yatsevitch	Watertown Arsenal, Watertown, Massachusetts	Coefficient of expansion of weld metal.
Prof. A. P. Young and R. R. Seeber	Department of Mechanical Engineering, Michigan College of Mining and Technology, Houghton, Michigan	1. Copper welding. 2. Methods of testing welds.

SOME MAJOR FIELDS OF FUNDAMENTAL RESEARCH

THE subjects of research in the List of Researchers may be grouped with greater or less precision under fourteen distinct topics which coincide with some of the main engineering aspects of welding. From time to time the researchers may transfer their activity and interest from one topic to another, and additional topics may come to the fore. Not all of the topics of interest to the Division are included in the list. The groups of researchers interested in each of the fourteen topics below, constitute a fertile source of information and ideas. Cooperation and exchange of ideas among researchers of each group are advantageous, but the initiative is usually taken by the researchers themselves. The central staff at Head Office, is, however, always ready to be of assistance in any desired way, to the extent of its power. Some researchers appear in several groups, whereas some, whose research is highly specialized, appear in none. It should be emphasized that (with the exception of the Resistance Welding Section) the researchers in each group have no formal organization whatever. But if they see advantages in establishing more formal groups in particular cases, they presumably will do so in such democratic manner as they deem desirable in each case. The mere fact of the convenient accessibility of this list will ensure frequent and profitable reference to it by the Division's researchers and by others.

(Abbreviations: *A*—research planned. *B*—research in progress. *C*—research completed.)

Group 1. PLAIN CARBON AND LOW-ALLOY STEELS

Mechanical Properties
Prof. R. L. Dowdell (*B*)
Prof. O. H. Henry (*B*)
Prof. T. P. Hughes (*B*)

Hardness
W. L. Warner (*B*)

Grain Size
H. Granberry (*B*)

Grain Structure
R. W. Emerson (*B*)
Dr. F. C. Fair (*C*)
Dr. W. A. Pearl (*A*)

Notch Impact Value
Prof. W. H. Bruckner (*A*)

Mine Rails
Dean R. P. Davis (*B*)
Prof. G. P. Boomsliter (*B*)
Prof. C. H. Cather (*B*)

Miscellaneous
Prof. H. M. Boylston (*C*)
Prof. G. M. Cover (*A*)
Prof. S. H. Graf (*A*)
Prof. K. P. Wendt (*A*)
Dean A. E. White (*B*)

Group 2. FATIGUE

A. Amatulli (*C*), rotating bend
Dr. R. K. Bernhard (*A*), oscillator
Prof. A. V. DeForest (*A*), vibration studies
Prof. L. Grover (*A*), effect of rest
Prof. O. H. Henry (*B*), rotating bend
Prof. S. C. Hollister (*B*), structural connections
Prof. J. B. Kommers (*A*), notched specimens
Prof. Joseph Marin (*A*), combined stress fatigue tests
Prof. H. F. Moore (*B*), rail joints
Prof. A. H. Sluss (*A*), rotating bend
Prof. G. E. Thornton (*A*), rotating bend
Prof. W. M. Wilson (*A*), structural connections

Group 3. OXYGEN CUTTING

J. J. Crowe (*B*)
Prof. H. L. Daasch (*A*)
H. F. Gray (*B*), arc saw
Prof. R. H. Harcourt (*B*), slag analyses

Group 4. SHRINKAGE STRESSES DISTORTION, CRACKING

Measurements of Shrinkage Stresses
Prof. S. C. Hollister (*B*)
Prof. T. R. Lawson (*C*)
Prof. S. L. Goodale (*C*)
Prof. R. E. Jamieson (*A*)
Dean A. E. White (*C*)

Distortion
Prof. Eric Therkelsen (*B*)

Effect of Ductility (Cracks)
Prof. S. H. Graf (*A*)
Prof. Joseph Marin (*B*)
Prof. M. F. Sayre (*B*)
Prof. W. T. Tiffin (*B*)

Stress Relief
Prof. J. P. DenHartog (*C*)
Prof. George Sachs (*A*)

Group 5. STRUCTURAL WELDING

Tests of Welded Structures
Prof. B. G. Johnston (*A*)
Prof. W. J. Krefeld (*C*)
Prof. T. R. Lawson (*C*)
E. H. Mount (*B*)
Prof. R. J. Roark (*C*)

Stress Distribution
Prof. M. M. Frocht (*A*)
Prof. C. D. Jensen (*B*)
Prof. A. Solakian (*C*)

Group 6. PHYSICS OF ARC

Prof. G. E. Doan (*B*)
Prof. F. H. Rhodes (*B*)
Prof. R. D. Thomas, Jr. (*B*)
Prof. J. G. Tarboux (*A*)
Dr. C. G. Suits (*C*)

Group 7. RESISTANCE WELDING

Spot Welding
H. F. Gray (*B*), temperature distribution
Dr. R. H. Harrington (*C*), electrodes
Prof. W. F. Hess (*B*), process variables and theoretical aspects
Prof. W. C. Johnson (*C*), temperature distribution
Prof. W. B. Kouwenhoven (*A*), contact resistance
Dr. F. J. Studer (*B*), contact resistance
Prof. N. F. Ward (*B*), temperature distribution and properties of spot welded tubes

Flash Welding
Dr. M. Yatsevitch (*B*)

High-Frequency Welding
Prof. Edward Bennett (*B*)
Prof. W. G. Dow (*A*)

Group 8. NON-FERROUS METALS

Prof. O. H. Henry (*B*), mechanical properties of welds in aluminum castings
Prof. W. F. Hess (*B*), spot welding copper alloys
I. T. Hook (*B*), copper alloys
Prof. G. E. Thornton (*A*), magnesium
Prof. N. F. Ward (*B*), spot welded duralumin
Prof. A. P. Young (*B*), copper

Group 9. METHODS OF TESTING

Prof. J. W. Breneman (*B*), shear tests
Prof. C. H. Cather (*B*), ductility of fillet welds
Prof. G. E. Doan (*C*), gamma ray tests
Prof. R. L. Dowdell (*B*), cold rolling tests
Prof. W. B. Kouwenhoven (*C*), magnetic testing
Dr. H. H. Lester (*B*), non-destructive tests
Prof. J. T. Norton (*B*), X-ray tests
Prof. M. F. Sayre (*B*), methods of testing
Prof. W. T. Tiffin (*B*), trepanning test
Dr. A. E. Vivell (*C*), magnetic testing

Group 10. CREEP TESTS

Prof. N. F. Ward (*B*)
Dean A. E. White (*B*)

Group 11. COATING AND FLUXES

Prof. D. E. Ackerman (*B*), viscosity
G. H. Chambers (*B*), recovery
Prof. D. J. Demorest (*A*), metallurgy
Dr. J. W. Miller (*A*), analysis

Group 12. IMPACT TESTING

Prof. O. H. Henry (*B*), tensile impact
Prof. T. R. Lawson (*A*), fillet welds
W. L. Warner (*B*), tensile impact
Prof. S. F. Yasines (*C*), Izod

Group 13. HARD FACING

Prof. M. L. Begeman (*B*)
Prof. H. Beresford (*C*)

Group 14. CAST IRON

Prof. P. R. Hall (*B*), recovery of elements from gray iron rods in oxy-acetylene welding
Dean J. A. Needy (*B*), malleable cast iron
Prof. W. A. Pearl (*B*), brazability, preheating
Prof. Gilbert S. Schaller (*B*), arc welding
Prof. W. A. Spindler (*A*), machinable welds

These 14 groups resulted almost entirely from the individual researcher's free choice of his subject. The researcher received advice whenever it was requested, but, nevertheless, it has been nearly always found that the researcher is the one most competent to decide upon subject, scope, method of precedure and other particulars.

Indeed the Division's experience repeatedly confirms the conclusion that the researcher himself should be the one to head up his own research. It should be he who should make practically all decisions right from the start and maintain command of the situation continuously until the completion of the undertaking. Specialists distributed all over the world should constitute his *Committee of Advisers*. Most of these specialists will remain unaware of their membership in his committee, for he consults them most effectively in perusing their literary contributions and work reports.

There are many sources to which the researcher may turn to ascertain the names and records of the leading authorities on the subjects with which he deals in his research work. As only one example, if a researcher consults the 1938 Edition of the *Welding Handbook* and limits his quest to the subject of his research, he can quickly prepare for his own use a list of authorities and of their technical papers and reports.

Another convenient and much more elaborate review of the work already available on his subject is the series of *Literature Reviews* prepared by the Literature Division which has been appearing for over a couple of years in the monthly issues of the Supplement of THE WELDING JOURNAL. These articles, and the bibliographies which they include, are very helpful indeed, and their careful study cannot fail to be profitable to the researcher in any of the various fields with which this series of articles has dealt.

The subjects of the reviews are:

Bend Testing of Welds—A Summary. M. F. Sayre; AMERICAN WELDING SOCIETY JOURNAL, December 1935, pp. 2–10.

Impact Tests of Welded Joints—A Review of the Literature to January 1, 1936. W. Spraragen and G. E. Claussen; AMERICAN WELDING SOCIETY JOURNAL, April 1936, pp. 2–12.

Fatigue Strength of Welded Joints—A Review of the Literature to October 1, 1936. W. Spraragen and G. E. Claussen; *Welding Research Supplement*, January 1937, pp. 1–44.

Welding of Copper and Its Alloys—A Review of the Literature to January 1, 1936. Ira T. Hook; *Welding Research Supplement*, Feb. 1937, pp. 7–32; and March 1937, pp. 33–46.

Welding of Cast Iron—A Review of the Literature to November 1, 1936. W. Spraragen and G. E. Claussen; *Welding Research Supplement*, March 1937, pp. 2–32.

Welding of Cast Steel—A Review of the Literature to November 1, 1936. W. Spraragen and G. E. Claussen; *Welding Research Supplement*, April 1937, pp. 2–11.

Stress Distribution in Fillet Welds—Review of the Literature to January 1, 1937. A. G. Solakian and G. E. Claussen; *Welding Research Supplement*, May 1937, pp. 1–24.

Welding Special Ferrous Alloy Castings—A Review of the Literature to November 1936. W. Spraragen and G. E. Claussen; *Welding Research Supplement*, June 1937, pp. 5–9.

Welding Malleable Cast Iron—A Review of the Literature to December 1, 1936. W. Spraragen and G. E. Claussen; *Welding Research Supplement*, June 1937, pp. 10–14.

Shrinkage Distortion in Welding—A Review of the Literature to January 1, 1937. W. Spraragen and G. E. Claussen; *Welding Research Supplement*, July 1937, pp. 29–39.

Corrosion Resistance of Welded Joints—A Review of the Literature to January 1, 1937. W. Spraragen and G. E. Claussen; *Welding Research Supplement*, August 1937, pp. 1–44.

Temperature Distribution During Welding—A Review of the Literature to January 1, 1937. W. Spraragen and G. E. Claussen; *Welding Research Supplement*, September 1937, pp. 4–10.

Fusion Welding Wrought Iron—A Review of the Literature to January 1, 1937. W. Spraragen and G. E. Claussen; *Welding Research Supplement*, September 1937, pp. 11–22.

The Effect of Total Carbon and Manganese on the Mechanical Properties and Structure of Welded Joints in Plain Low-Carbon Steel—A Review of the Literature to January 1, 1937. W. Spraragen and G. E. Claussen; *Welding Research Supplement*, September 1937, pp. 23–32.

The Heat Effect in Welding—A Review of the Literature to January 1937. W. Bruckner; *Welding Research Supplement*, October 1937, pp. 53–70.

Shrinkage Stresses in Welding—A Review of the Literature to January 1, 1937. W. Spraragen and G. E. Claussen; *Welding Research Supplement*, November 1937, pp. 2–62.

Effect of Nitrogen on the Welding of Steel—A Review of the Literature to July 1937. W. Spraragen and G. E. Claussen; *Welding Research Supplement*, April 1938, pp. 9–30.

Welding of Nickel Steels—A Review of the Literature to July 1937. W. Spraragen and G. E. Claussen; *Welding Research Supplement*, May 1938, pp. 1–17.

Welding Vanadium Steels—A Review of the Literature to July 1, 1937. W. Spraragen and G. E. Claussen; *Welding Research Supplement*, June 1938, pp. 26–30.

Welding Chromium Steels (Up to 10% Cr)—A Review of the Literature to July 1, 1937. W. Spraragen and G. E. Claussen; *Welding Research Supplement*, July 1938, pp. 1–39.

Welding Manganese Steels—A Review of the Literature to July 1, 1937. W. Spraragen and G. E. Claussen; *Welding Research Supplement*, August 1938, pp. 1–22.

Welding Silicon Steels—A Review of the Literature to July 1, 1937. W. Spraragen and G. E. Claussen; *Welding Research Supplement*, September 1938, pp. 1–7.

Impact Tests of Welded Joints II—A Review of the Literature from January 1, 1936 to January 1, 1938. W. Spraragen and G. E. Claussen; *Welding Research Supplement*, September 1938, pp. 8–27.

Welding Molybdenum Steels—A Review of the Literature to July 1, 1937. W. Spraragen and G. E. Claussen; *Welding Research Supplement*, November 1938, pp. 12–21.

The Effect of Oxygen on the Welding of Steel—A Review of the Literature to July 1, 1937. W. Spraragen and G. E. Claussen; *Welding Research Supplement*, January 1939, pp. 1–8.

The Effect of Aluminum on the Welding of Steel—A Review of the Literature to July 1, 1937. W. Spraragen and G. E. Claussen; *Welding Research Supplement*, January 1939, pp. 8–11.

Internal Stresses in Castings. C. W. Briggs; *Welding Research Supplement*, January 1939, pp. 22–27.

Welding Coated Steel—A Review of the Literature to July 1, 1937. W. Spraragen and G. E. Claussen; *Welding Research Supplement*, February 1939, pp. 33–43.

Effect of Sulphur on the Welding of Steel—A Review of the Literature to July 1, 1937. W. Spraragen and G. E. Claussen; *Welding Research Supplement*, February 1939, pp. 44–49.

Welding Chromium and Chromium-Nickel Corrosion and Heat Resisting Steels—A Review of the Literature to July 1, 1937; W. Spraragen and G. E. Claussen: *Welding Research Supplement*, March 1939, pp. 65–107.

The Effect of Phosphorus on the Welding of Steel—A Review of the Literature to July 1, 1937. W. Spraragen and G. E. Claussen *Welding Research Supplement*, April 1939, pp. 123–130.

One researcher of the Division stated, "The question raised by those engaged in research might well be: Where shall the greatest research activity be concentrated for the most rapid advancement in welding? The answer perhaps will be found in a re-digest of the reviews of literature presented in the form of a balance sheet, which would state the problems still challenging research. Often by drawing attention to undesirable factors which have been taken for granted by specialists, we start research in a profitable direction. For example, the rippled surface usually observed on fusion welds is undesirable from the standpoint of stress concentration. It is to be expected that research in the future will overcome rippling. The motivating force behind most researchers is the knowledge that improvement is a result of activity."

The Division is fully convinced that the procedure it recommends is extremely effective (at least in the field of fundamental research). If, in the course of his work, a researcher discovers some phenomenon previously not known to exist, which is of such a nature as to render desirable an immediate change in his program for the research, he can act freely and feels no embarrassment to associates. With a formal committee in consultation with whom he made up his original program, the usual procedure in these circumstances often would necessitate serious delays and would involve needless correspondence and discussion. Thus we have the remarkable consequence that if we do not emphasize planning and if we rely, instead, on the natural progress of evolutionary processes, we shall find the whole field better and more promptly covered and coordinated than by any amount of well-meant planning.

The procedure leads automatically to the undertaking and controlling of the fundamental researches by intelligent and industrious men with conspicuously inquisitive minds, each acting with complete independence in accordance with his own ideas. A research into the fundamentals of a subject is an *exploring* expedition which goes forth to discover, not only mere facts (though they must first be discovered), but also to learn the reasons for those facts and for the phenomena consequent upon them.

The men who have been most successful in fundamental research are usually very strongly individualistic and they rarely relish the subordination of their own ideas which is almost inevitable with committee association. If a committee will remain in the background and be content to service the researcher, when called upon by him, it can be of much use, but it is rarely that the relationship is of this nature. Our Head Office plan of servicing the individualistic researcher is, we believe, an effective method, and provides the needful services for which committees sometimes are established.

A researcher is irked by formalities, he wants to get back on the job. His job will be done best if any cooperation consists in helping him have his own way.

LIST OF PUBLICATIONS

SINCE the last issue of the Fundamental Research Supplement in February 1938, the following reports were published in the *Welding Research Supplement* by researchers of the Division:

Metal Deposition in Arc Welding. G. E. Doan, Associate Professor, Department of Metallurgical Engineering, Lehigh University; *Welding Research Supplement*, January 1938, pp. 15–19.

Laminated Specimens for Non-Destructive Testing. W. B. Kouwenhoven, Professor of Electrical Engineering, Johns Hopkins

University, and A. E. Vivell, Instructor in Electrical Engineering, Princeton University; *Welding Research Supplement*, March 1938, pp. 19–22.

Developments in Welding at Rensselaer Polytechnic Institute. W. F. Hess, Assistant Professor, Department of Metallurgy, Rensselaer Polytechnic Institute; *Welding Research Supplement*, March 1938, pp. 14–18.

Resistance Brazing of the End Rings of Squirrel Cage Rotors. J. H. Lotter, submitted as a Master's Thesis, Department of Mining and Metallurgy, College of Engineering, University of Wisconsin; *Welding Research Supplement*, March 1938, p. 10.

Thermal Study of Arc Welding—Experimental Verification of Theoretical Formulas. D. Rosenthal, Professor, Université Libre de Bruxelles, Brussels, Belgium, and R. Schmerber, Electrical Engineer, S. A. Arcos, Brussels, Belgium; *Welding Research Supplement*, April 1938, pp. 2–8.

Cast Iron, Some Observations on Its Arc Welding—by Gilbert S. Schaller, Professor, Mechanical Engineering, University of Washington; *Welding Research Supplement*, May 1938, pp. 37–40.

Arc Welding Atmospheres. Gilbert E. Doan, Professor, Department of Metallurgical Engineering, Lehigh University, and A. M. Bounds, Engineering Foundation Research Fellow at Lehigh University; *Welding Research Supplement*, June 1938, pp. 1–4.

Welded Beam-Column Connections. T. A. Weyher, abstract from a thesis submitted to Massachusetts Institute of Technology; *Welding Research Supplement*, June 1938, pp. 5–9.

Fatigue Tests of Welds at Elevated Temperatures. A. Amatulli, Instructor, Brooklyn Technical High School, and O. H. Henry, Professor, Department of Mechanical Engineering, Polytechnic Institute of Brooklyn; *Welding Research Supplement*, June 1938, pp. 14–20.

Tensile Impact Tests on Welds at Low Temperatures. O. H. Henry, Professor, Department of Mechanical Engineering, Polytechnic Institute of Brooklyn; *Welding Research Supplement*, August 1938, pp. 23–27.

An Investigation of the Tensile and Torsional Strength of Weld-Metal and Welded Joints. John R. Grandinetti, thesis submitted to the faculty of the Towne Scientific School of the University of Pennsylvania for the degree of Master of Science in Mechanical Engineering; *Welding Research Supplement*, August 1938, pp. 27–29.

Tests of Welds in Alloy Steels—Hardness and Impact Strength of Critical Sections in Welded Steels, by S. F. Yasines, Assistant Professor in Civil Engineering, New York University; and Photography and Metallography of Welds, by F. C. Fair, Instructor in Chemical Engineering, New York University; *Welding Research Supplement*, September 1938, pp. 28–34.

Physical and Metallographical Investigation of Low-Carbon Low-Alloy Welded Steel. Robert C. Boehm, thesis submitted to Case School of Applied Science for the degree of Bachelor of Science in Metallurgical Engineering; *Welding Research Supplement*, September 1938, pp. 34–36.

The Metallurgical Aspects of Resistance Welding Electrodes. R. H. Harrington, Research Metallurgist, General Electric Company, Schenectady; *Welding Research Supplement*, October 1938, pp. 18–22.

Stress Distribution in Welds Subject to Bending. Cyril D. Jensen, Associate Professor of Civil Engineering, Lehigh University, and R. E. Crispen, formerly student at Lehigh University, *Welding Research Supplement*, October 1938, pp. 22–24.

Welding Arcs from the Standpoint of Recent Investigations. C. G. Suits, Research Laboratory, General Electric Company, Schenectady; *Welding Research Supplement*, October 1938, pp. 35–38.

Studies of the Spot Welding of Low Carbon and Stainless Steels. W. F. Hess, Assistant Professor, Department of Metallurgy, Rensselaer Polytechnic Institute, and R. L. Ringer, Research Fellow, Department of Metallurgy, Rensselaer Polytechnic Institute; *Welding Research Supplement*, October 1938, pp. 39–48.

Crater Formation in Arc Welding. G. E. Doan, Professor of Physical Metallurgy, Lehigh University, and S. Young, H. M. Byllesby Research Fellow, Lehigh University; *Welding Research Supplement*, October 1938, pp. 61–67.

Residual Stresses Due to Circumferential Welds in Pipes. E. L. Eriksen and I. A. Wojtaszak, University of Michigan; *Welding Research Supplement*, October 1938, pp. 68–71.

Effect of Eccentricity on the Strength of Welded Joints. William W. Stecker, thesis prepared in the College of Engineering, Department of Civil Engineering, University of California; *Welding Research Supplement*, November 1938, pp. 8–11.

Column Tests of Reinforced Channels. R. J. Roark, Professor of Mechanics, University of Wisconsin, C. B. Voldrich and E. Sollid; *Welding Research Supplement*, November 1938, pp. 31–42.

Stresses in Single Angles. J. Merle, Jr. and W. W. Smith, thesis prepared at Washington University, St. Louis; *Welding Research Supplement*, January 1939, pp. 12–14.

Deflection Tests on the Welded Steel Frame of Lagunita Court. R. A. Hattrup and H. A. Russell, thesis supervised by Professor A. S. Niles in the Department of Civil Engineering, Stanford University; *Welding Research Supplement*, February 1939, pp. 51–55.

Data on the Spot Welding of Low-Carbon Steel. Wendell F. Hess, Associate Professor of Metallurgical Engineering and Head of the Welding Laboratory, Rensselaer Polytechnic Institute, and Robert L. Ringer, Research Fellow, Department of Metallurgical Engineering, Rensselaer Polytechnic Institute; *Welding Research Supplement*, April 1939, pp. 113–119.

The Use of Light Steel Trusses. Leslie W. Graham, Clarkson H. Oglesby and Earle S. Sloan, résumé of a thesis, supervised by Professor A. S. Niles, submitted to the Dept. of Civil Engineering of Stanford University in partial fulfilment of the requirements for the Degree of Engineer, June 1936; *Welding Research Supplement*, April 1939, pp. 119–122.

Using "Controlled" Predistortion to Produce Straight Headers. Gerard Pesman, Institute of Mechanical Engineering, Montana State College, Bozeman, Montana; *Welding Research Supplement*, April 1939, pp. 132–138.

The following tentative titles of forthcoming papers by fundamental researchers are indicative of some of their current interests.

Author	Tentative Title
1. Professor E. Bennett University of Wisconsin	Application of Proximity Effect to Welding
2. G. H. Chambers Foote Mineral Co.	Manganese in Rutile-Type Coated Steel Electrodes
3. Professor G. E. Doan Lehigh University	Arc Welding in Controlled Atmospheres
4. R. W. Emerson Westinghouse Elec. & Mfg. Co.	The Effect of Alloying in Metallic Arc Welding
5. Herman Granberry Texas Technological College	Grain Size Effects in Welding Mild Steel
6. Professor W. F. Hess Rensselaer Polytechnic Institute	Spot Welding Stainless Steel
7. Professor W. B. Kouwenhoven Johns Hopkins University	Research on Resistance Welding (brief report)
8. Dr. Frank J. Studer and Ernest Winzenburger Union College	Contact Resistance
9. R. David Thomas Jr. and Professor F. H. Rhodes Cornell University	Reactions in Arc Welding
10. Professor W. T. Tiffin University of Oklahoma	Properties of Trepanned Welds
11. Professor N. F. Ward University of California	Creep Tests of Arc Welded, Low Carbon Steel
12. Professor A. P. Young Michigan College of Mining and Technology	Welding of Copper

A list of laboratory equipment of each researcher has been prepared so far as possible, and may be obtained upon request.

RECENT RESEARCH NOTES

(BROUGHT TOGETHER FROM CORRESPONDENCE AND PROGRESS REPORTS)

I. E. Bennett, Department of Electrical Engineering, University of Wisconsin, Madison, Wis.: WELDING BY MEANS OF THE PROXIMITY EFFECT (HIGH-FREQUENCY CURRENT). The proximity effect method of heating is a method of selectively heating narrow shallow strips on the surface or the edges of large metallic bodies and of causing the heating current to concentrate in the selected strips.

The large quantities of power at high frequency are supplied by a Poulsen arc in the present experimental

welding arrangement. The edges of mild steel plates 2 inches wide and 1/4 inch thick are heated to welding temperature in 20 seconds. The edges are squeezed together and welded at once. There is a small amount of flash. Tests of the welds demonstrate that their strength is higher than that of unwelded base metal. Basically the process is not restricted to flat plates, but may be used for pipes and other sections.

II. GILBERT S. SCHALLER, Department of Mechanical Engineering, Seattle, Wash.: WELDING RESEARCH ON CAST IRON. In order to follow the practice of electric furnace melting we reasoned that to solve our problem in welding cast iron with the use of a steel rod, we needed to make additions to the arc that would adjust the analysis of the weld metal to that of cast iron. This procedure indicated the addition of graphite and some dependable graphitizing agent. Accordingly, we experimented with electrode coatings until we found that a coating consisting of pure graphite mixed with sodium silicate as a binder seemed to offer results.

Our next endeavors consisted of using a heavier shielding on the electrodes. However, we found very little, if any, change in the weld metal. It was felt then, that the solution of our problem lay in the direction of increasing the amount of coating with respect to the cross-sectional area of the welding electrode. Apparently this meant the investigation of some electrode shape other than the commonly known circular one.

It is our intention to pursue this investigation further.

III. FRANK J. STUDER, Department of Physics, Union College, Schenectady, N. Y.: CONTACT RESISTANCE STUDY. In the Physics Laboratory at Union College we have begun, during the last summer, a study of the nature of contact resistance between surfaces pressed into contact, and to gather data on the factors which influence it, both from the literature and from some experiments of our own. Our preliminary experiments have been with 18–8 stainless steel surfaces, investigating: (*a*) variation of surface resistance with method of preparation of surface, and subsequent treatment; (*b*) contact resistance as a function of pressure; (*c*) contact resistance as a function of temperature. Stainless steel was chosen for this work, with a view to collaborating with Prof. W. F. Hess and his associates at Rensselaer Polytechnic Institute, who have been making measurements of contact resistance of 18–8 steel during the actual welding cycle, and who were particularly interested in some experiments to confirm their measurements showing the change of contact resistance with temperature.

IV. W. H. BRUCKNER, Department of Metallurgy, University of Illinois, Urbana, Ill.: THE WELD-QUENCH TEST. The investigation which we may be able to undertake at the University of Illinois is a continuation of the research carried out at the Naval Research Laboratory by the writer. The previous research has been published this year in A. S. T. M. 1938 preprint No. 36, entitled: "The Use of the Charpy Test as a Method of Evaluating Toughness Adjacent to Welds."

A series of steels with varied chemical contents were welded with a single welding bead placed on the surface of 1/2-in. thick flat plates of the steels. The heat-disturbed area adjacent to the weld deposit was surveyed for hardness, grain size and microstructure. The area of microscopic extent where maximum hardness and grain size occurred adjacent to the single-bead weld was physically magnified by synthesizing the microstructure by means of a "weld-quench" heat treatment. The latter treatment produces in a half-size Charpy bar practically the same maximum hardness, grain size and microstructure as is found adjacent to the single-bead welds.

The "weld-quench" test is successful in separating welds into two categories: (1) steels insensitive to the welding quench and therefore easily welded without preheat or other special handling, and (2) steels sensitive to the welding quench such that special care, preheat or preheat and subsequent heat treatment, are required for successful welding.

The product of the previous work may be summarized by saying that it was found that a "weld-quench" test made on small samples of welding steels indicated that steels suitable for easy weldability with heavily coated electrodes could be picked out by means of the test. There is an exceptional opportunity for determining the usefulness of the "weld-quench" test in conjunction with Professor W. M. Wilson's "Large Scale Fatigue Tests on Welded Joints." In making the "weld-quench" test on the parent steels used by Professor Wilson for fatigue testing, it will be possible to determine whether the weldability and service behavior can be predicted from the results of the test. The investigation could also be extended to include the low-alloy, high-tensile steels in order that a method of classifying their comparative weldabilities may be available.

V. D. E. ACKERMAN, Department of Chemical and Metallurgical Engineering, Purdue University, Lafayette, Ind.: WELDING RESEARCH AT PURDUE UNIVERSITY. For many years it has been known that the quality of many types of welds is due in large part to the high-temperature properties of the welding slags used, or formed. For example, such physical properties as melting point, surface tension and viscosity determine whether the drops of molten metal leaving the rod will be covered with a thin film of slag and whether, upon deposition in the weld, this slag can escape from beneath the still-fluid bead.

In spite of the importance of these factors, quantitative data on the physical properties of welding slags are almost non-existent. The reason is not far to seek—the necessary experiments are expensive in time and equipment, require considerable outlay for supplies and necessitate an exacting technique under temperature conditions which are difficult to maintain with the requisite accuracy. It is thought, however, that the problem is of sufficient importance to warrent an attempt at its solution.

Initially, effort will be directed toward the measurement of the viscosity, as a function of temperature, of one slag which can be considered as the basis for several of the currently used welding rod coatings. Viscosity is selected for primary consideration because its measurement is slightly less difficult than that of surface tension, yet the high temperature technique developed can be applied, in many respects, to the surface tension work which will be done later. After perfecting the technique with one coating, and checking against standard glasses, the composition of the base coating will be systematically varied within, or somewhat beyond, the ranges of commercial interest; other base coatings will be taken up in turn.

After a search of the literature on the subject of glass and slag viscosity determination, it was decided to use the rotating cylinder viscometer, the outer cylinder, or crucible, to be rotated. A smaller cylinder is suspended from a thin wire and allowed to hang in the center of the crucible. The torque exerted on this cylinder by the fluid flux in the crucible is measured by the torsional deflection of the wire. From the torque value the vis-

cosity of the flux can be calculated, if speed and the dimensions of the various parts are accurately known. A molybdenum wound resistance furnace, designed for operation up to 1600° C., will be used to melt the fluxes and maintain a constant temperature around the crucible during tests.

Materials for the framework have been received and the assembly of this portion of the apparatus has begun. The refractory materials for construction of the furnace as well as the molybdenum resistance wire have been ordered, as have parts for driving the rotating crucible.

VI. M. L. BEGEMAN, Department of Mechanical Engineering, University of Texas, Austin, Tex.: HARD FACING. Our plans for hard facing research are based upon several ideas: First, the idea that adherence is a prime factor in many hard facing applications and should be reduced to quantitative terms. Second, the ductility of the facing and heat-affected zone as a function of the hardness is often an important consideration affecting this chipping and pitting of hard faced parts. Third, as hardness is a desirable property of facing materials it should be obtained as a function of both the thickness of deposit and the kind of base metal used.

At the start, we plan to keep the base material constant and vary the weld material, using successively low, medium and high carbon-steel rod. The specimens will be $^3/_8$ in. × 2 in. of S. A. E. 1020 O. H. steel. The thickness of the specimens will be varied with each series of tests. Specimens will be prepared first with a single bead down the center, followed by specimens with a series of beads about 1 in. wide. Hardness numbers will be taken on each specimen and the microstructure will be observed for each type of specimen. The specimens will be given the bend test, and peeling, cracking or other defects noted. Following these tests, similar tests will be made using alloy steel deposits. The hardness of the base material may then be changed in progressive steps by increasing the carbon content, by heat treatment and by using alloy steels.

The success of these tests will depend largely on having the approximate analysis of the welding rod and base metal. Such information is difficult to obtain from manufacturers, but without it any correlation of results will be next to impossible. It is hoped that some cooperation may be obtained from the manufacturers of this material.

Another question that has not been definitely determined is the procedure for conducting the bend tests. One method is to have the specimen in a horizontal position resting on two roller supports with the facing material in tension. A load is applied midway between supports and continued until failure is noted. The other method, free-bend test, is to bend the two ends at an angle of 30° with the center portion, and then test the specimen in compression in a vertical position, the facing being on the tension side. The latter method is being given most consideration. A preliminary report on the tests is nearing completion.

VII. W. F. HESS, Department of Metallurgical Engineering, Rensselaer Polytechnic Institute, Troy, N. Y.: RESISTANCE WELDING RESEARCH. We have accomplished successfully the application of procedure and techniques outlined in our advance program, and have proved them to be capable of providing a wealth of fundamental data. The amount of basic information which we can furnish during the coming year will be limited only by the resources of men and materials which are placed at our disposal.

The advance program of this research was published in the March 1938 Supplement of THE WELDING JOURNAL. The papers on the "Studies of the Spot Welding of Low-Carbon Stainless Steels," and on "Data on the Spot Welding of Low-Carbon Steel" which were published in THE WELDING JOURNAL for October 1938 and April 1939 should also be considered a part of this report.

We are cooperating with other investigators in similar fields through conferences and the interchange of materials and information. This will make possible the correlation of various phases of our program with the work of others.

Other investigations in the field of resistance welding which will be undertaken as graduation theses, are:

1. Fundamental studies in the flash welding of plain carbon steel.
2. Studies of various methods of measuring current used in spot welding.

Future programs will naturally extend the use of the facilities available and the techniques developed to:

1. Problems in the spot welding of different metals and alloys.
2. Projection welding.
3. Seam welding.
4. Flash welding.

VIII. JOSEPH MARIN, Department of General Engineering, Rutgers University, New Brunswick, N. J.: COMBINED STRESSES IN WELDING. The research program on combined stresses will be considered in three parts:

1. A Study of Deformations and Stresses in the Case of Combined Loadings. This study will involve theoretical analysis of the plastic deformations in the case of combined stresses for both the two- and three-dimensional stress problem. In addition, the program calls for static combined stress tests in torsion and tension on thin tubes. In these experiments, data will be obtained on the plastic deformations up to failure, as well as the determination of the condition of yielding. This work might be extended to a study of various types of welds on tubular specimens subjected to a combined tension and torsion load.

2. A Study of Failure Under Combined Fatigue Stresses. This study will be made on specimens subjected to combined torsion and bending fatigue stresses on the new National Physical Laboratory machine. In these tests a study can be made of various types of welds subjected to combined fatigue stresses. Analytical studies will accompany the tests.

3. Applications of the Above Experiments to Theoretical Studies of Engineering Design. With data obtained on the above two studies it would then be possible to apply the theories derived to the solution of actual problems.

IX. HERMAN GRANBERRY, Texas Technological College, Lubbock, Tex.: THE GRAIN SIZE PROBLEM IN WELDING. This research is concerned with the effect of electrode flux composition upon the grain size in single-pass mild steel welds. The present-day fluxes used on mild steel electrodes may be classed under either one of two types, namely: (1) the gas shielded type containing large amounts of volatile matter; or (2) the slag shielded type containing large amounts of oxides or other slag-forming constituents.

This research consists of a number of somewhat preliminary observations of the "inherent" grain size of weld metal (*a*) as welded, and (*b*) after being subjected to the McQuaid-Ehn test. In this way the temperature at which grain growth commences in weld metal may be determined from which conclusions may be reached on the deoxidation of weld metal.

The analysis and type of steel, as well as electrodes, are known, and the speed of welding, diameter of electrode, current, number of passes, length of pass, etc., have been noted.

Differences between the steels and electrodes have been found and will be discussed in a research report.

X. GILBERT E. DOAN, Department of Metallurgical Engineering, Lehigh University, Bethlehem, Pa.: CRATER FORMATION IN ARC WELDING. We are continuing our work on the causes of crater formation in arc welding in order to determine with certainty whether a lowered surface tension of the liquid metal is the factor actually responsible for crater formation. We have a very promising graduate student on this problem, who is subsidized by one of the Gotshall Scholarships at Lehigh University. No crater appears in welds made in pure chlorine.

Later on I expect to have a report on the sensitivity of the gamma rays for detecting very small flaws, such as hair-line cracks in welds, this report to cover certain phases of the field not included in our last report to A. S. T. M.

XI. CYRIL D. JENSEN, Department of Civil Engineering, Lehigh University, Bethlehem, Pa.: (1) STRESS DISTRIBUTION IN WELDS SUBJECTED TO BENDING AND (2) WELDED GIRDERS WITH INCLINED STIFFENERS. The first of these two subjects was completed and presented as a paper in Detroit last October. The second subject is in progress, a progress report having been presented at Atlantic City in October 1937 under joint authorship with William Lotz.

Lehigh University has given me a Fellow, Charles Antoni, for the more active prosecution of this second research, with the result that wonderful progress is being made. Tests have been made on 15 girders and two trusses.

The evening conference of the Fundamental Research Division is definitely worth while. I believe you should make it a practice to keep a memorandum of the speakers at this conference and insure rotation from year to year. I firmly believe we should make a drive in our division to have speakers present their papers to the audience by speaking to them directly, and not by reading word for word from a paper.

In regard to the technical sessions, I should like to see a better grouping of the papers. For instance, the audience that listened to my paper on "Stress Distribution in Welds Subjected to Bending," listened in turn to papers on "Photoelasticity," "Metallurgical Aspects of Resistance Welding," "Crater Formation" and "Creep Tests." I think it is safe to say that the papers involving metallurgy could well be grouped, as could the papers involving stresses in welds and in joints. Other groupings are likewise apparent.

XII. CARL H. CATHER, College of Engineering, West Virginia University, Morgantown, W. Va.: WELDED MINE RAILS. Professor Boomsliter and I are working on the problem of welding mine rails, but have not completed the tests. We have all the material that various cooperators have sent to us and we have tested 17 welded rails. An additional dozen will be tested later. Photomicrographs are being made, and a fatigue machine for welded mine rails is being designed.

XIII. WILLIAM A SPINDLER, Department of Metal Processing, University of Michigan, Ann Arbor, Mich.: ARC WELDING CAST IRON. The object of my research at the present time is to produce a machinable weld in gray cast iron without preheating, and at a low cost. The factor of weld strength is not of great importance in certain applications relating to the filling of shrinkage cavities in iron castings weighing upward of five to twenty tons, and preheating is difficult and costly.

Arc welding appears to be necessary to keep the localized heat to a minimum, and to maintain low cost. Available non-ferrous electrodes while satisfactory are expensive. When using ferrous electrodes and arc welding without preheat, the machinability of the fusion line, which cools rapidly and is high in carbon, becomes the focal point of the process.

A metallographic study is being made of the hard zone of beads deposited on gray iron plates with a variety of electrodes. The compositions of the electrodes include cast iron, steel, and non-ferrous metals. A flux coating is necessary on these electrodes and some attention is being given to the compositions of these coatings.

At the present time, work is being done to determine the possibility of graphitizing the hard zone with material added to the coating of a cast-iron electrode. Calcium silicide is considered to be one of the more effective graphitizers and an attempt was made to incorporate it into a coating. It was found, however, that, when mixed with sodium silicate, calcium silicide is unstable and that a chemical reaction occurs which so far has prevented its use. Further studies will be made with this material and other graphitizers reported in the literature. It is hoped that some useful information may be obtained on graphitization and machinability.

XIV. R. H. HARRINGTON, General Electric Company, Schenectady, N. Y.: RESISTANCE WELDING ELECTRODES. We are continuing our research, one phase of which is to develop new alloys for resistance welding electrodes. The results have not yet reached the report stage.

XV. GERARD PESMAN, Department of Mechanical Engineering, Montana State College, Bozeman, Mont.: DISTORTION IN WELDED PIPE HEADERS. A report on a "gap-gage" method of eliminating distortion in welded pipe headers has been issued. A simple method for determining gap-gage constants has been developed recently that facilitates application of the method.

XVI. A. P. YOUNG, Michigan College of Mining and Technology, Houghton, Mich.: THE WELDING OF COPPER. The research project is at the stage of compiling results for a report. Further welding will be done when, and if, the results are not consistent with previous welds and tests. A study will be made of the microstructure of welds and base metal.

XVII. N. F. WARD, Department of Mechanical Engineering, University of California, Berkeley, Calif. CREEP TESTS AND STRESS DISTRIBUTION. A final report has been prepared on creep tests of arc-welded low-carbon steel. Another report is nearing completion on the distribution of stress in loaded Vierendeel trusses. The U. S. Patent Gazette and Chemical Abstracts have been searched for patents on electrodes and electrode coatings for ferrous and non-ferrous metals. Approximately 350 patents have been listed.

XVIII. GORDON H. CHAMBERS, Foote Mineral Company, Philadelphia, Pa.: ELECTRODE COATINGS. Our laboratory has been working on three major welding problems. One of these is the relative efficiency of Rutile from mines in various parts of the world. Also, the effect of the arc and welding characteristics of various elements added to the Rutile has been studied.

A second problem is the relative efficiency of Manganox (Mn_3O_4) in comparison with ferromanganese, and other manganese products, in welding rod coatings. The third problem is the testing of various substitutes for the present expensive type of alpha cellulose used in electrode coatings.

REGIONAL SUBDIVISION OF FUNDAMENTAL WELDING RESEARCH

Introductory Note: The following description of a plan for regional subdivision of fundamental research in welding is inspired by a long-felt realization on the part of researchers of the Fundamental Research Division and of research-minded members of Local Sections of the AMERICAN WELDING SOCIETY, of the benefits to be derived in their particular research undertakings from closer local acquaintanceship and cooperation. As an initial effort and to serve the purposes of explanation, thirteen such regional subdivisions are proposed. Appropriate procedures for informal cooperation are outlined in a preliminary way in the hope that valuable additional suggestions will be forthcoming.

RESEARCH and Specialization are related terms. Nearly always the researcher in science or engineering is a specialist, for example, in some department of chemistry, physics, metallurgy, etc., or in some engineering field. He is expert in some kind of scientific or engineering technique, or thought, and in the course of his professional activity he has undertaken a project in which it appears to him that welding may be a helpful tool or component. Not yet a specialist in welding, his ideas as to its most advantageous embodiment in his plans are rather general. Largely owing to the rapid advent of wide-scale employment of welding, there are still many leading specialists in other engineering and scientific fields who have had no opportunity or time as yet, to acquaint themselves more than in a very general way with welding possibilities and welding procedures. Even in welding circles, some know much about arc welding and little or nothing about gas welding, and vice versa. There are many veterans in gas- or arc-welding applications who have had no occasion to acquaint themselves with the various kinds of resistance welding. There, again, the converse statement also holds.

For many applications, materials and types of joint, no one yet definitely knows whether the ultimate best process is to use resistance welding, gas welding, or arc welding; or which subdivision of each of these broad classifications as regards economies and the attainable quality. There are still other valuable welding processes which have not been mentioned in this statement.

It is evident that no individual, however highly experienced, is yet in a position to guide others as to the best course to be pursued in any particular case.

So the researcher who, while leading in his own field, realizes that he must rely on the advice and experience of others until he has acquired more experience with welding, will be very glad of informal occasions to explain his proposals, not to any one but to a welding group with widely varying individual backgrounds.

This specialist who, admittedly, has never been in sufficiently close touch with welding but who has heard enough to be convinced that it is a tool of great value in many applications will welcome having good opportunities to see welding done, and to talk with welding people about the present position of welding in general, its future and its problems.

Unless he has available a great amount of time, he cannot learn from welding books and welding papers nearly all he needs to know right away. Indeed, there remain great gaps in the integrated published knowledge of welding, and such complete contradictions in the experiences and opinions of various people who are nevertheless rightfully regarded as the best informed on the subject of welding, that our hypothetical specialist in another field would acquire increasing doubts as to his best course, the more he studied the literature. To a great extent, this will also be the case as the results of his discussions with an informal local group. But with a knowledge of the nature of his research project, his local confreres will give him the advantage of their discussions with one another and with him, and ultimately he will determine upon one or a few courses desirable for his particular project.

We may assume a continuance of this informal group cooperation and that the preparations and preliminary tests and the results will be learned with interest as they materialize. But it is the man *seeking* the advice who should remain the final judge at every step, and do the deciding. For the thing at stake is the success of *his* project, and his informal local advisers are *serving*, and are, in return, themselves learning much in the process.

The statement of the problem is the first step in the production of a welding research report. Some welding procedures are of long standing and are fairly common knowledge. On the other hand, some others are not. Each industry has its own peculiar welding problems, of which only that industry's own experts are fully aware. Sometimes it is difficult to formulate the statement of the research problem to be solved. It is believed, however, that the persevering pursuit of a subject, such, for example, as plastic flow in metals, or contact resistance, may uncover facts having much significance in working out the particulars of the desired welding feature. In such cases as indicated above, the welding researcher generally benefits from association with welding experts, and the welding plant, in order to acquire the background necessary to the success of his undertaking, and also to enable him intelligently to visualize the position which his efforts occupy. Although the researcher has his laboratory, he is likely to go astray on matters, which are of much more consequence than would appear at first, unless he senses the environment in which he is working.

A decision having been reached about the problem or the direction the research should take, the researcher will inquire whether he can secure materials that have been welded according to best practice, or materials that he may use for welding. He will also need welding literature.

When his initial needs have been met, the researcher sets to work collecting evidence, experimenting, and generalizing. The advice and encouragement of his associates in various fields will not be wasted during this stage of the research and throughout its development. The final stage is the research report and its discussion.

At all stages of its progress to a successful consummation, the research project matures faster and more soundly if the researcher has the benefit of willing assistance, discussion and encouragement. The Fundamental Research Division is hoping to secure these three essentialsof informal assistance, discussion and encouragement,

for as many of its researchers as possible through this plan of regional subdivision, involving the cooperation of research-minded members of Local Sections of the AMERICAN WELDING SOCIETY. The plan is intended to supplement similar activities of the Division, such as its annual conference, its list of suggested research problems, its list of the Division's researchers, its literature references, and its translation service.

The following regional research groups are suggested as a start for the plan:

1. Baltimore
   - Johns Hopkins University
2. Boston—Cambridge—Lynn—Watertown
   - Harvard University
   - Watertown Arsenal
   - Massachusetts Institute of Technology
3. Cleveland
   - Case School of Applied Science
4. Columbus
   - Ohio State University
   - Battelle Memorial Institute
5. Detroit—Ann Arbor
   - University of Michigan
   - University of Detroit
6. Milwaukee—Madison—Chicago
   - Armour Institute of Technology
   - University of Wisconsin
7. Minneapolis
   - University of Minnesota
8. Mohawk Valley
   - Union College
   - Schenectady Works of General Electric Company
   - Rensselaer Polytechnic Institute
9. New York City and vicinity
   - Rutgers University
   - Columbia University
   - New York University
   - Webb Institute
   - Polytechnic Institute of Brooklyn
10. Oklahoma City—Norman
    - University of Oklahoma
11. Philadelphia and vicinity
    - University of Pennsylvania
    - Princeton University
12. Pittsburgh
    - University of Pittsburgh
    - Carnegie Institute of Technology
    - U. S. Bureau of Mines
13. San Francisco
    - University of California
    - Stanford University

The success of these local research groups, which are based upon prominent scientific and engineering research laboratories in leading educational institutions, will depend upon liberal cooperation of individuals and firms in the vicinity represented by the group. Cooperation among the institutions in the list exists now through the medium of the Fundamental Research Division. The proposed regional plan is expected to increase the cooperation of firms interested in welding with the laboratories in their neighborhood who are interested in welding research.

The firms can cooperate by visiting the research laboratories of the educational institutions, by assisting in the formulation of research programs, by supplying specimens, by lending equipment or welders, and by exhibiting an interest in discussing welding research investigations that are being conducted in laboratories of nearby educational institutions. It is emphasized that the thirteen groups are merely suggestions. It may turn out that other localities are in a position to function as cooperative groups, and that some of the suggested groups are not ready for the plan.

It is *not* planned to organize these proposed informal groups except in a loose way. The plan is rather to acquaint the Local Section with the researchers, and to acquaint both with the desirability and advantages of *informal* cooperation. Laboratory researchers, while regarded by most of us as the leaders in scientific achievement, at the same time are (unjustly in many cases), held rather lightly as none too practical. On the other hand, the researchers are said by some to be too often prone to hold themselves somewhat aloof from the rest of the world.

Acquaintance between Researchers and Section may be fostered in three ways.

1. Visits (*a*) by researchers to local plants using welding; (*b*) by Section members to local laboratories engaged in, or interested in, welding research.

2. Research evenings—occasional informal meetings of the Local Section (or component groups) may be devoted to discussions, or statements, of problems encountered by local welding experts, the meetings to be open to all interested local researchers irrespective of their professional affiliations. Possibly on such occasions one or more ambitious young research engineers in the district might address the meetings briefly on the subject of their research results, or on the research subjects which they would like to conduct. Older members might outline the trend which they believe research in the district ought to follow.

3. Seminars—university seminars, at which welding researches are discussed, may be attended by welding experts of the Local Section.

In addition to drawing the Section and the researchers closer together, the plan of regional grouping emphasizes the desirability of cooperation. Perhaps the researcher needs materials or assistance which the Section may be able to provide, enthusiasm for research having been known to open the way for unexpected generosity. From the opposite viewpoint there may be those in the Local Section who are harassed by technical problems in the solution of which the researchers may welcome the opportunity to assist with suggestions from their individual experiences.

In each of the thirteen regional subdivisions, the two groups, Section and researchers, are in existence and are decidedly active. The need for cooperation and the means by which cooperation may be effected remain to be pointed out. Convinced of the value of the plan of regional subdivision it is up to the research-minded members of the Local Sections, and the researchers themselves, to become better acquainted. While in the above statement it has been attempted to make helpful suggestions, it is believed that the variety of conditions in each case will indicate the advantage of individual solutions. It is, however, above all things urged that informality should be maintained and assurance of individualistic independence.

In every Local Section there are young men eager to play a part. Some of the most promising of these young men occupy positions which are not consistent with research in laboratories, but among them are some who, nevertheless, look forward to the time when they may be thus engaged. Prior to that time, they can make very helpful contributions by library research in *fundamental* subjects. In this way, the older and more experienced men in the Local Section will find their own research efforts greatly helped. These older men in the group can contribute by suggesting subjects and programs, and

helping with advice in such library research projects. In due course, a young member can embody his results (which may even attain to being *discoveries*) in a paper for presentation at a Local Section meeting. Such further developments as occasional prizes for the best papers contributed in this way, naturally would follow. The library, as a tool, is much more widely available than are laboratory facilities, and frequently the desired facts and new conclusions are reached with far less outlays of time and money than by laboratory efforts. Our Division has repeatedly found it advantageous to make a fairly elaborate preliminary research in libraries (when a project is undertaken) and to base thereupon recommendations, or programs, for further research on that project in the laboratory.

It can reasonably be expected that the local manufacturing concerns will, on occasions, call attention to projects which they consider important. Consequently the group should include forward-looking officials of such local concerns.

In former times, manufacturers inclined to the belief that great advantage accrued to having *exclusive* control of the results of their researches and this tended to prejudice them against any projects for cooperative research. Gradually this extreme stress on exclusiveness has yielded to a disposition, in the interests of the general good, to permit, in many instances, of the publication of their research results. By cooperation of the broad kind indicated, a greater amount of new knowledge becomes available to ALL the cooperators. The trend is strongly in that direction and will be accelerated by the addition of informal Local Section projects. While they will find advantages in availing themselves of the head office facilities of the Fundamental Research Division, it is probable that a large degree of autonomy will be maintained. By the strength thus secured from local interest and local cooperation on the one hand, and from head office facilities on the other hand, there is double assurance of providing ways and means in any research endeavor.

The Division does not propose, or wish, to encroach in any way on the already existing and admittedly excellent Local Section activities, but makes these suggestions as likely to be supplementary and desirable in certain cases. After making the suggestions available at this time, it believes that it will be preferable to await local initiative. In any case, it is extremely desirable that each local project should be worked out in whatever way is most suitable for the conditions in the particular region. Presumably this will lead to a great diversity between the plans and procedures determined upon by the local people.

Welding is of much interest in so many fields of engineering, that presumably there will be cases where the Local Sections of several engineering societies will be interested to be interconnected in the plan in some way, and this wider interest generally would be helpful. Enterprises of this sort usually develop best if natural tendencies are followed, and if no attempt is made to conform to a general pattern.

Our head office staff is as much a *Technical Information Bureau* as it is a *Research Head Office*. It is differentiated from other servicing bodies chiefly in that it specializes in *Welding*. If it could be arranged for it to also serve other organizations in this way, its thereby increased usefulness might justify the maintenance of a gradually larger staff and the provision of proportionately more effective service. The experience of the Division has demonstrated that the usefulness of its activities is limited only by the number of able and industrious men who can be employed from the available resources.

MEMORANDUM ON THE FUNDAMENTAL RESEARCH DIVISION OF ENGINEERING FOUNDATION'S WELDING RESEARCH COMMITTEE DEALING WITH ITS UNDERLYING PHILOSOPHY

EXCELLENT work in fundamental welding research is now being conducted at many universities, in the laboratories of some manufacturers and in certain Government departments. It is, however, readily understood that manufacturers and users of welded products and welding processes should, and do, direct the greater part of their research efforts to solving the problems unexpectedly arising each day in the course of their activities in their various uses of welding. Nevertheless, it is gratifying to be able to report a steadily growing recognition by users and manufacturers of the predominating importance of establishing the fundamental principles and continuing a research until the "reason why" has been ascertained or more nearly approached.

Notwithstanding these improving tendencies, it still is all too usual in a manufacturer's laboratory, to discontinue a research as soon as the immediate objective has been attained; the immediate objective being to find a good way of overcoming some particular difficulty which has been encountered in the course of the development, or manufacture, of the product. Frequently the expenditure of a further 10 or 20 per cent would enable a manufacturer to learn the "reason why" of the discovered "good way" of overcoming the difficulty and avoid the waste of beginning all over again the next time he encounters some diffculty differing in some respects from the preceding case as regards materials or procedure.

Scope and Purpose.—In an ideal research laboratory the objective of a research is to ascertain fundamental principles. This is synonymous with increasing our basic knowledge of the subject. Although at present the ideal conditions and attitudes are more often encountered in university laboratories than elsewhere, nevertheless the trend toward recognition of the importance of learning the underlying fundamental principles is increasing steadily in the laboratories of manufacturers.

The manufacturer (or user) who has become convinced of the value of fundamental research thus has two courses: (1) to encourage and support fundamental research in university laboratories, and (2) to carry on fundamental research in his own research laboratory and not be content with simply obtaining from his researches just enough empirical knowledge to be able to continue manufacturing a product until some new difficulty arises or until competition forces him to ascertain ways of improving the product or of reducing the cost. At present, many manufacturers and users are employing both methods. That is to say, (1) they are helping the university researchers with materials, advice and financial support, and (2) they are to a greater extent each year planning the researches in their own laboratories, so as to ensure that they will be continued toward

the acquirement of knowledge of the "reason why." And these manufacturers and users are finding that fundamental research is amply justified by the results secured.

Furthermore, it is a satisfaction to note that both in university laboratories and in manufacturers' and users' laboratories there is a steadily increasing practice of promptly publishing research results and with less reservation than has been usual heretofore.

It is quite impossible to establish a sharp dividing line between fundamental research and research of other kinds. Some projects are unmistakably either in the one field or in the other.

If a research is conducted to overcome a manufacturer's difficulty, or to determine the cause for a consumer's complaint, the research generally is non-fundamental. Should the researcher continue his research beyond the stage at which the difficulty is overcome and seek to determine the "reason why," his research becomes as fundamental as if he had set out to test a hypothesis, or to deduce a scientific law. Thus, non-fundamental researches are those intended to secure information likely to be of value only in solving an immediate problem of strictly local significance; for example, a problem created by a works operation, or by a new product. It is realized that despite their humble nature, many insignificant problems have led to researches into the deepest fundamentals. Hence, the problem itself is not the criterion of whether or not a research is fundamental.

Indeed, it is inevitable in all human affairs that increments of knowledge are, in the first instance, empirical; that is, collections of instances from which emerge rules of thumb. The empirical rules are nearly always inapplicable when the range of work is extended, or when unexpected obstacles are encountered. It is not until we have available the empirical knowledge that we can undertake researches which will reveal the fundamental principles. Consequently, there are many occasions on which the Fundamental Research Division undertakes researches which in the first stages yield only empirical results. If, on such occasions, the mood of the researchers is that of being so satisfied with these empirical results that they discontinue their investigations at that stage, then they have failed to respond to the expectations of the Division. This is not serious, however, for others will, in due course, resume the search and will approach more nearly to learning the fundamental principles involved. The earlier workers will not necessarily have been at fault, for there are, admittedly, plenty of occasions where empirical results are adequate for the time being, and where meticulous insistence on striving to carry through a more fundamental research would be premature, unjustified or wasteful. It is manifest that there can be no strict definition of the term "fundamental research." The attitude of the researcher frequently determines the question whether his research should or should not be included as an activity of the Fundamental Research Division.

At the present state of development we find that the majority of fundamental research projects are being undertaken by research workers in universities. However, it is with deep satisfaction that we note the steady increase of interest by manufacturers in conducting fundamental researches in their own laboratories. The multiplicity of researches and researchers may not guarantee research results of the highest importance, but it greatly increases the probability of rich ore being struck by at least a few. A few rich strikes in research amply repay what may appear at a cursory glance to be numerous wildcat efforts.

One of the objects of cooperative research (particularly when supervised by committees) is stated to be to "avoid waste" through preventing duplication. Our Division is not of the opinion that several independent researches on the same subject are undesirable, at any rate in the field of fundamental research. If in a relatively unexplored field, it is attempted to learn about the underlying phenomena by entrusting the task to a single researcher, or a single group of researchers, we are liable to place too much confidence in the results at which they arrive. If the same task is undertaken independently in two different places and without collusion, we all know that in most cases the two investigators will arrive at greatly differing conclusions. In seeking for explanations of these great differences new truths are discovered, whereas had we simply accepted the results obtained by a single researcher, some important phenomena would have escaped our attention completely. Here again we have an illustration of entrusting to individual researchers the choice of subject and the carrying out of the investigations. If we trust to their intelligence and to their constantly inquisitive attitude, the actual facts will ultimately be disclosed and we shall be able to acquire a full understanding of all the phenomena involved. On the other basis of "avoiding waste" by discouraging duplication, we may continue for years to place complete confidence in the erroneous conclusions deduced from the results of a single research that has been arbitrarily assigned.

In the case of many fundamental research projects, a library research should precede laboratory research. Much valuable work can be accomplished by researchers in libraries in abstracting and translating and in preparing bibliographies, and in discussing the published results, and in other ways making existing published knowledge more available and useful to scientists and engineers. But we can go still further in the library and discover or demonstrate new fundamental principles, and this is what we mean by the term "*Library Research.*" By that term we do not mean the bibliographic and other literary work which we assume shall have preceded it. Very often, library research discloses fundamental principles of great importance much more promptly and at less expense than laboratory research. Frequently, however, *Library Research* leads only part way to the desired result. In that case, it will be followed up by fundamental research in the laboratory. When the fundamental truth sought is conclusively evident from the library research, no outlays need be incurred for further work in the laboratory.

Organization—Irrespective of their individual affiliations, the approximately 100 researchers in our Division have adopted a policy of mutual helpfulness. This is especially in evidence when we consider some one locality. For instance, one or two universities, several technical libraries and several manufacturers and users of welding may be within a few miles of one another. Each can and does contribute to the others his experience and facilities. Research thus becomes cooperative, and wasteful overlapping of laboratory facilities is avoided. No one in the group desires to be a recipient to an extent disproportionate to his contributions. It is not intended, however, to suggest that any effort is devoted to evaluating these things. Each simply accepts and renders help as occasions require. The commodity exchanged is usually knowledge or tools or materials.

Superposed on these local groups there is a head office group which combines the functions of an information bureau and of centralized guidance and which is in constant touch with Engineering Foundation's

Welding Research Committee, which is the mainspring of the integrated undertaking. The full time head office workers respond promptly to the extent of their ability, to all requests for advice or explanation or assistance. All the researchers, irrespective of whether they are employed by manufacturers or users of welded products and processes, or whether they are researchers in university laboratories, have learned to use frequently the unique service available at the head office. Thus duplication of research effort, instead of being feared, becomes a useful check, because previous and current activities of all researchers are known so far as possible.

The organization has been made as loose as is consistent with the Division's comprehensive undertaking in coordinating all types of fundamental welding research. Nearly all the researchers are expert in a chosen branch of engineering or scientific thought or technique. Therefore, the Division regards each researcher as best able to decide the destinies of his research. The researcher usually finds it helpful to solicit advice and suggestions from other researchers of the Division, from experts of the Welding Research Committee, or from others. The researcher's decision to conduct research on a topic of his individual selection, irrespective of whether the Division's list of suggested problems was the basis of selection, is the only assurance the Division desires that the research is in capable hands.

By means of charts and other coordinating guides, and by consultation with the head office, any researcher can be promptly put in touch with others working on related subjects. Thus, any number of such researchers may freely act as groups or committees to any extent they consider desirable, but they will do so of their own free will or on subjects and occasions of their own choosing. However, the usual experience has been that the link with the head office together with informal correspondence with one another is found to be the most effective method. It has been the Division's experience that a formal committee set-up may hinder progress more than it may help and that committee procedure is likely to be undesirably cumbersome. Under all circumstances *rigid* research programs are discouraged. Nothing is less bearable to the fundamental researcher than stubborn adherence to a preconceived outline in the face of startling unforeseen experimental evidence. Very likely these conclusions apply only to the Division's particular kind of work—namely, the work of learning fundamental truths.

Conferences—Every year a conference of the entire Division is held on the occasion of the annual meeting of the AMERICAN WELDING SOCIETY. At the Annual Conference, the research workers meet with one another and compare their research experiences. Through subsequent correspondence they are helpful to each other after returning to their respective laboratories. In the course of the conference, valuable acquaintanceships are made with other people in the welding professions.

The most recent annual evening conference of the researchers of the Fundamental Research Division was held during the 1938 Detroit Meeting of the AMERICAN WELDING SOCIETY. Thirty-six researchers were present, who with a number of visitors raised the total attendance to over fifty. After an introductory address by Dr. Comfort A. Adams, Chairman of the Welding Research Committee, the researchers briefly described their projects. Among those who spoke was Professor Edward Bennett of the University of Wisconsin, who described the process of welding he has developed, utilizing the proximity effect. Professor Bennett exhibited a number of welds he has produced by the process. New information on the contact resistance of 18–8 stainless steel was presented by Dr. F. J. Studer of Union College. Other researchers discussed progress in research on the impact testing of welded alloy steels, hard facing, resistance welding, the viscosity of welding slags, cracks in welds, and cast-iron welding. All present participated in the informal discussion that followed these narratives of welding research effort.

List of Problems.—At intervals, the division compiles a list of Fundamental Research problems for the assistance of the researchers in the Division and others. This list, as revised and greatly extended in this supplement, describes the fundamental researches believed to be particularly needed in the welding field.

Direct Contacts.—The Division maintains direct contacts with the researchers not only through correspondence and the research conferences already mentioned, but by visits from the head office staff. Some 40 laboratories were visited during the past year, a special transcontinental trip being made by Secretary Claussen from New York to the Pacific Coast and back. These head office visits serve many useful purposes, including (1) stimulation of progress; (2) securing of first-hand information of facilities available at each laboratory including equipment and personnel; (3) guidance in solving problems and in overcoming obstacles; and (4) securing reports of researches completed, but not ordinarily published.

Acknowledgments—Among a number of cooperators who have contributed specimens or supplies to the researchers of the Division are:

Aluminum Company of American
American Brass Company
Bethlehem Steel Company
Budd Manufacturing Company
Champion Rivet Company
Chicago Bridge and Iron Company
Dow Chemical Company
General Electric Company
Lincoln Electric Company
Lukenweld, Inc.
Malleable Founders' Society
Metal and Thermit Corporation
Union Carbide and Carbon Research Laboratories, Inc.
U. S. Steel Corporation—Research Laboratory
U. S. Steel Corporation of Delaware
Watertown Arsenal
Wellman Engineering Company
Westinghouse Electric & Mfg. Co.
Wilson Welder & Metals Company

This opportunity is taken to express the Division's appreciation.

LIST OF SUGGESTED FUNDAMENTAL RESEARCH PROBLEMS IN WELDING*

CONTENTS

	Page
Weldability Tests	203
Welding Copper and Its Alloys	203
Stress Distribution in Fillet Welds	203
Shrinkage Stresses and Distortion in Welding	204
Welding Plain Carbon and Alloy Steels	205
Effect of Carbon Content	205
Sulphur	205
Phosphorus	206
Oxygen	206
Nitrogen	206
Aluminum in Steel	206
Silicon Steels	206
Manganese Steels	207
Nickel Steels	207
Chromium Steels	207
Molybdenum Steels	208
Vanadium Steels	208
Corrosion Resisting Steels	208
Coated Steels	208
Welding Cast Steel	209
Welding Wrought Iron	209
Welding Cast Iron	209
Impact Tests of Welds	209
Coatings and Fluxes in the Welding of Steel	210
Spot Welding	211
A. Strength and Testing	211
B. Physics (Electrical and Thermal)	211
C. Metallurgical	211
Physics	213
Welding Procedure	214
Structural Studies	214
Chemistry	215
Metallurgical Studies	216

THE Fundamental Research Division presents its revised list of suggested research problems in welding for the purpose of attracting the attention of engineering and scientific researchers of all types to the varied and interesting problems created by welding. The range of processes, phenomena and materials dealt with by welding makes welding research appropriate for a surprisingly wide variety of engineering and scientific laboratories.

While the revised list contains many new problems, a large number of older problems have been retained because they have not yet been investigated systematically, and because they remain important. It has not been easy to prepare the list. Each type of welding and materials presents its own peculiar problems, of which only a few welding experts may be aware. Reluctance on the part of the expert to make his problems known often is based on unwillingness to make statements that can be construed as derogatory to industry. The Division has found through experience that it is nearly useless to canvass industry for suggested fundamental research problems. The Division has had better success in securing suggestions from scientific experts, who have examined existing information on some welding phenomena and sometimes have found it wanting. The Division is glad at all times to receive suggestions for research problems and comments on suggested problems.

It is realized that the statement of the majority of the problems is brief and is barely sufficient for commencing research. Additional information and references about any of the problems will be furnished by the Division upon request made to its Chairman, Mr. H. M. Hobart, or to its Secretary, G. E. Claussen. The Division will assist in securing specimens for research on any of the problems. Usually the specimens are received several months after request.

Prospective researchers upon consulting the list of suggested problems will find that some research will require specimens of welds, whereas others will not. For example, research on the effect of fluxes on the surface tension of metals can be conducted without any welding materials or equipment, except possibly welding rods and proprietary fluxes. On the other hand, research on the fatigue strength of welded joints can scarcely be conducted without welded specimens. Ordinarily, if welds are to be studied, the researcher requires welds representative of current commercial practice. Since it is unusual for the researcher to possess facilities for preparing welds of high quality made under favorable conditions, he should not hesitate to inquire of the Committee whether welded specimens prepared by expert welders can be secured. It may be that the researcher can secure the cooperation of a local welding firm in providing specimens.

The revised list of problems is based to a large extent on the reviews of welding literature that have appeared in the *Research Supplement* from time to time during the past two years. The problems are listed mainly under headings corresponding to the reviews. Classification of the problems according to different engineering fields, such as mechanical and electrical, has been attempted in the past with little success, but lists of problems so classified will be furnished upon request.

In general, welding research problems arise from:

I. Process
   - (*a*) Control of variables
   - (*b*) Mechanism of phenomena

II. Base Metal and Filler Metal
   - (*a*) Chemical composition
   - (*b*) History of manufacture
   - (*c*) Gas content and gas solubility
   - (*d*) Melting point and vapor pressure
   - (*e*) Specific and latent heats
   - (*f*) Macro- and microstructure
   - (*g*) Arc behavior
   - (*h*) Thermal and electrical conductivity
   - (*i*) Mechanical properties at all temperatures up to the melting point
   - (*j*) Thermal coefficient of expansion
   - (*k*) Solidification characteristics, freezing range, etc.
   - (*l*) Heat treatment characteristics
   - (*m*) Flowing power
   - (*n*) Reactions in solid and molten state occurring under welding conditions.

(The above classification was suggested by H. W. G. Hignett.)

III. Procedure of Welding
   - (*a*) Rates of heating and cooling
   - (*b*) Atmospheres
   - (*c*) Dimensions of materials

IV. Properties of Welded Joints
   - (*a*) Mechanical properties
   - (*b*) Chemical properties
   - (*c*) Electrical properties
   - (*d*) Defects

* (NOTE. The thanks of the Fundamental Research Division are due chiefly to G. E. Claussen for this excellent list of Fundamental Research problems in welding and his accompanying explanations and discussions. While some of the material has been provided by researchers in the Division and by other collaborators, the list as here published owes its great value and interest to the large background of metallurgical and welding knowledge which Dr. Claussen has brought to the subject and also to the great amount of hard work put into it by him. An ideal toward which many of us have vainly striven for some years, now has been realized to an extent which will be of much value to the many researchers in the Division and to others with related interests. H.M.H.)

V. Design

Each of the factors listed in the classification is a source of problems which, so far as possible, are listed below.

WELDABILITY TESTS

The expression "weldability" has acquired a meaning different from the simple sense of "ability to weld," because practically all metals and alloys may be welded. The term now denotes degree of difficulty in producing welds having desired characteristics. The chief weldability tests are:

1. Tests for tensile, bend, hardness, impact, fatigue and other mechanical properties of the weld metal and joint.
2. Usability tests (see A. S. T. M.: A 205–37 T).
3. Soundness tests (nick break, density, X-ray).
4. Ability of the welding rod to deposit a bead of given characteristics in different positions.
5. Tests for deposit efficiency.
6. Crack sensitivity tests.

Crack sensitivity tests are the most prominent weldability tests for steel at the present time, and research into the characteristics of existing cracks, sensitivity tests and into the development of others is urgently needed. Several instances have been recorded in which steels, particularly low-alloy steels of high yield strength, have cracked unexpectedly during or after welding.

In some steels, such as aircraft tubing and sheet of chromium-molybdenum type, the cracks occur in, or close to, the weld while it is cooling. The cracks may be related to the welding process, impurities in the steel, or other causes, and are accentuated by conditions of restraint. Weldability tests have been devised for the aircraft steels in which a free or clamped sheet is welded or subjected to the heat of the torch. The percentage of the welded or melted zone that is occupied by cracks before or after bending is known as the crack sensitivity.

In other steels, such as stainless steels, cracks may appear in the weld bead on account of excessive grain size or incorrect composition. The test for sensitivity consists in depositing a bead in such a position at the root of an X scarf that contraction due to a subsequent bead on the other side of the X will tend to crack the first bead. If a crack appears, the weld metal has failed in the test. In contrast with the aircraft steels, the weld metal, not the base metal, is the important factor. Similar tests have been devised for welding rods to fillet-weld thick, low-alloy steels, in which cracking may be due to hot shortness of weld metal, or to a brittle zone in the heat-affected zone which is unable to absorb the deformation produced by the cooling of the fillet weld. In one form of crack sensitivity test, a bead of weld metal is deposited on the tension side of a thick bend specimen. The bead may crack at a small bend angle, but the base metal must resist the spread of the crack.

Cracks in welded structures far from the welds have been observed, rarely it is true, but none the less important, under conditions of unfavorable multi-axial stress. Difficulties with these tri-axial tension cracks appear to be associated with low-alloy steels. Economical application of low-alloy steels requires that the structure be welded. The cracks may owe their presence to low notch sensitivity of the steel, especially at low temperature which may be aggravated by lack of normalizing, shrinkage stresses due to welding, or improper sequence of welding. Crack sensitivity tests for these circumstances do not appear to have been developed. Welding is not the important consideration; rather a test for the ductility of the steel under tri-axial tension may have to be devised.

In all crack sensitivity tests the dimensions of the specimen, the speed and other characteristics of welding, the rate of cooling and the temperature and structure of base metal are known to be of vital importance. However, the variables are so numerous that few, if any, have been investigated systematically.

Weldability is an elusive term, as a preliminary search of the literature has revealed. A prominent French metallurgist expressed weldability in terms of a parameter, which was the product of two (or more) variables. The two variables most commonly involved were: (1) the coefficient of compactness or density, which, varying from 0 to 1, qualified the proportion and significance of physical defects disclosed by etch tests, radiography, fracture tests or magnetic tests; (2) the coefficient of homogeneity, which, varying from 0 to 10, qualified the degree of uniformity of essential or useful properties. The latter coefficient expressed to what degree the properties of the weld are akin to those of the metals to be welded. Others limit the term "weldability" to qualifying the freedom of the joint from all kinds of defects, that is, undesirable attributes from hard zones to blow-holes. Other authorities conceive that weldability expresses ability to join assigned metal parts with a given procedure. A non-ferrous welder used the term weldability to include melting rate and the size and shape of the drops of weld metal. The trend seems to be toward limiting the term to describe some combination of properties of the base metal with minor emphasis on the process of welding.

WELDING COPPER AND ITS ALLOYS

The following problems are summarized from a list prepared by Professor A. P. Young and Ira T. Hook in the last issue of the List of Suggested Research Problems.

1. From the standpoint of resistance welding, it would be desirable to prepare a graph showing the effect of silicon on the electric resistance, strength, ductility and other properties of copper.
2. Research on covered electrodes of the type developed by W. Lessel (*The Welding Industry* (London), March and April 1937).
3. The resistance butt welding of hot-short copper alloys. As Hignett pointed out, little is known about the mechanical properties of alloys just below the solidification range. "Research is required on high-temperature properties in the wrought materials with rising temperature, and in the cast materials with falling temperature."
4. The flash welding of copper alloys having a wide solidification range.
5. An analysis of the factors involved in torsion testing spot welds in copper alloys.
6. A determination of the tendency of copper spot and seam-welding electrodes to alloy with the metal being welded.
7. Recovery of deoxidizing elements in welding copper and its alloys. How much deoxidizer is lost in welding with different processes and techniques?

STRESS DISTRIBUTION IN FILLET WELDS

List of suggested research problems compiled by Cyril D. Jensen

In the Review of Stress Distribution in Fillet Welds in the May 1937 *Supplement* of THE WELDING JOURNAL

three factors are indicated: (1) the materials factor, (2) the welding factor, (3) and the design factor.

A. Suggested Research Based on the Materials Factor

Apparently very little research has been done on other than mild steel and wrought iron. Research in this field is, therefore, open. The writer cannot answer as to *the importance* of determining whether or not the same distribution of stresses obtains in the welded joints of other materials as obtains in mild steel joints. For a more complete statement the reader is referred to the May 1937 *Supplement* mentioned above.

B. Suggested Research Based on the Welding Factor

1. Shape of profile of a fillet weld appears to be a good subject for research. Static tests and polarized light studies have been made proving the superiority of welds with certain profiles, notably concave profiles and the 30–60° fillet welds, over the standard 45° weld. It is suggested that fatigue tests, probably by the vibrator method, be made to show the advantage of these special shapes over the 45° weld.

C. Suggested Research Based on the Design Factor

1. Fatigue studies of stresses in fillet welds are needed. Theorists and experimenters have proved the existence of stress concentrations in side welds, end welds, and in the so-called box welds consisting of both side and end welds. Yet in static tests these welds give high ultimate strengths. It is important to ascertain how much of this stress concentration is really effective in joints subjected to repetitive loading. In other words, how much are joints subjected to repetitive loading to be penalized due to the stress concentration?

A test program might be something as follows: (1) Fatigue test of end-welded specimens having 45° welds, some of which are made with bare and some with covered electrodes to check theory that the stress concentrations are two or three times the average stress. (2) Fatigue test of a series of end-welded specimens in which the size of 45° weld is varied (suggest using covered electrodes for this series). (3) Fatigue test of a series of side-welded specimens making length of weld the variable. (4) Fatigue test of a series of specimens with box welds, holding length or width constant and varying the other. (Keep to such size that direct comparisons can be made with (1) and (2).)

2. The behavior of side fillet welded joints in wide plates (9 inches or more wide) should be studied, both with static and repetitive loading.

3. The subject of "Effect of Welding at Low Temperature on the Internal Stresses in Fillet Welds" is entirely open. The writer is not aware of any systematized investigation of this subject.

4. There may be a zone of high hardness in the heat-affected zone of fillet welds in some low-alloy structural steels. The hard zone lowers the bend ductility, but it has not been determined what effect is exerted by the zone on the static and fatigue strength of end and side-fillet welds in plates about $^1/_2$ inch thick under direct and bending stresses.

SHRINKAGE STRESSES AND DISTORTION IN WELDING

1. Magnitude and distribution of shrinkage and reaction stresses in spot- and seam-welded assemblies, and of shrinkage stresses in flash welds and resistance butt welds.

2. Shrinkage stresses in welded high-alloy steels, such as 18–8.

3. Shrinkage stresses in welded non-ferrous metals. Do shrinkage stresses (if any) in welds in some non-ferrous metals which exhibit a measurable creep rate at room temperature disappear in the course of time? What relief or increase of shrinkage stresses may be expected by aging reactions at room temperature in some precipitation hardening non-ferrous alloys. (An annotated bibliography of articles dealing with the residual stresses set up in metals by aging or allotropic transformations is available upon request.)

4. Magnitude and distribution of shrinkage stresses prependicular to the surface of the plate (in the direction of the thickness) in welds in plate more than about $^1/_2$ inch thick.

5. Shrinkage stresses in arc welds made in thick plates in a single pass.

6. Shrinkage stresses in soldered, bronze-welded and thermit welded joints.

7. For a given type of weld in a given material is there a relation between heat input, speed of welding (arc, gas, flash, etc.), speed of cooling (Newton's Law), and shrinkage stresses perpendicular to the weld? A quantitative theory of this sort based on inelastic deformation has been evolved by Boulton and Lance Martin for shrinkage stresses parallel to the weld at all times before and after the source of welding heat has passed a given section.

8. A complete and analytical catalogue of welding cracks with accompanying quantitative data on the rate and temperature of propagation of the crack, and the magnitude of the shrinkage stresses.

9. Thermal shock tests (plunging a locally heated body into a quenching bath) as an indication of the response of a metal to some of the factors involved in weldability, without introducing the complications of weld metal or extensive microstructural changes.

10. To what extent may overstrain be relied upon to relieve shrinkage stresses in welded higher-carbon steel? To what extent does impact overstrain relieve shrinkage stresses in welded low-carbon steel?

11. There is a need for a review of theoretical and experimental work on multi-axial stresses. It is believed that a cause of brittle cracks at room temperature in ductile welds is multi-axial stress, but it is not known to what extent multi-axial stresses may be expected to reduce the capacity for deformation (slide) and to account for brittle failure.

12. There is a large amount of information on relief of external stress on unwelded metal by stress annealing. To what extent does it apply to the relief of different systems of shrinkage stresses in welds by annealing? Are the reservations important or minor?

13. A table containing reliable quantitative data on the physical properties (thermal and electric conductivity, vapor pressure, etc.) of metals at all temperatures that are important for all types of welding.

14. It is fairly well known that there are residual stresses up to the yield point of the material in welding. At the stress-relieving temperature, 600° C., these stresses are reduced to some low value, around 5000 or 7000 pounds per sq. in., depending upon the yield point of the material. There is ample evidence that residual stresses of a bi-axial nature in ordinary mild steel do not lower the endurance limit in fatigue testing. On the other hand, they may have some effect under corrosion conditions; also, there is reason to believe that they affect the impact properties. There is a belief (see H. Holler, Proceedings International Acetylene Congress, 1937, p. 589) that residual stresses, if distributed over a large area, are not particularly dangerous, but that localized residual stresses are dangerous.

Residual stresses can be lowered by stress relieving, but we do not know what happens to residual stresses in structures which are not stress relieved. In mind steel, residual stresses of a bi-axial nature may not be harmful unless the conditions of service are unusually severe, such as extreme restraint which would give a tri-axial condition, or a higher carbon content which gives a poor metallurgical structure. Some investigations along the following lines are suggested:

(1) Specimens which have a known amount of residual stresses, such as cast steel, could be subjected to various types of tests, including static, dynamic and corrosion. Some of the specimens would be stress relieved, some would not. Some would be proportioned to contain bi-axial stresses; others to contain tri-axial stresses.

(2) The composition of the steels would be varied.

(3) Low temperatures should be investigated.

(4) Rolled bars of square cross section could be used. Instead of welding on the surface, flame heating could be used to introduce extreme and moderate stress conditions on one or more surfaces. This type of stress introduction could be controlled fairly carefully.

(5) Thick, flat and square bars should be tested in which local stresses have been incorporated by local heating.

15. The shrinkage stresses in the vicinity of crossing welds have been determined by the Mathar method in a welded boiler drum. Other shrinkage stress measurements on crossing welds should be made in order to deduce general principles about the probable effect of a crossing weld on the magnitude and system of shrinkage stresses.

16. There is little quantitative information on distortion in structural welding. A common assumption in calculating pre-distortion is to assume that the shortening due to a bead of weld metal on an unrestrained plate is proportional to the length of the bead. The proportionality constant X must be determined for every type of welding and is said by Lewenton to obey the formula: $x = n\left(c + b\frac{a^2}{F_o}\right)$, where n = number of welds, a = thickness of weld bead, and F_o = area of plate upon which the bead is deposited. Evaluation of the constant for different types of welding is needed, as well as an investigation of the validity of the method of calculating distortions.

WELDING PLAIN CARBON AND ALLOY STEELS

Effect of Carbon Content

1. Porosity in welds sometimes is attributed to carbon content. Reaction between carbon and oxygen (FeO) in the weld metal in the absence of sufficient deoxidation by silicon, aluminum or other elements provides carbon monoxide which may be trapped in the solidifying weld metal. Leitner's tests should be repeated using different types of covered electrodes, and base metal containing different amounts of carbon, silicon, manganese and other alloying elements. In Leitner's tests with low-carbon bare electrodes, porosity in the deposit seemed to disappear beyond about 0.40 C in the base metal. Leitner used only one welding speed and one size of electrode.

2. The arc welding of pure iron in different atmospheres has been achieved, but pure iron-carbon alloys have not been welded. Tests made on open-hearth steels containing no silicon, low carbon, unusually low manganese (0.17%) and 0.23 Cu exhibited no peculiarities. The arc and resistance welding of steels with higher carbon and lower manganese contents should be investigated both in air and in special atmospheres.

3. The physical properties of welds should be determined in a series of plain carbon steels having a wide range of carbon contents. Filler rods that are designed to deposit weld metal of the same composition as base metal should be used. Heat treatment of the welds should be studied.

4. The welding of steel often creates difficulties, but the permissible temperature for welding under different sets of (adverse) conditions has not been determined. Tests should be made to estimate the effect of temperature of base metal from −20° to +20° C. (but up to 500° C. also is necessary) on the structure (hardness, cracks) and properties (strength and ductility) of beads on, and welds in, a series of plain carbon steels, 0.10–0.90 C, made by metal arc, gas, carbon arc, and resistance processes at various speeds. Similar tests should be conducted on oxygen cutting and oxygen machining at different speeds. The importance of strict regulation of all variables, such as thickness of metal, size of flame and filler rod, is emphasized. Perhaps tests in which the base metal at different temperatures is melted to different depths and allowed to cool would yield comparative results confirming the welding tests.

5. The properties and structure of atomic hydrogen welds in thin sections of higher carbon steels (over 0.35 C) should be investigated.

6. The welding of graphitic steels has not been investigated. Resistance welding and surfacing by means of fusion welding appear to be the important applications requiring study.

7. Sometimes a band of pearlite is observed at the fusion line of welds in low-carbon steel, particularly after normalizing. Several explanations have been proposed but none has been established. According to one explanation the band is not pearlite but sorbite, which creates a false impression of high carbon content. Others believe that the carbonless zone has a high oxygen content (Thompson-Willows theory). Still others point out that the weld metal usually has a lower carbon content than base metal. During cooling, ferrite forms first on the low-carbon side of the fusion line and pushes the higher carbon austenite before it. Finally, it has been thought that the carbon in those zones of base metal near the weld and heated below the critical temperature, diffuses into the austenitic regions by reason of the higher solubility of carbon in austenite than in ferrite (carbon solubility differential). Since the band of pearlite possibly may exert a slight effect on ductility, an inquiry into its cause and remedy would be useful.

Sulphur

1. Poor ductility in welds with rather high sulphur content is often blamed on the sulphur. Welds in carefully refined steels to which sulphur has been added intentionally in amounts up to 0.15% should be prepared and tested for ductility. Similar tests should be made on weld metal of high quality except for sulphur content. The welds should be examined for porosity to determine whether sulphur-containing gases are the cause of any poor properties that may be found.

2. The solubility of sulphur compounds and gases in the various steels as a function of temperature should bear profitable study to explain the conditions under which gas pockets form. Such a study would have to

account not alone for the total sulphur contents involved, but also its condition of segregation.

3. Further studies of rimmed steel in terms of the effect of sulphur segregations are needed.

4. Intergranular sulphide has been observed unexpectedly in welds in steels containing relatively high sulphur. The conditions should be determined under which sulphur in plain-carbon and alloy steels may occur as intergranular films of sulphide in weld metal or heat-affected zone.

5. To find the cause of cracks that may occur due to poor ductility of the steel at elevated temperatures. The steel must first have been heated to all temperatures up to the melting point and not cooled below critical before testing. Perhaps depositing a weld bead and bending it before it cools to determine whether cracks occur, might be a fruitful approach.

6. Is it true that the effect of sulphur is to make the welding arc unstable, because $FeSO_4$ is not a stabilizing substance? $FeSO_4$ may occur as an impurity in materials for electrode coatings, or as a pickling residue.

Phosphorus

1. Several explanations have been offered for the beneficial effect of phosphorus on the forge welding of Bessemer steel. The phosphorus may vaporize to protect the surface or may form a fusible phosphate. The precise rôle of phosphorus has not been made clear.

2. Methods and welding rods for depositing weld metal identical in composition with the low-alloy steels containing phosphorus do not appear to have been developed.

3. The effect of phosphorus on the properties of spot and seam welds in mild steel sheets has not been investigated.

4. Despite the lack of interest in phosphorus displayed by welding investigators, they often have relied upon phosphorus segregations to provide an explanation for otherwise mysterious difficulties. Whether the phosphorus was the main difficulty or whether it was the associated inclusions was never determined. Further, it is not plain whether the effect of segregated phosphorus on melting point was more important than its effect on brittleness.

Oxygen

Only a few of many obvious problems, albeit difficult of solution, are listed. In any research on oxygen in weld metal it is important to keep in mind the caution issued by one investigator who states, "Too much research is based on the fallacy that weld metal is necessarily homogeneous. Too little attention is paid to the varying content of constituents due to the difference in conditions in laying the beads of weld metal, the difference in grain formation at the bottom and the top of the weld, and the difference in thermal conditions at the beginning and the end of a weld. It may be necessary to remelt weld metal in a crucible to secure a uniform distribution of constituents, at the risk, however, of altering initial conditions in the weld metal."

1. Fractional oxygen analyses of weld metal as a function of speed and technique of welding and of the composition of welding slag and coating or flux are required.

2. What is the oxygen content of the solidified globule at the end of a covered electrode when welding has been interrupted? Has the globule a higher or lower oxygen content than weld metal deposited by the electrode? The spattered globules might also be analyzed for oxygen. In this way the origin of oxygen pick-up might be found and some conception of the efficiency of the slag in fluxing the oxides might be gained.

3. The cause of shrinkage cracks in weld metal is said by some investigators to be iron oxide; by others, oxygen is not considered to have any effect. Are the shrinkage cracks related to hot shortness created by oxygen? High-temperature (500–1300° C.) tensile or bend tests of weld metal containing different amounts of oxygen combined in different forms might provide the solution of the problem.

4. Although oxygen is a major constituent of the gas found in weld metal, it is not clear whether high oxygen content increases or decreases the likelihood of porosity and shrinkage cavities. In this connection it has not been demonstrated whether the rimming reaction is a factor in weld metals, and, if so, to what extent.

Nitrogen

1. Nitrogen pick-up in controlled atmospheres.

2. Which of the constituents of coatings are most effective in reducing nitrogen pick-up?

3. Solubility of nitrogen in iron up to the boiling point of iron.

4. The effect of the size of the weld puddle and rate of solidification on nitrogen pick-up.

5. Does a bead of covered electrode weld metal absorb nitrogen when deposited on an iron-nitrogen alloy, such as a bare electrode deposit?

6. Distinction between the aging effects exerted by carbon, oxygen and nitrogen in low-carbon steel weld metal deposited by bare electrodes and having different contents of the three elements.

Aluminum in Steel

1. The fundamental effects of aluminum on the welding of steel are not clearly known. On the one hand, aluminum forms an oxide film around the molten drop of weld metal. The film is difficult to flux and there is no information on the relative fluxing effect of different fluxing agents in different types of welding. On the other hand, the preferential oxidation of aluminum protects silicon, manganese and similar elements from loss during deposition, to an extent that has not been studied. Weld metal containing about 0.5 Al has been found to be unusually fluid probably because the temperature was unusually high as a result of aluminothermic reaction. The fluidity of weld metal containing aluminum has not been studied.

2. It is not certain whether the effect of alumina in increasing the rate of cooling required to produce martensite in heat-affected base metal is conducive to crack-free welding.

3. Porosity has been associated with alumina by several investigators but the evidence has not been conclusive. Examination of beads of weld metal deposited on steels with different alumina contents should reveal any tendency toward porosity with increasing alumina content.

Silicon Steels

1. There is an obvious paucity of mechanical test results on welded silicon steels. There are many gaps in information on fatigue, hardness and impact value. Studies could profitably be made of the effect of silicon content on welding the new low-alloy high-strength steels, as well as other alloy structural steels. The effect of silicon in the plain carbon steels would form the basis of these more complicated studies.

2. One of the obvious needs is a study of the interrelated effects of carbon and silicon. These results could then be combined with the proposed similar study

with regard to manganese and carbon. Expecially in gas welding there appears to be a definite relation between the silicon and manganese present, and this same relation, in a different degree, is no doubt also present in arc welding.

3. According to a correspondent, it is surprising that good forge welds can be made in relatively low-silicon steels (up to 1.35% Si) as well as in high-silicon steels (4 to 4.65% Si), whereas steels with intermediate silicon content cannot be forge welded. Research on the scaling of silicon steels with particular reference to the tenacity and melting point of the scale and its fluxing by welding fluxes might shed light on the confused subject of the effect of silicon on forge- and spot-welding silicon steels.

4. The rôle of silicon in reducing porosity in arc and gas welds is not clearly defined. Leitner's tests appear to be in the right direction, but information on the composition and gas content of base metal and weld metal in tests of the type Leitner made, would be informative. The effect of welding speed and silicon content on the elimination of porosity should also be studied, as well as the relationship, if any, between porosity, slag inclusions and cracks in welded silicon steels.

Manganese Steels

1. The effect of preheating (300–500° F.) on the weld-quench values (Bruckner method) of low-alloy manganese steels.

2. For a given set of welding conditions how does the permissible manganese content of base metal vary with the contents of carbon and other alloying elements?

3. The loss of manganese in fusion welding is commonly attributed to oxidation. Quantitative tests of the amount of Mn lost by vaporization and by oxidation (nature of oxidation product) in welding different manganese steels by different processes would aid in explaining the cause of manganese loss. The effect of other elements on Mn recovery should be studied. A related problem is the possibility that vaporization of Mn may increase the difficulty of spot welding steels with high manganese content.

4. The welding properties of steels with 0.2 C, 2 to 5 Mn and the mechanical properties and structure of welds made therein by different processes.

5. What are the consequences, in welding, of the well-known effect of manganese in increasing the sensitivity of steel to overheating?

6. In the last issue of the *List of Suggested Research Problems*, Dr. R. H. Aborn suggested three problems not involving welded joints that yet have a close bearing on the welding of pearlitic manganese steels. The problems are:

(*a*) the determination of critical cooling rates of carbon-manganese steels by the method used by T. G. Digges;

(*b*) the determination of the short-time and long-time tensile properties of carbon-manganese steels at elevated temperatures;

(*c*) the systematic study of the influence of carbon and manganese contents on the grain coarsening characteristics of austenite.

7. Is there a similarity between the Mn-Al-S balance in cast steel, which governs ductility, and the similar balance which may obtain with aluminum dioxidized weld metal?

8. It has been found that manganese up to 12% exerts a large influence on the characteristics of the arc. A quantitative study of the effect of manganese on the arc is called for.

9. What are the causes of crater cracks in arc welding 14% Mn steel? In what way does peening prevent the cracks?

10. To what extent is it true that the relative absence of brittleness in the heat-affected zone of welds in 14% Mn steel made with Ni-Mn filler rods is due to the diffusion of nickel into the heat-affected zone?

Nickel Steels

1. Resistance welding nickel steels of any nickel content and thickness. Research on feasibility, properties and treatment of welds is needed.

2. Properties of welds made with high-nickel steel rods, e. g., 25% Ni.

3. Gases absorbed in welds as a function of nickel content.

4. What is the composition of the slag in flame cutting nickel steels? What is the composition and structure of the flame-cut edge?

5. What properties may be expected of brazed or Monel welded joints in nickel steels?

6. Metallographic study of different types of welds in $3^1/_2$% Ni steel as a function of carbon content and heat treatment.

7. The effect of nickel in steel on shrinkage stresses in welding.

8. Has nickel in the coating a different influence (if any) on arc characteristics from nickel in the core rod?

Chromium Steels

1. The creep characteristics of welded 4–6 Cr steel with different alloying elements, such as titanium columbium, molybdenum and tungsten, and with different heat treatments. What is the effect of multilayer welding, and is the required reinforcement with plain carbon steel rods calculable from the results of creep tests?

2. What is the composition of the material blown from the kerf of flame-cut plain chromium and chromium alloy steels? Has chromium any effect in increasing pick-up of carbon from the oxyacetylene flame?

3. What rôle does chromium play in the arc between steel electrodes containing chromium?

4. The significance of temper brittleness in welding is not clear. If a steel that is subject to temper brittleness is welded, is a section of the heat-affected zone embrittled? Is the heating and cooling cycle during different types of welds in chromium alloy steels of different thicknesses of the sort to create temper brittleness during slow cooling after heating to the stress-relieving temperature? If so, what remedies are available?

5. If metallurgical treatment is not possible in welded high-tensile steels and such treatment is necessary to prevent air hardening or other embrittling tendencies, special steels will have to be evolved in which these tendencies do not exist. (Suggested by R. Smallman-Tew.)

6. What factors (high melting point of chromium oxide, high heat of formation of chromium oxide, effect of coating constituents and flame adjustment) govern the recovery of chromium from welding rods of 4–6 Cr steel and other chromium steels? To what extent does plain carbon-steel weld-metal pick up chromium from chromium-steel base metal and to what extent does pick-up depend on welding process and technique?

7. Can the alloying additions that are added to 4–6 Cr steel to prevent excessive air hardening be added to nickel chromium steels ($3^1/_2$ Ni, $1^1/_4$ Cr) with the same success, thus overcoming some of the welding difficulty? What is the effect of these additions on the appearance of martensite and pseudo-martensite?

Molybdenum Steels

1. Because of the good properties of carbon-molybdenum steels at elevated temperatures, a systematic study of the creep properties of welds in these steels should be made. The data in the review, while indicative of good creep resistance of these welds, nevertheless are very meager and incomplete.

2. Due to the increasing use of ferromolybdenum in electrode coatings, and its stated beneficial effect on arc stability and recovery, further studies of these factors might well be made. What is the mechanism of this improved arc stability, and is it present to the same degree when the molybdenum is present in the coating as when present in the electrode wire?

3. There is much conflicting opinion as to the degree of air hardening and loss of ductility and creep strength, if any, that takes place upon welding carbon-molybdenum steel without preheating, and there are still some who are skeptical of the need for preheating. A research project in this question would be of value to the industry.

Vanadium Steels

1. Mechanical properties (static tensile, bend, notch and tensile impact) of all-weld-metal containing different carbon and vanadium contents with perhaps additions of manganese, nickel and molybdenum prepared by oxyacetylene and metal arc processes. The composition (0.18 C max., 1.45 Mn max., 0.15–0.30 Si, 0.08–0.14 V) suggested in Metals Handbook—1936 Edition, p. 463, is a particularly timely one to study.

2. How does the grain size (dendrites) of single beads of weld-metal containing different proportions of carbon and vanadium deposited by arc and oxyacetylene process compare with similar deposits containing no vanadium? What types of microstructure are observed? Has the deposition of another layer any unusual effect on the first layer?

3. Hardness surveys of the heat-affected zone of carbon-vanadium, manganese-vanadium and nickel-vanadium steels with carbon contents from 0.10 to 0.30% after a bead of weld-metal has been deposited on the surface. Effect of thickness and other dimensions of plates, and of speed of travel and heat input of the source of heat.

Corrosion Resisting Steels

1. The recovery of alloying elements in stainless rods during different welding processes should be studied from the viewpoint of origin of loss. Slag metal reactions, about which nothing appears to be known, probably determine the loss more than any other factor. The quantitative aspects of recovery have been neglected.

2. What function is fulfilled by manganese in 18–8 welding rods?

3. There is scanty information about the corrosion fatigue strength of welded 18–8 and other stainless steels and irons in the corrosive conditions to which they often are subjected in service. Corrosion fatigue tests should be conducted on polished weld metal and unmachined welded joints.

4. It has been said that columbium and titanium reduce porosity in welding stainless iron. Do these elements exert a deoxidizing effect, or do they affect gas solubility, arc temperature, rate of deposition or other factors?

5. There is no information on the effect of copper on the welding of stainless steels. For example, a steel with 0.30 C, 20 Cr, 1 Cu is said to be free from rapid grain growth and embrittlement on prolonged exposure above 650° C. Addition of 1% Cu and 1% Si to 12–16 Cr iron is said to result in unusually sound castings and to increase the static and impact strength of wrought products.

6. What effect do sulphur and selenium exert upon the welding of stainless steels?

7. Corrosion tests should be made on welded 18 Cr–8 Mn alloy.

8. A study should be made of the factors affecting porosity in stainless alloy deposits. Some of the factors appear to be arc length, speed of welding (puddling), fluxes, position of rod in flame and slag from preceding bead. The method of depositing a bead on the surface of a plate and examining the deposit for quantity and type of blow-hole and inclusion might be applied.

9. Practically nothing is known about the magnitude of shrinkage stresses in welded stainless steels or about the effect of shrinkage stresses on service behavior of welded stainless steel parts.

10. Besides existing as an impurity up to as high as 0.30% in stainless steel weld metal, nitrogen often is added as an alloying element. A detailed metallurgical study of nitrogen in welded 18–8 (heat-affected zone and weld metal) is needed.

11. The gain in ductility through heat treatment of welded 18 Cr iron has not been explained.

12. Corrosion tests should be made on welded joints between mild steel and corrosion-resisting steels and irons. Tests have been made in sea water at 20° C. and boiler water at 160–190° C. on mild steel welded with austenitic electrodes (25 Cr, 20 Ni, 1.5 Mn). Additional tests in different reagents on 18–8 welded to mild steel are called for. Electrolytic potential studies might be useful. The heat treatment of assemblies involving mild steel welded to 18–8 requires investigation.

Coated Steels

1. Shrinkage stresses in unannealed fusion-welded nickel-clad and stainless-clad steel. The distribution of shrinkage stresses in butt-welded plates made with and without restraint should be investigated by X-ray or subdivision methods.

2. Fatigue strength of fusion-welded nickel-clad and stainless-clad steel.

3. Creep behavior of fusion-welded nickel-clad and stainless-clad steel.

4. A study of the welding of higher-tensile alloy steels clad with nickel or stainless steel. Cracking and other difficulties should be studied as well as the mechanical properties of welds.

5. Mechanical properties of fusion-welded copper-clad and Monel-clad steel.

6. Intergranular penetration of zinc-lead-tin solder in areas of alloy steel affected by welding heat resulted in cracks. The conditions necessary for such cracking should be determined. Doubtless many steels will be insensitive and some solders will not exhibit intergranular penetration under any circumstances. It has been reported that a welder who found that tinned steel became extremely brittle when welded with mild steel electrodes, avoided the difficulty by substituting Monel electrodes. Embrittlement occurred, perhaps, through intergranular penetration of molten tin into the steel, which was subjected to tensile shrinkage stresses. Experiments should be undertaken to determine the reason for the absence of penetration when welding is performed with Monel electrodes.

7. The fusion welding of light-gage stainless clad (lighter than 0.090 inch) is common practice but methods must be developed for reducing the iron contamination on the stainless side. Iron contamination seriously

reduces the corrosion resistance of welds in light-gage stainless clad.

WELDING CAST STEEL

1. Base Metal. Complete investigations of physical properties, welding characteristics and hardness penetration of low-alloy cast steel welds, particularly of cast steel containing copper.
2. An investigation of welding cast steel (plain carbon) with low-alloy, low-carbon or austenitic filler rods.
3. If weld metal and junction zones are not more coarse grained than heat-treated casting, is heat treatment above 600° C. (stress-relief) necessary?
4. Shrinkage stresses in cast steel welds.
5. Low-temperature tensile properties of cast steel welds.
6. Fatigue and vibration tests of welded cast steel structural elements.
7. The thermal expansion of weld metal compared with cast steel of the same composition.

WELDING WROUGHT IRON

1. Impact and fatigue properties of arc- and gas-welded wrought iron.
2. Properties of bronze-welded wrought iron.
3. Welding of wrought iron alloyed with nickel (metallurgy and testing).
4. Welding of galvanized wrought iron (procedure and properties).
5. Metallography and properties of flash-welded wrought iron.
6. Strength of spot welds in heavy wrought iron plates ($^1/_4$ inch to $^1/_2$ inch thick).
7. The slag question in welding wrought iron with covered electrodes. Do some coatings tend to entrap slag in the weld?
8. The relationship among power, pressure, upset, slag content and mechanical properties and structure of resistance butt-welded wrought iron.

WELDING CAST IRON

A thorough statement of the more important problems awaiting solution in the welding of cast iron was prepared by Professor W. Spindler in the previous issue of the List of Research Problems. The following list summarizes the more important of his suggestions:

1. Cause of Hard Spots.—It is generally considered that difficulties, including hard zones, of ungraphitized cast iron, arise in the welding of carbon steels when the carbon content exceeds about 0.3 per cent. In cast iron with a total carbon content usually above 2.5 per cent the difficulties of controlling weld hardness and other properties are intensified and not completely understood. Undoubtedly some of the causes of hard zones are more important than others and further study should be made to establish the types of irons and welding conditions and procedure where each cause becomes active.

2. Rate of Cooling and Preheating.—It appears that the primary effect of preheating is to reduce the rate of cooling although the more uniform temperature distribution due to preheating results in lower shrinkage stresses. It is known that the hard, unmachinable and brittle white iron and martensitic structures result from very rapid cooling rates. Yet there is no quantitative information available on cooling rates of welds, due no doubt to the difficulties of measurement involved. The rates of cooling through the temperature ranges where graphitization and phase changes occur are especially significant and should be fully studied.

3. Effect of Variations in the Composition of Cast Iron on Its Weldability.—Further studies should be made on the effect of independent variations of the usual chemical elements singly as well as additions of special elements to the base metal and filler metal. Improvement in ease of welding including reduced preheat and increased welding speeds should result from these studies as well as better and more uniform weld properties. Representative alloy and high-strength irons should be included in these researches.

4. Compositions of Fluxes, Electrode Coatings and Slags.—Little appears to be known about the composition of slags produced in the welding of cast iron. Likewise there is much difference of opinion as to flux compositions and the resulting effect on ease of welding and weld properties. Electrode coatings are used to add certain desirable chemical elements to the weld metal as well as act as a flux and should be fully studied. Extensive and tedious chemical analyses are needed to correlate weld chemistry with metallographic and physical properties of the welds and deposited metal.

5. Gas Holes and Porosity.—Considerable disagreement and inconclusive experimental evidence is found in the literature as to the cause of porosity in cast-iron welds. Thermodynamic and physical chemical studies as well as experimental attack should hasten the solution of this problem. The effect of fluxes and coatings in deoxidizing and degasifying the deposited metal should be studied as well as the variables of welding procedure. The effect of water vapor from the flame, flux or coating also should be studied.

6. Comprehensive Studies of Physical Properties.—Additional information is needed on the mechanical properties of welds made by all methods using optimum procedures in representative plain cast irons. Practically no such information is available on the properties of welds in the newer alloy and high-strength irons such as pearlitic or spheroidized malleable cast iron. Much of the physical property data reported is incomplete and difficult to correlate because full details of welding and test procedure are not stated. For this reason there is need for a standard test procedure for welds in cast iron which should include the size and shape of test welds and amount of discard and dimensions of test specimens and the testing procedures. Tests should include tensile, transverse, hardness, repeated impact and fatigue on which scattered information is now available. Nothing appears to be known of the compression, torsion, wear, corrosion and high- and low-temperature properties of cast-iron welds.

7. Monel and Nickel Filler Metal.—Due to the fact that neither copper nor nickel forms compounds with carbon, the problem of hard fusion zones should be reduced using an alloy of these metals. Some investigators have found this to be true while others have not. Further work on this problem is indicated which should include studies of cooling rates, fluxes, microstructures and physical properties of the weld and deposited metal.

IMPACT TESTS OF WELDS

Based on a detailed discussion of important research problems by Professor O. H. Henry in the last issue of the List of Suggested Research Problems, the following problems are only a few of the numerous questions created by notch effect and impact loading.

1. The *Deoxidation* factor is important in steels.

Low-carbon steels deoxidized with elements such as aluminum, vanadium and zirconium have good notch-impact value even down to −120° C. In this may lie the explanation of the observation that weld metal from covered electrodes sometimes has better notch-impact value than plate metal at low temperatures. All deoxidizers are not equally effective, which brings up the problem: What Is the Relative Efficiency of Deoxidizers in Weld Metal, Particularly from Covered Electrodes in Multilayer Welds, in Maintaining Good Notch-Impact Value at Low Temperatures?

2. Grain size in steel is closely connected with the deoxidation factor. Coarse grain structure is generally associated with poor notch-impact value. Widmannstatten structure in plate metal in the vicinity of the last bead in multilayer welds, or on all sides of the weld in single-layer butt and fillet welds is probably bad for notch-impact value but the magnitude of the effect is not clearly known. Tests on specimens heat treated to structures similar to those found in different heat-affected zones show that notch-impact value decreases as grain structure is coarsened. The austenite grain size appears to be the controlling factor here. Still, if a zone of relatively coarse grain structure is backed up or supported by a fine grain zone of high notch-impact value, is the notch-impact value of the aggregate decreased in proportion to the relative thickness of the coarse grained zone? The problem is: To Determine the Notch-Impact Value at Different Temperatures of Weld Metals Having Known Proportions of Coarse and Fine Grain Structure Back of the Notch.

3. Some precipitation hardening alloys occasionally must be welded under conditions which make subsequent heat treatment impracticable, e.g., Duralumin. In every case the question may arise: What Welding Process and Procedure Has Least Effect on Notch-Impact Value?

4. Notch-Impact Value of Weld and Heat-Affected Zones in Welded Low-Alloy Steels at Different Temperatures. Apart from deoxidizer effects some elements are known to have a large effect on the notch-impact value of welds in low-alloy steels. What governs the effect of a given element in this connection appears to be unknown. Data are available for some combinations of alloying elements, but are lacking for others.

5. Notch-Impact Value of Weld and Heat-Affected Zones in Welded Non-Ferrous Alloys. Practically none of the non-ferrous alloys has been seriously studied, although Monel metal, among others would appear to be worth investigation. The usual procedure of notching the weld and the different heat-affected zones as revealed by micrographs would be appropriate.

6. What Are the Effects on Notch-Impact Value of Local Occurrences of Martensite of Different Relative Proportions with Respect to Ferrite-Carbide Structures, in the Heat-Affected Zones of Welds? An informative study might be based on Meunier and Rosenthal's method of depositing weld beads on the surface of a steel at different speeds of welding, of notching the bead down to the heat-affected zone and of testing the specimen so formed. Hardness measurements according to Theisinger's method, as well as microscopic examination, would also be made. In this way any relation that exists between maximum hardness in the welded area and the notch-impact value could be determined.

7. Exploratory Tensile Impact Tests at Relatively Low Speeds of Commercial Welds in All the Common Metals and Alloys of Engineering Construction with and without Heat Treatment. This is a large order but an important one.

8. The "Critical Speed," if Any, for Welds Made by the More Important Processes in the More Important Metals and Alloys at Different Temperatures. There are, of course, many other problems but none seems so important as the two that have been mentioned.

9. The Application of the Torsion-Impact Test to the Study of Welded Aircraft Tubing. Disregarding the reheating effect of the beginning of the weld by the end of the weld, the torsion-impact test might be a useful method for testing all kinds of butt-welded tubing and pipe. Thin material could be tested as well as solid bars. No relation has been found between the torsion-impact test and other forms of impact testing.

10. The explosion impact tests recently performed by U. S. Steel Corporation literally open up a new field of investigation. It would seem that tests on a smaller scale might be a bit less hazardous, but detailed suggestions for further research, of course, must await the detailed analysis of the results obtained thus far.

11. What is the impact value of spot and seam welds?

COATINGS AND FLUXES IN THE WELDING OF STEEL

1. There are many distinct types of successful covered electrodes. The basis for assigning an electrode to a given type may be composition or welding characteristics (position or type of current). Stricter classification of existing covered electrodes should be based on the A. S. T. M. Usability Tests or on such characteristics as melting rate and penetration.

2. If the same coating is used on electrodes of different diameters implying different power conditions, the thickness of the coating must be adjusted accordingly. How do the characteristics of a successful covered electrode vary with the thickness of the coating? By characteristics is meant (1) recovery of carbon and alloying elements, (2) pick-up of foreign elements, nitrogen and oxygen, (3) porosity, cracks and inclusions in weld metal, (4) melting rate of electrode, (5) penetration, (6) shape of bead, (7) ease of removal of slag, (8) arc voltage and atmosphere, (9) globule size.

3. To what extent does the flexibility of successful covered electrodes vary? In other words, to what extent can optimum welding conditions (position, welding speed, arc length, current, type of current) be altered without injuring mechanical properties beyond a specified amount?

4. In what form are slag and metal carried across the arc gap in welding with inorganic covered electrodes? The metal from organic covered electrodes is in the form of minute globules.

5. The composition of welding slags has not been studied comprehensively. A mild steel electrode should be covered with different thicknesses of elementary coating materials and deposited under the same sets of conditions, so far as possible. For example, a waterglass coating might be deposited at several currents. The slag from each bead should be analyzed chemically and petrographically. The weld metal also should be examined. The quantities of iron oxide in the slag under different conditions of coating thickness, rate of forward motion of electrode, and current might provide some insight into the speed of reactions during arc welding. A similar study might be made for stainless steels and for oxyacetylene welding. A different initial flux would be needed for oxyacetylene welding, possibly borax.

6. It has been surmised that the heat of reaction evolved during solidification of weld metal in which the Mn, Si, C and O_2 contents are of the correct proportions for a rimming reaction may affect the hardness of the

heat-affected zone in low-alloy steel to a significant extent. The conditions necessary for the reaction and the approximate magnitude of the heat evolved should be determined.

7. A study should be made of the effect of different constituents in the coating on the grain size and corrosion resistance of weld metal deposited by mild steel and corrosion-resisting steel electrodes.

SPOT WELDING

Suggested by Fundamental Research Division—Resistance Welding Section

A—Strength and Testing

1. Strength of Spot Welds. (Unless otherwise specified, strength implies tension on a lap weld.) What is the relation of diameter of spot to thickness of sheet? The approach would be the determination of the strength as a function of diameter for different thicknesses. The tests should be made on a wide variety of ferrous and non-ferrous metals welded under optimum conditions of current, pressure and time.

2. What is the change in the strength of an individual spot as a function of the number and arrangement of adjacent spot welds in a joint.

3. The endurance limit of spot welds is an important factor in design. Very few basic data on fatigue strength of spot welds are available, except in special circumstances. The stress should be combined tension or compression with shear in the zone adjacent to the edge of the weld.

4. Satisfactory methods for determining the tension impact value of spot welds should be devised.

5. Impact tests of spot welds in low-alloy steels at low temperatures.

6. Does the unit pressure for a weld of maximum strength decrease as the area of the weld electrode increases?

B—Physics (Electrical and Thermal)

1. Techniques of Measuring Rapidly Fluctuating Temperatures as a Function of Time. Survey of existing methods supplemented by development of promising techniques below the range of visible radiation are of greatest importance. These temperatures should be determined simultaneously at a number of localized positions near resistance welds.

2. Thermal Contact Resistance at Elevated Temperatures and Pressures. Theoretical considerations on thermal distribution in spot welds are based on the as yet unknown resistance of the contact to passage of heat. The important contact is the metal-to-electrode contact. Experimental determination of thermal contact resistance is needed before several hypotheses of spot welding may be confirmed.

3. Electrical Contact Resistance at Elevated Temperatures and Pressures. Particularly in spot welding, the two parts to be welded are brought into intimate contact by pressure. Upon passage of a heavy electric current, heat is generated mainly at the interface (sheet to sheet) where the contact resistance is encountered, and leads to welding. It thus appears that the contact resistance, especially at temperatures near that of welding, is of significance in resistance butt and spot welding. Measurements of the contact resistance at room temperature between sheets of mild and stainless steels and copper alloys have been made. Whether the contact resistance is created by a surface film of adsorbed gas or oxide or sulphide or whether it is due to the inherent roughness of a metal surface which prevents intimate contact even under high pressures at room temperature is not known. The pressures involved are 10,000–20,000 lb./in.^{2} for mild steel, and 40,000 to 60,000 lb./in.^{2} for stainless steels.

Increase of pressure lowers the rate of heating. To what extent is the decrease due to widening of contact area and to increased conduction of heat to the cooled electrode?

Some interesting data can be secured by making comparative resistance measurements electrode to electrode when *one sheet* of thickness $2t_1$ is interposed and when *two sheets* of thickness t_1 are interposed. These measurements should show the additional resistance caused by the extra (welding) pair of surfaces. Measurements taken with various values of t from 0.010 in. to $^1/_8$ in. would furnish desirable information. If the total thickness of the two sheets is made up of one thin sheet and one thick sheet, different conditions are encountered, which should be studied.

Resistance measurements on welding materials must be made using special jigs, embodying friction-free moving parts and readily adjustable and calibrated pressure arrangements, uniform contacting surfaces, etc.

Measurements of contact resistance, as a function of time during the flow of welding current, are being made by means of a calibrated oscillograph. Records of sheet-to-sheet and electrode-to-sheet voltages are taken simultaneously with that of secondary current. This permits the data taken for the evaluation of secondary current to be used also for the contact resistance measurements. These data also make possible an electrical calculation of the energy input to the weld.

The following items must be considered in determining contact resistance:

1. Deformation of materials under pressure.
2. Kind of material.
3. Thickness of material to be welded for a given electrode area.
4. Electrode size and design.
5. Condition of material (i.e., cast, forged, cold worked, etc.).
6. Condition of surface of material (i.e., smooth, polished, rough, buckled, etc.).
7. Surface films (i.e., amorphous metal, oxides, slags, dirt, etc.).
8. Pressure.
9. Temperature and temperature effects (i.e., formation of films, increase in resistivity of materials).
10. Values of current and voltage in determining resistance values. An arbitrary, constant current density and voltage should be decided upon.
11. Methods of determining resistance.
    (*a*) Voltage drop—Potentiometer.
    (*b*) Resistance bridge—Kelvin Bridge.

4. Contact Resistance of Tip Materials. A study of the temperatures reached in the spot-welding process as a function of time and distance from the center of the weld. This problem might be attacked to a certain extent by thermocouples and by non-metallic materials of known melting point.

5. What methods are available for measuring the large actual spot-welding currents that flow for only short intervals of time? Current transformers with or without vacuum tube voltmeters, oscillographic shunts, primary current measurements and other devices should be subjected to analysis and experimental comparison.

6. What effect have wave shape or transients on spot welding, provided they are developed by the transformer.

7. What is the effect of the inertia of the moving parts of a spot welder on the motion of the head during welding and on the pressures exerted? Experimental methods for studying inertia must be developed.

8. Thermal and Electric Conductivity of Metals at Elevated Temperatures. A summary of available information relating to the thermal and electric conductivity of the commoner metals and alloys that are spot welded at temperatures from 20° C. to the liquid state would be useful, particularly if it were supplemented by determinations on the metals and alloys for which information may be found to be lacking at elevated temperatures.

9. Thermal and Electric Conductivity of Alloys Used for Electrode Tips. A similar summary to 8 with additional data on the effect of local cold work on the conductivities and of surface films or surface preparation on the contact resistance of tip to work would also be valuable.

10. The Mechanism of Spot Welding. The process of spot welding is well developed and consists of the application of pressure to bring the plates into contact, the passage of current to create heat for welding and the continued application of pressure after the current is switched off to keep the weld intact until it cools sufficiently to permit handling. There are many variations in the commercial processes and the welding cycle may occupy a small fraction of a second, or a minute or two depending on the metal, the thickness of plate and the size of the spot.

The mechanism of welding is not so well understood as the process. In some welds in some metals, welding may occur by growth of crystals across the interface under the combined influence of recrystallization and pressure. In other welds there is local fusion, crystals from both sheets obtaining coherence by reason of crystallization from a common liquid. In still other welds it is contended that there is local evolution of metallic vapor, which is considered to play an unfavorable rôle, and which necessitates the application of high electrode pressures to prevent the sheets being forced apart or liquid metal ejected at the interface.

Perhaps an examination of sheets subjected to successive small increments of the spot-welding cycle would reveal the mechanism of spot welding. Different alloys and different spot-welding cycles might be studied. If the cycle lasted, let us say, 1 second, specimens might be examined after being subjected to 0.02, 0.04, 0.06, etc., seconds of the entire cycle. The strength of the weld, the appearance of the fracture and the microstructure might be studied.

A more complete study would include an investigation of spot welds made with a given 60 cycle current setting in:

$^1/_{120}$ Sec.	with	variations	in	pressure	
$^1/_{60}$ "	"	"	"	"	
$^1/_{30}$ "	"	"	"	"	
$^1/_{20}$ "	"	"	"	"	
$^1/_{15}$ "	"	"	"	"	
$^1/_{10}$ "	"	"	"	"	
$^1/_{6}$ "	"	"	"	"	

Perhaps longer periods of time would be desirable since many shops have only low-capacity transformers. The investigation would include (*a*) strength, (*b*) size of spot, (*c*) width and depth of depression, (*d*) crystal growth.

Another phase of the subject is to study the effect of a *given unit pressure* using electrodes of different diameters (pressure varying as the square of the diameter) with *given unit currents* (amperes per square inch of electrode face).

11. Short Circuiting Effects in Adjacent Spots. How close together can spots be welded without serious decrease in strength of the last weld under customary settings? How much is the current reduced in welding spots $^1/_4$, $^1/_2$, $^3/_4$ inch, etc., apart?

12. Interrupted Spot Welding of Thick Plates. It has been found possible to spot weld mild steel plates 1 inch thick with lapped surfaces by means of interrupted power impulses, such as 30 applications of power, each 60 cycles long, with 60 cycles off between impulses. If an attempt is made to spot weld thick plates with steady application of power, the first weld may be successful but shunt effect prevents subsequent welds on the same plates from being made. Why does application of power in impulses overcome the shunt effect? It is believed that, since a higher current and voltage can be used with the impulse method than with the continuous method, more intimate contact is made between the plates directly under the electrodes. Consequently, the shunt effect is reduced. Furthermore, the resistance of preceding spots may increase because the temperature rise from the first few impulses of a spot is transmitted to preceding spots. However, a detailed investigation remains to be made.

13. Film Breakdown in Resistance Welding. A chemisorbed film being present on many of the metals that are commonly resistance welded, e.g., oxygen on aluminum and iron, SO_2 perhaps or Cu_2O on copper, some research might be worth while on the mechanism of breakdown of the film when a heavy current is passed through, as in resistance welding. Films of different thicknesses could be prepared by exposure to different gases at different pressures and temperatures prior to welding. If applied to anodically oxidized aluminum treated for different periods of time, the study might lead to a solution of the effect of film thickness.

14. Spot Welding of Thick Sections of Steel and Other Metals. Calculation or experimental work on the difficulties inherent in spot welding thick sections is needed.

15. Inspection of Individual Spot Welds. Interest should be stimulated in an inspection device for measuring a voltage drop across the weld area and a voltage drop proportional to the secondary current on a standard spot welder. A pair of indicating devices or a combination thereof is needed which would show quantities proportional to each drop, or would indicate the amount of energy expended in the weld area. For a satisfactory weld this would be a definite quantity, that is, within limits. If the quantity shown is larger or smaller than the satisfactory value the weld should be classed as unsatisfactory. The general problem resolves itself around a means for indicating a voltage drop in a very short interval of time (0.01 second). This device would act as an automatic inspector for weld quality if suitable signals were attached.

16. Current Distribution in Spot Welding. There exists the problem of the distribution of current flowing from one electrode to the other especially across the interface of the sheets being welded. A knowledge of this distribution would assist the accurate calculation of temperatures reached during spot welding. The approach should include mathematical analysis of the distribution of pressure over the interface, because the pressure distribution affects contact resistance. The other important factor determining current distribution would be the mathematical analysis of the current distribution in a cylindrical conductor (representing the metal be-

tween the electrodes) by the methods developed by Steinmetz.

17. Optimum values of time, pressure and current for various thicknesses and finishes of different metals and alloys.

C—Metallurgical

1. The general problem of the effect of different contents of impurities and segregations thereof on the spot welding of common ferrous and non-ferrous alloys and their physical properties.

2. The effect of alloying additions on the properties of spot welds.

3. Effect of pressure and temperature on grain growth at elevated temperatures.

4. Accurately define the necessary properties for tips for different applications.

5. Development of better alloys, particularly of the precipitation hardening type.

6. Development of tips to reduce diffusion of molten metal into tip.

7. Effect of coatings, such as nickel, tin, zinc, lead, cadmium or chromium on spot welding of steel or brass. What are the properties and microstructure of the welds?

8. Different reductions by cold working of mild steel. It is known that too great mechanical work, with resulting loss of ductility, is detrimental to spot welding.

PHYSICS

1. Mechanism and rate of metal transfer in arc welding, including overhead welding and controlled atmospheres.

2. Laws governing magnetic disturbances in A.C. and D.C. arc welding and how these disturbing effects may be eliminated.

3. The effect of alloying elements, such as manganese, and of multi-constituent coatings on arcing characteristics of the electrode. The effect of a large number of single coating constituents being known now, it would seem most desirable to determine their combined effects as in commercial coatings. Perhaps a spectrographic study providing information on the excitation of coating components singly and in combination might usefully supplement the oscillographic mode of attack on stabilization problems.

4. D.C. and A.C. arc temperatures with various metals and alloys, in various atmospheres, and over a wide range of amperage.

5. Calorimetric Study of Carbon and Metal Arc and Gas Welding. This study would be of great practical value if the heating effect of various conditions on the material being welded could be determined quantitatively. Such results might lead to methods of predetermining amounts of shrinkage by mathematical analysis.

6. Theory of Combustion in the Oxyacetylene, Oxyhydrogen and Other Welding Flames, and Metal Transfer During Welding with These Flames. A spectroscopic examination of different regions of the flames using various gas ratios, with and without introduction of the welding rod, would probably suggest means for more efficient gas welding procedures.

7. The physics of the arc, particularly the welding arc, is too little known, as C. G. Suits has pointed out (THE WELDING JOURNAL, **17** (3) Supplement, p. 13, March 1938; (10) Supplement, pp. 35–38, Oct. 1938; (11) Supplement, pp. 63–64, Nov. 1938). The voltage gradient of the arc is one of its most important characteristics. Precision measurements are needed of the variation in gradient with current and of the potential distribution between electrode and metal to be welded. Calculations of time of deposition and means of promoting metal transfer in overhead welding, among other things, have been made by Engel and Sack. Further advance along these lines depends to a large extent on the accumulation of fundamental facts about the physics of the arc.

8. Non-Destructive Tests of Welds. (*a*) Correlation between X-ray photographs and specific gravity, nick-break test and physical properties. This investigation should include a quantitative check on several qualitative X-ray tests that are used in some quarters. The shape of inclusions, porosity and other defects may govern their effect on strength and ductility in the same manner as in gray and malleable cast iron.

(*b*) Investigation of the supersonic vibration method and its sensitivity to common defects in welds.

(*c*) Investigation of electrical and magnetic methods of weld testing with particular reference to fillet welds.

9. The Nature of the Bonding Force in Welding. If the base metal is not melted, as in brazing, the bonding forces are created by intercrystalline penetration of filler metal into base metal, and by diffusion into or absorption on base metal. During welding, the base metal is melted and the molten filler rod simply increases the volume of metal that subsequently solidifies. The problem is to study the nature and strength of the bond between base metal maintained at different temperatures and molten metal poured on its surface. Information obtained from these tests, admittedly difficult to control, might then be compared with the strength of joints in solid-solid and solid-gas "welding."

10. There is little information about the magnetic properties of welds in steel or other magnetic materials as a function of welding process and heat treatment. The magnetic characteristic of importance would depend on the material under investigation. Sometimes an apparatus depending on its magnetic properties is fabricated by welding. The effect of welding on the magnetic properties then assumes importance.

11. The minimum temperature at which pressure welds can be made in solid metals gradually is being lowered and the range of metals and alloys that can be so welded is being extended. It is not clear whether the impossibility of pressure welding metals below characteristic temperatures is related to films, high elastic limit or decreased atomic mobility in preventing complete contact of the surfaces to be welded. If atomic mobility is the main factor, the characteristic temperature might show some relation to the Periodic Table. If films are the difficulty, the surfaces should be placed in contact during or after electrolytic treatment or cathode sputtering.

12. Surface Tension of Metals in Contact with Fluxes. It would be desirable to develop general rules whereby the constituents required in a flux for welding or brazing could be predicted from a knowledge of the reaction of the constituents among themselves and of the effect of the reaction products and constituents on the surface tension of molten metals. There is a paucity of information on the surface tension of molten metals and alloys and on the effect of flux constituents, e.g., borax and alkali halides, on surface tension. The possibilities of gaseous fluxes have been explored in organic covered electrodes. The effect of arc and flame gases on surface tension should be investigated as well as the possibility of applying gaseous fluxes to operations now requiring solid fluxes, which invariably entail tedious washing operations.

WELDING PROCEDURE

Apart from proprietary methods there is much to be done on the most satisfactory procedures for welding metals and alloys by all processes. The conflicting statements that occur in recent literature on welding alloy steels and non-ferrous metals are proof that well-planned experimental work is urgently needed, including a study of welding variables as well as of physical properties and microstructure of the welds.

1. Speed of Welding and the Relation between Voltage and Amperage. A record of the power consumption at the arc with these tests would provide definite information along a direction in which very uncertain information now exists. There is an analogous problem in gas welding, namely, the relation between welding speed and gas consumption.

2. Proper Welding Procedure to Obtain Satisfactory Welds in Hydrogen and Other Gases. The experimental work on this problem need not involve the actual making and testing of welds since the chief problem is the amount of metal discharged from the tip of the electrode. Automatically controlled experimental conditions should preferably be adopted.

A careful study should be made of the amount of electrode melted down with different arc voltages from the lowest to the highest practicable and also with the lowest and highest current densities practicable for any size of electrode. Different combinations of experimental conditions should be tested and a record made of the rate at which the metal is melted and also the rate at which it is deposited. Bare and covered electrodes, ferrous and non-ferrous, should be investigated, along with D.C. as well as A.C. and atomic hydrogen.

The objects of these experiments would be to determine the nature of metal transfer, to determine if there is an optimum current density applicable for all sizes of electrodes, to discover the effect of electrode composition on deposition efficiency, as well as a number of other features which the experimenter would realize as the work progressed.

3. Effect of Peening on the Quality of Welds. Although it is known that peening reduces shrinkage stresses in welds in mild steel up to 50%, it is still a question whether peening has a favorable effect on impact or fatigue value. The temperature range in which peening is done and the point at which the blow is struck are the two major factors. Some believe that peening the weld metal is undesirable but that peening the base metal adjacent to the weld is helpful. It is suggested that experimental peening be performed with an air hammer supplied with a constant air pressure.

4. Capacity to Produce Resistance Welds of Constant Quality in Different Individual and Total Thicknesses of Material Using a Given Setting of the Machine. Tests to determine optimum current, pressure and dwell for all resistance-welding processes, as determined by physical properties and economy.

5. The effect of annealing above or below the critical point on the physical properties and machinability of welds. The tests should be directed particularly toward improving gas, arc and resistance welds in high-carbon and high-alloy steels and cast iron. The problem is particularly acute in welding two steels having widely different critical ranges.

6. Is the rippled appearance of fusion welds related to the temperature of the process, the size of the bead, the viscosity and surface tension of weld metal, the speed of welding, the temperature range of solidification of weld metal or other factors? Unionmelt and atomic hydrogen welds often are free from ripples, whereas beads from covered electrodes of steel or aluminum nearly always exhibit pronounced ripples.

7. The structure and properties of atomic hydrogen welds in magnesium and its alloys should be determined.

8. It has been said that the spacing required in fusion welding low-alloy steels varies with the yield strength. To what extent is the statement true, what are the relationships between yield strength and spacing, and does high yield strength imply low distortion in welding?

9. Unionmelt Welds. Butt welds made by Unionmelt process in mild steel $^1/_2$ and $1^1/_2$ inches thick should be examined for heterogeneity and reaction to heat treatment. By heterogeneity is meant the variation in chemical composition in different parts of the weld; for instance, the top center of the weld might have higher carbon, sulphur and phosphorus contents than other parts of the weld owing to segregation during solidification. The possibility of refining the grain size of weld metal by heat treatment, such as normalizing, should be investigated. The structure and properties of the heat-treated joints should be determined. Similar investigations should be made on Unionmelt welds in low-alloy structural steels. The static and fatigue strength of fillet welds made by the Unionmelt process should be determined. The distortion of, and shrinkage stresses in, parts welded by the Unionmelt process also should be determined.

10. Ductility Test for Fillet Welds. Sometimes the ductility of fillet welds is an important characteristic. To determine the ductility of the side fillet weld, the amount of relative motion of the component plates per unit length of weld to fracture might be utilized. A bend test might be devised using the specimen shown in Fig. 4, page 667, Welding Handbook, 1938 Edition. Load would be applied slowly, the surface of the weld would be in tension, and the measurement of distortion would be made over the surface of the fractured weld.

11. Silver soldering is a process requiring considerable research. The quantitative determination of the flowing properties of different silver solders on metals, such as mild steel, stainless steel, stainless iron, Monel metal, copper and brass, remains to be made. The fatigue and impact value of joints made under favorable and adverse conditions has not been investigated. It has been said that hard, brittle alloys, which may have high strength, should be used only in shear, and are not resistant to shock and fatigue. The soldering flux and atmosphere as sources of porosity deserve study. The need has been stressed recently for the standardization of conditions of soldering before reliable comparative figures can be obtained.

12. The structure, hardness, ductility and wear resistance of oxygen-machined surfaces should be investigated.

13. What steels, and what initial structures are unsuitable, in general, for flame heat treatment? Quantitative determinations of distortion in flame heat treatment should be made, including means for counteracting distortion possibly through the application of a tempering torch following the main heat treating-torch.

STRUCTURAL STUDIES

The greatest advantages to be obtained by the use of welding in fabricating structures of all kinds can only be obtained by adopting structural methods suited to welding. At present the influence of joining processes, such as nailing, pinning and riveting, which are basically

different from welding, is felt to too great an extent in welding design. But the best design methods for welded structures can only be developed on the basis of experimental studies of the stress distribution and shrinkage of welded joints. Recent literature on these topics show that considerable progress is being made, yet a number of fundamental problems are still unsolved.

1. Stress Distribution in Full-Scale Welded Structures. Experimental and mathematical analysis of stress distribution in beam and column connections and in built-up girders should be made in order to deduce reliable design formulas and to determine safe design practice. Experimental studies should take weld shrinkage into account.

2. Photoelastic Studies of the Stress Distribution in Welded Joints. Although the technique of the photoelastic study of welded joints appears to have been worked out, there are relatively few quantitative data on the magnitude of stress concentrations in typical welded connections; for example, transverse and diagonal butt joints with intermittent welds, several common marine types of lap joints, turned-up sheet welds, welded stiffening webs and plates in beams, crossing butt welds in all pressure vessels; pinned or riveted strap joints strengthened by welding, right angle butt welds. In addition to the information obtained from photoelastic models containing no defects, it would be useful to know the maximum increase in stress concentration created by typical defects such as blow-holes, undercut and faulty penetration. Thus, the maximum stress concentration should be determined in corner welds of various types with poor root penetration.

3. A photoelastic investigation might be made of the stresses in free bend specimens at various stages of bending. The stress condition in the shear test specimen suggested by G. J. Gibson (THE WELDING JOURNAL, 16 (7), Supplement, p. 10, July 1937) also might be determined by photoelastic means to determine to what extent shear stress prevails on the cross section of fracture.

4. An attempt should be made to correlate elongation measured in the free bend test with elongation measured in the tension test, particularly from the standpoint of elastic and permanent deformations at the reduced section in the tension specimen.

5. The Degree of Restraint That May Be Counted on in Welded Beam Connections. The quantitative study of rigidity in welded construction has been advanced by vibration studies of bridges and machine tools but a great deal of information is still required. A particular problem is the relative value of welding rotor hubs to shafts compared to press or shrink fits.

6. Reviewing the literature on welded structural connections, L. F. Denaro ("Welding of Steel Structures," Report of the Welding Panel of the Steel Structures Research Committee, London, 1938) states, "It is unfortunate that more detailed knowledge is not available about the behavior of other connections typical of present-day design." For example, not all of the large number of connections illustrated in Chapter 53 A of Welding Handbook, 1938 Edition, have been subjected to static load tests under laboratory conditions.

7. The structural engineer requires a great deal of information about shrinkage stresses and distortion. Accurate measurements of distortion should be made on welded structural elements prepared with different sequences of welding; for example, plate girders and truss details. Tests should be made on the utility of depositing a bead on the reverse side of a ribbed flange after the plate girder has been welded in order to counteract distortion. The effect on fatigue strength exerted by distortion and the means to counteract it should be kept in mind. Distortion measurements in welding require instruments of high precision. If the welding is performed manually there are likely to be considerable differences in results obtained with different operators. Measurements of distortion on typical welded structures should be made with a view toward unifying the procedure of measuring, interpreting and quoting distortion caused by welding.

8. Tests similar to these suggested in Problem No. 7 should be made in which shrinkage stresses were measured. The shrinkage stresses in a welded tank corner (three plates at right angles) should be investigated, particularly, with reference to different sequences of welding and to different methods of avoiding the corner welds by forming one or more of the plates. The relative value of the different methods of measuring shrinkage stresses in welding should be assessed.

9. A welded joint made under conditions of restraint contains shrinkage stresses due to the restraint, which are fairly uniformly distributed, as well as localized shrinkage stresses in the immediate vicinity of the weld, which are the result of inelastic deformation of the metal close to the weld during welding. To what extent, if any, are these two types of stresses relieved at room temperature? It is known that the stress is relieved at elevated temperatures to an extent depending on time of annealing. Some structural engineers believed that in the course of time, years perhaps, the shrinkage stresses in a welded structure release themselves. There is little experimental information on relief of shrinkage stresses in welded structural steel at room temperature. Tests should be made on elementary welded joints with and without constraint to determine the effect of rapid temperature changes between, for example, 66° and 74° F., and of vibration or light impacts on release of shrinkage stresses.

CHEMISTRY

1. The Chemistry of Fluxes and Coatings. (*a*) Viscosity, melting and boiling points, and surface tension of typical flux and coating compositions.

(*b*) Is the action of the coating on coated or shielded arc electrodes in excluding nitrogen mechanical or chemical? Experiments should be made in which a weld is made in air or nitrogen with a coated electrode, and then with a bare electrode using a flux of the same composition as the coating. The weld metal and slag in both experiments should be analyzed, particularly for nitrogen. If the action of the coating is mechanical, that is, if the volatilization of carbonaceous or other material forms a mechanical shield around the arc despite the turbulent conditions, then the second weld may be expected to contain nitrogen. If the action is chemical both welds will be free from nitrogen and the gases around the arc (which might be analyzed perhaps partly by spectroscope) or the slag itself will contain stable nitrogen compounds.

2. Corrosion of Welds. The recent review of literature on the corrosion of welds shows that the following problems require study:

(*a*) The rate of corrosion of all kinds of welds in various atmospheres and media at different temperatures.

(*b*) The theory of the corrosion of welds. Some of the factors that are said to affect the corrosion properties of welds are: (1) rough surface of weld; (2) difference in electrode potential of weld metal due to difference in chemical composition, in microstructure and in other properties; (3) slag content in and around weld metal; (4) highly stressed condition of unannealed weld

metal and adjacent base metal. The relative importance of these factors should be estimated for welds in ferrous and non-ferrous materials.

(*c*) Procedures for testing the corrosion properties of welds. Accelerated methods are not necessarily implied. A procedure for obtaining the maximum measurable corrosive effect under service exposure is required, using specimens of convenient size prepared under normal welding conditions.

(*d*) The effect of corrosion on the mechanical properties of welded joints in heat-treatable alloys. Welding sometimes sensitizes the heat-affected zone of alloys to intergranular or accelerated corrosion. The problem has been studied to exhaustion in 18–8 and closely related alloys but has been neglected in light metals and copper alloys.

METALLURGICAL STUDIES

1. Study of Series of Alloy Steels Varying by Only One Element, Such as Nickel, Chromium, Molybdenum and Vanadium. Determinations of hardness and bend ductility should be made. It should be remembered that alloy steels are not welded indiscriminately by the same procedure; in experimental work it is essential that changes in procedure be studied before conclusions are drawn.

2. Effect of Preheat Temperature on Hardness Produced During the Weld Quench, and Arrangement of Alloys into Groups Depending on the Preheat Required to Maintain a Standard of Bend Ductility Adjacent to the Weld.

3. Flame Hardening with the Oxyacetylene Torch, Using Alloy Steels (Air and Water Hardening) and Possibly Non-Ferrous Alloys, Such as Duralumin and Beryllium-Copper. The metallurgical effects are different in spot and straight line hardening and especially in closed circle hardening where the end effect is important.

4. Surface Tension Effects of Different Types of Weld and Brazing Metal on Steel, Copper and Alloys in General. Surface tension, which has a large influence on penetration and weldability, should be studied with strict control of atmosphere and metal composition, and with adequate consideration of fluxes and slags.

5. The Metallurgy of Welding Difficult Metals and Alloys Under Water or Other Liquids, Such as Carbon Tetrachloride.

6. A Monel metal rod containing 5% Al, 0.5% Ti has been recommended for welding Monel metal. What are the properties of the weld metal and what effect has heat treatment?

7. An important question in some types of resistance welding and in solid-solid welding by pressure is the effect of pressure on grain growth and recrystallization in metals.

8. It has been found (Metals Handbook, 1939 Edition, p. 1059) that welds are unsatisfactory for gas carburizing. Satisfactory results also have been obtained (THE WELDING JOURNAL, **18** (3), pp. 163–166 (1939)). An investigation should be made of oxyacetylene, arc and atomic hydrogen weld metal (plain carbon and alloyed) and of oxygencut and resistance welded surfaces that have been pack, gas or liquid carburized. Response to heat treatment after carburizing should be studied.

9. The Metallography of Austenitic Welds in Pearlitic Cast Iron. A microscopic and hardness survey of welds (cold and preheated) made with several types of austenitic filler rods in a single common grade of pearlitic cast iron would reveal the sources of defects as well as the probable field of application of each rod. A related problem is the metallographic study of welds made with high-chromium rods to determine the extent and effects of nitrogen pick-up, either from the atmosphere or from high-nitrogen base metal.

10. Assembling Complicated Magnets of Nickel-Aluminum and Other Magnet Steels by Fusion Welding Followed by Suitable Heat Treatment.

11. Carbon Migration in Flame Cutting. The literature of 1937 contains at least two references to the alleged fact that during flame cutting of plain carbon steels, especially over 0.30% C, some carbon from the interior of the steel diffuses to the layer of steel just back of the cut edge. This carburized layer is quite distinct from carbon that may be derived from the preheating flame. In other words, the flame-cut surface of a plain carbon steel cut with a preheating jet containing no carbon, an oxy-hydrogen preheating flame, for example, will contain appreciably more carbon than the body of the steel, which will have supplied the increase at the edge. Presumably the hypothesis is that the layer of steel adjacent to the cut edge is heated into the austenite region during flame cutting, and austenite, having a greater solubility for carbon than ferrite, absorbs carbon from the ferrite with which it is in contact. The hypothesis has not been refined to the extent of considering the Delta region in which the carbon solubility is also small, unless the statement that the carburizing effect is particularly marked in steels containing over 0.30% C may be so interpreted. A few carbon analyses on selected cuts probably would settle the question once for all.

12. Grain-Size Characteristics of Welds, Particularly in Steel. Beyond the fact that welded joints in structural steel made by gas or arc processes may be fine grained or coarse grained in weld metal and heat-affected zone depending principally on the number of passes, very little is known about the grain-size characteristics of present-day weld metals. It is not safe to apply the data available on grain size of open hearth, bessemer or electric steel to weld metal of similar composition because grain size characteristics are notably dependent on melting practice. The conditions under which weld metal is produced are radically different from those prevailing in the steel furnaces. In the steel furnaces the slags are either predominantly basic or predominantly acid, and are made up principally of CaO, SiO_2, FeO and MnO. Welding slags are usually of an entirely different nature, the deoxidizer frequently being titanium or other unusual element.

13. Hard Facing. Some of the questions in hard facing are:

What is the effect of thickness of layer on the bend value of faced surfaces?

What is the fatigue strength of hard-faced bars?

What shrinkage stresses are induced in hard-faced parts, and what heat treatment or mechanical treatment may stress relieve them?

What is the extent of penetration of hard facing into base metal and vice versa (quantitative as a function of facing procedure)?

What is the hardness of hard-faced parts as a function of thickness of deposit and method of hardness testing?

What is the thermal coefficient of expansion of hard-faced bars?

What happens to the wearing properties of a hard-faced part when the base metal is exposed in spots?

THE ENGINEERING FOUNDATION

Welding Research Committee

Sponsored by the American Welding Society « « » » and American Institute of Electrical Engineers

Supplement to the Journal of the American Welding Society, July 1939

OXYGEN CUTTING OF STEEL

Part I—Mechanical Properties and Metallurgy

A Review of the Literature to January 1, 1939

By W. SPRARAGEN* and G. E. CLAUSSEN†

This report is prepared under the auspices of

THE LITERATURE DIVISION OF THE ENGINEERING FOUNDATION
WELDING RESEARCH COMMITTEE

TABLE OF CONTENTS

	Page
Summary	217-s
Introduction	219-s
Properties of Oxygen-Cut, Plain Lower-Carbon Steels	220-s
Tension Tests	220-s
Bend Tests	221-s
Fatigue Tests	224-s
Hardness	224-s
Impact Tests	226-s
Fabrication Qualities	227-s
Corrosion Tests	227-s
Shrinkage Stresses	228-s
Metallurgy	228-s
Heat-Affected Zone	228-s
Carbon Migration	230-s
Composition of Slag	233-s
Segregations	235-s
The Effect of Carbon on the Oxygen Cutting of Higher-Carbon Steels	235-s
Preheating and Annealing	235-s
Cutting Speed	236-s
Mechanical Properties	237-s
The Effect of Alloying Elements on the Oxygen Cutting of Steel	238-s
Phosphorus	238-s
Cr-Cu-Si-P	238-s
Sulphur	239-s
Nitrogen	239-s
Aluminum	239-s
Molybdenum	239-s
Silicon	239-s
Si-Mn	239-s
Manganese	239-s
Ni-Mn	241-s
Cu-Si-Mn-Mo	241-s
Chromium	241-s
Nickel	242-s
Cr-Ni	244-s
Vanadium	244-s
Copper	244-s
Cu-Ni-Mo	244-s
Tungsten	244-s
Welding on Oxygen-Cut Scarves	244-s
Tests	245-s
General Observations	246-s
Acknowledgments	247-s
Bibliography	247-s
Suggested Research Problems	248-s

NOTE: In accordance with the Welding Handbook, 1938 Edition, p. 280, the term "oxygen cutting" is used instead of "flame cutting" to denote the process of severing steel with a jet of oxygen fortified by a preheating flame from the cutting torch. In lance cutting the preheating flame either is discontinuously applied or is absent. Flame cutting applies to the severing of non-ferrous, non-metallic and sometimes ferrous materials with a flame, with or without a jet of gas to blow away the molten metal. In arc cutting the flame is replaced by an arc. Oxygen deseaming and oxygen machining are processes utilizing a torch similar to the oxygen-cutting torch for the purpose of removing metal (drilling, planing, etc.) without severing. The corresponding processes for non-ferrous and non-metallic materials may be called flame deseaming and flame machining, but they have no application at present. Some authorities favor the abbreviation of the term "oxygen cutting" to "oxy-cutting."

Summary

PROPERTIES OF OXYGEN-CUT, PLAIN LOWER-CARBON STEELS

Tension Tests

The tensile strength of specimens smoothly oxygen cut from mild steel containing up to 0.20 C is increased slightly, possibly from two to three per cent compared

* Secretary, Welding Research Committee.
† Research Assistant, Welding Research Committee.

with machined specimens. The yield strength is raised four to ten per cent. The ductility (elongation and reduction of area) is only slightly affected. Oxygen-cut edges are definitely superior to sheared edges in tension tests. The preheating fuel—acetylene, coal gas or hydrogen—has little or no effect on the tensile properties of the cut. If the plates are thin or if the successive cuts are in close proximity to each other, the heat may cause a slight annealing effect tending to lower the tensile strength and yield point of the affected steel by a few per cent with corresponding increase in ductility.

Bend Tests

Specimens that have been flame cut from mild steel less than 0.23 C may be bent 180° flat without cracking. In this respect they display the same characteristics as machined specimens and display superior qualities to sheared specimens which usually can be bent only 90° before cracking. The bend ductility of oxygen-cut mild steel is at least equal to that of machined specimens. Semikilled and killed steel displayed slightly inferior properties when oxygen cut compared to the rimmed steel. Smoothness of cut is particularly important in determining angle of bend before cracking. The angle to which specimens of structural steel (0.27 C, 0.63 Mn) punched for riveting may be bent without cracking at the edges of the holes is considerably reduced if the steel contains segregations.

Fatigue Tests

The decrease in pulsating tension and pulsating bending fatigue strength of steel with tensile strengths of 60,000 to 100,000 lb./in.^{2} due to oxygen cutting was nearly as great as the decrease due to a smooth hole, and was due to the roughness of the oxygen-cut surface compared with machined surfaces. The fatigue strength of the oxygen-cut surface was approximately 60 to 80% of the fatigue strength of machined or mill scale surfaces. The heat effect in oxygen cutting had little, if any, effect on fatigue strength. Removing ridges by grinding materially improved the fatigue properties.

Hardness

The average increase in hardness at the edge of oxygen cut mild steel up to 2 inches thick is about 70 Vickers units (maximum 120 Vickers units). The hardness effect decreases rapidly so that it is barely detectable at a depth of 0.05 inch. In general the hardness increases as both thickness and carbon content increase. Preheating the high-carbon steels greatly decreased the hardness of the cut surface. Annealing above 500° C. reduced the hardness of all oxygen-cut surfaces to that of unaffected base metal.

Impact Tests

Oxygen-cut specimens of mild steel have practically the same notch-impact value as carefully machined specimens. Poor steel coupled with poor workmanship give inferior results.

Fabrication Qualities

Oxygen-cut surfaces in mild steel containing not more than about 0.30 C offer no difficulties to machining, caulking or carburizing.

Corrosion Tests

Tests in tap water and artificial sea water indicate superior corrosion resistance of oxygen-cut surfaces. On the other hand, localized corrosion has been observed in some concentrated acids.

Shrinkage Stresses

There is no reliable quantitative information on residual stresses created by oxygen cutting. Preheating (500–700° C.) or stress annealing (600° C.) removes shrinkage stresses in oxygen-cut steel.

METALLURGY

Heat-Affected Zone

As the cutting speed increases, the depth of heat-affected zone decreases. The depth of the heat-affected zone decreases with decrease in acetylene consumption and with increase in oxygen purity.

The depth of the heat-affected zone revealed by metallographic etching increased as the carbon content was increased. One investigator found that the heat-affected zone in oxygen-cut steel containing 0.4 C was 0.055 inch deep (no details) and that there were no cracks. There was no difference between the use of city gas and acetylene for preheating as far as the depth of heat-affected zone is concerned. In oxyacetylene cutting mild steel the depth of heat-affected zone revealed by microstructure increases directly with increase in thickness of cut up to 8 inches. In steel thicker than 8 inches the depth increases less rapidly with increase in thickness.

Carbon Migration

For want of a better term, "carbon migration" has been adopted to express the higher carbon content that has been found in oxygen-cut surfaces than in unaffected base metal. Although the majority of investigators detected higher carbon content in the cut surface than in unaffected material, they are not in agreement about the extent of the increase or its causes.

Segregations

Cracking of oxygen-cut surfaces occurs mainly in dirty or defective steel. Besides tending to create cracks, segregations of slag roughen the cut.

Segregations of carbon in steel (not cast iron) have little effect on flame cutting. Notwithstanding, it is common practice in cutting heavily case carburized armor plate (case several inches deep) to use the torch on the soft side.

THE EFFECT OF CARBON ON THE OXYGEN CUTTING OF HIGHER CARBON STEEL

Preheating and Annealing

Steels with over about 0.30 C should be preheated before flame cutting. Unless preheating is applied, surface cracks may be found in oxygen-cut steel containing over 0.3 C. The International Acetylene Association recommended reheating to 620° C. or preheating to 430° C. to reduce the hardness of oxygen-cut surfaces in higher carbon steels. Preheating and reheating may be accomplished with flame softening torches.

Cutting Speed

The preheating flame must be increased as the carbon content of the steel is raised.

Mechanical Properties

The effect of increasing the carbon content from 0.28

to 0.40% in steel to be subjected to oxyacetylene cutting, is to increase the hardness and decrease the ductility. There may be carburization of the cut surface to a depth of 0.002–0.003 inch and hardening to a depth of about 0.05 inch. Flame softening, which consists of preheating or reheating the line of the cut with a flame softening torch, decreased the hardness and increased the bend ductility.

THE EFFECT OF ALLOYING ELEMENTS ON THE OXYGEN CUTTING OF STEEL

Phosphorus

Phosphorus apparently has no effect on oxygen cutting. The hardness and bend ductility of production machine oxygen cuts in a special alloy steel of high phosphorus content with Cr, Cu and Si steel were found to be satisfactory in thin material but not so satisfactory in thick material.

Sulphur

The oxygen cutting of plain carbon steel was not affected by sulphur.

Aluminum

Calorized steels were easy to cut if the calorized layer was relatively thin (no details). Steel with 10–15% Al could not be oxygen cut even if both oxygen and steel were preheated. Pure aluminum could not be oxygen cut.

Molybdenum

Pure molybdenum does not cut so rapidly as iron; 5.5% Mo added to a steel containing 8 W, 1.4 Cr, 1 C prevented it from being cut by the oxyacetylene cutting torch. Special techniques have been developed for cutting the steel by a process of melting and oxidation.

Silicon

One investigator was able to oxyacetylene flame cut transformer iron with 4% Si and very low carbon, but the operation was slower than with mild steel. If, however, 0.2 C was present with 4 Si, the steel could not be oxygen cut. High silicon iron (15 Si, 0.5 C) and silicon itself could not be oxygen cut with or without preheat.

Si-Mn

It is usually necessary to heat treat the cut surface of steel containing 0.40 C, 1.51 Mn, 2.00 Si, in order to reduce its hardness.

Manganese

Oxygen-cut specimens of steel containing 0.12 C, 1.00 Mn, 0.62 Si, 0.26 Cu have excellent strength and ductility. On the other hand, flame softening is desirable with steel containing 0.35 C, 1.43 Mn and 0.20 Si. Oxygen-cut 14 Mn steel should be heat treated by heating the part uniformly and quenching from a high temperature.

Ni-Mn

One investigator had no difficulty oxygen cutting steel $^1/_8$ to $^3/_8$ inch thick containing 0.25–0.40 C, 10–12 Ni, 6–8 Mn.

Chromium

With up to 3.5 Cr there is no difficulty in oxidizing the metal during oxygen cutting. The steels with high carbon and chromium (up to 10% Cr) are more difficult to cut, are air hardening, and must be heat treated after cutting to soften the kerf. Steels containing over 10% Cr can be cut with a special technique involving melting and oxidizing.

Nickel

The S. A. E. nickel steels of the 2000 series can be oxygen cut cold unless the carbon is above 0.30–0.35%, in which case preheat to 260–315° C. (500–600° F.). If the carbon content is above 0.50, preheat to 260–315° C., and anneal at 790–820° C. (1450–1500° F.) after cutting. Nickel steels are not difficult to cut. It was found necessary to intensify the preheating flame to cut the nickel steels compared with unalloyed, low-carbon steel.

Vanadium

Low-carbon vanadium steels should be heat treated after oxygen cutting.

Tungsten

The oxygen cutting of tungsten steels is not difficult, if the tungsten content is not above 10% with or without 5% Cr at 0.8% C. Steel with 17 W can be oxygen cut only if it is preheated and the oxygen pressure is increased. Steels with over 20% W cannot be oxygen cut even if the steels are preheated.

WELDING ON OXYGEN-CUT SCARVES

There is no difficulty in welding on oxygen-cut scarves if a welding process is used that fluxes the scale. The physical properties of welds are excellent with freedom from porosity. The situation is different when using bare or lightly coated wires. A band of oxide inclusions in the junction zone corresponding to the oxygen-cut scarf was observed in the microstructure of these welds. In general it is best to remove slag and thoroughly clean the cut surface. Machining is not necessary.

Part I—Mechanical Properties and Metallurgy

INTRODUCTION

AS THE first part of a review of literature on Oxygen Cutting, the following sections summarize available information on the properties of oxygen-cut, plain carbon and alloy steels and on the metallurgical characteristics of the cut surface. The properties of oxygen-cut mild steels appear to be little different from the properties of machined mild steel. The metallurgical characteristics of the cut surface become important only in the higher carbon and alloy steels, the chief effect of carbon being in hardening the cut edge and reducing its ductility. In alloy steels the effect of carbon may be accentuated and the alloying element may hinder the process of oxygen cutting. Much of the information on oxygen-cut alloy steels appeared in previous reviews, all of which is repeated herein.

PROPERTIES OF OXYGEN-CUT, PLAIN LOWER CARBON STEELS

Tension Tests

1937—Hessler[1]

Oxyacetylene machine cuts (oxygen pressure = 57 lb./in.^{2}, tip distance = 0.08 inch) were made in steel 0.79 inch thick containing 0.095 C, 0.44 Mn, 0.032 S, 0.024 P and were tested 38 days later to permit completion of any possible aging. The tension specimens were 1.15 inches wide. The specimens with oxygen-cut sides, Table 1, had slightly lower elongation and slightly higher yield strength than machined specimens.

Table 1—Tensile Properties of Oxyacetylene Machine-Cut Mild Steel Transverse to Direction of Rolling, Averages of 3 Specimens. Hessler[1]

	Oxyacetylene Cut	Machined (No Details)
Yield strength, lb./in.^{2}	31,600	29,000
Tensile strength, lb./in.^{2}	51,000	50,500
Elongation, % in $5^1/_2$ inches	31.6	35.1
Elongation, % in 8 inches	28.2	31.6
Reduction of area, %	66.0	65.3

1937—Dümpelmann[2]

Tension specimens with "very smooth" surfaces were machine cut from steel 0.79 inch thick containing 0.18 C, 0.47 Mn, 0.10 Si, 0.12 Cu, 0.018 S, 0.023 P using acetylene or hydrogen for preheating. Comparison with specimens prepared entirely by milling, Table 2, was slightly in favor of the oxygen-cut specimens both in strength and ductility. Distance between oxygen cuts (0.79 to 1.58 inches) had no obvious effect nor was there any difference between acetylene and hydrogen for preheating.

1937—*Machinery (London)*[4]

Without supplying details, it was stated that flat tensile specimens with oxygen-cut edges had a tensile strength of 58,000 lb./in.^{2}, whereas sheared specimens of the same steel had a tensile strength of 55,000 lb./in.^{2}

1933—Clarke[5]

Tensile specimens 1.5 inches wide, $^3/_8$ inch thick were cut from mild steel (134–156 Brinell) by band saw or with cutting torch, using acetylene or coal gas for preheating. The oxygen-cut specimens had higher strength and ductility than the sawn specimens, Table 3. There was no change in the properties of the sawn specimen if the cuts were filed smooth.

Table 3—Tensile Properties of Mild Steel Cut on All Four Sides. Clarke[5]

Specimen	Yield Strength, Lb./In.^{2}	Tensile Strength, Lb./In.^{2}	Elongation, % In 2 Inches	Elongation, % In 8 Inches	Maximum Brinell Hardness
Oxyacetylene cut	36,100	61,700	30	17	217
Oxy-coal gas cut	37,000	61,400	33.5	17.5	207
Band saw cut	33,600	58,800	26	13.5	196

1931—Hovey[6]

Wind-bracing connections $18^3/_4$ inches perpendicular to the flange, 16 inches parallel to the flange, with two rows of four holes each, $^{15}/_{16}$ inch diameter, were oxyacetylene cut from a single 24-inch, 15.09 lb. I-beam of normal structural quality but rather softer than average. Three sets of specimens (two specimens in each set) were tested in tension at the Bureau of Standards: (1) holes punched, edges then oxygen cut; (2) holes drilled, edges then oxygen cut; (3) holes punched, edges then oxygen cut and planed. All burrs were filed flush with the surface of the web without rounding the holes.

Upon slow application of load, strain lines were observed first in the vicinity of the holes farthest from the flange, then successively at the other lines of holes. Fractures commenced at the sides of the holes and progressed to the edges and middle. All three types of specimens had the same yield point but the specimens with drilled holes had greater ultimate strength elongation and reduction of area (no details) than those with punched holes. Planing the oxygen-cut edges did not increase either yield point or ultimate strength.

1929—Wiss[7]

Tensile specimens were milled or oxyacetylene machine cut from three thicknesses (0.20, 0.39 and 1.18 inches) of mild steel plate containing 0.1–0.2 C. The widths of the specimens were 0.20, 0.39 and 1.0 inch for the plate 0.20

Table 2—Tensile Properties of Milled and Oxygen-Cut Boiler Plate, Averages of 3 Specimens. Dümpelmann[2]

Breadth of Specimens, Inch		Acetylene as Preheating Fuel 0.79 Milled	0.79 Oxy-Cut	1.18 Milled	1.18 Oxy-Cut	1.58 Milled	1.58 Oxy-Cut	Hydrogen as Preheating Fuel 0.79 Milled	0.79 Oxy-Cut	1.18 Milled	1.18 Oxy-Cut	Averages All Milled Specimens	All Acetylene Cut Specimens	All Hydrogen Cut Specimens
Yield strength, lb./in.^{2}	(L)	35,700–38,200	43,500–44,200	37,700–38,700	41,500–42,300	37,400–41,000	39,100–45,200	35,700–38,200	44,500–45,100	37,700–38,700	42,200–45,800	35,800	42,400	44,400
	(T)	34,000–36,200	44,400–45,400	38,000–38,400	42,400–43,500	33,000–39,500	40,600–44,400	34,000–36,300	42,800–45,400	38,000–38,400	41,000–43,400	36,400	43,600	43,100
Tensile strength, lb./in.^{2}	(L)	60,300–61,900	62,600–64,900	60,900–62,700	63,000–64,000	61,700–63,600	61,400–64,300	60,200–61,900	64,100–64,800	60,900–62,700	62,700–65,400	61,900	63,300	64,300
	(T)	60,100–63,900	63,900–64,000	60,500–60,900	63,200–63,700	61,300–62,100	62,600–62,900	60,100–63,900	62,600–62,900	60,500–60,900	62,000–65,000	61,600	63,300	63,000
Elongation, % in 5.65 $\sqrt{A}$	(L)	23.2–29.2	28.7–30.6	28.1–29.2	29.9–32.9	24.6–28.6	27.2–30.6	23.2–29.2	30.2–32.7	28.1–29.2	26.4–28.6	27.4	30.2	29.7
	(T)	26.2–28.6	29.3–32.8	30.0–31.9	29.7–31.4	20.7–31.0	21.7–29.5	26.2–28.6	30.7–32.9	30.0–31.9	22.9–33.4	29.4	29.1	29.2
Reduction of area, %	(L)	56.1–56.5	56.8–59.0	51.1–55.3	53.6–55.0	52.2–54.2	52.0–55.7	56.1–56.5	55.7–58.8	51.1–55.3	53.9–55.1	54.5	54.7	55.7
	(T)	47.6–51.7	49.0–53.8	49.3–51.1	48.6–50.9	47.1–50.0	48.4–50.9	47.6–51.7	52.5–55.3	49.3–51.1	51.2–53.7	49.4	49.9	53.4

(L) = Parallel to direction of rolling.
(T) = Perpendicular to direction of rolling.
(A) = Cross-sectional area, sq. mm. gage lengths were:
5.3 inches for specimens 0.79 inch wide
6.5 inches for specimens 1.18 inches wide
7.1 inches for specimens 1.58 inches wide

inch thick; 0.20, 0.39 and 0.79 inch for the plate 0.39 inch thick; and 0.39, 1.0 and 1.6 inches for the plate 1.18 inches thick. During cutting, the narrowest strip from the plate 0.20 inch thick became red-hot. The results were expressed in percentage of the milled specimens.

With plate 0.20 inch thick the width of the specimen had little effect. The yield strength was 90 to 100% of milled, the tensile strength 95 to 105% of milled, and the elongation and reduction of area was 100 to 105% of milled. With plate 0.39 inch thick as the width increased, yield strength and tensile strength increased from 96 to 104% of milled while the elongation and reduction of area decreased from 102 to 93% of milled. With plate 1.18 inches thick as the width increased, the tensile strength decreased from 104 to 102% of milled and the yield strength decreased from 110 to 105% of milled, but elongation also decreased from 92 to 82% of milled, and reduction of area decreased from 100 to 90% of milled.

1929—Dawson and Miller[8]

Tensile specimens oxyacetylene cut from structural members or plates had higher strength and, on the whole, slightly lower ductility than equivalent milled specimens, Table 4.

1929—Miller[9]

Tests made on sheared, milled and oxygen-cut plates (no details) showed that oxygen-cut specimens had the same properties as milled and better properties than sheared.

1928—*Railway Engineer*[10]

Oxyacetylene or oxy-coal gas cutting did not affect the strength of mild steel (no details) tensile specimens 10 inches long, about 1 inch square, but raised the elongation in 3 inches from 31% (machined) to 35–38%.

1928—Bureau of Standards[145]

Wind connections 8 inches wide were oxygen cut from a standard 24-inch, 105-lb. I-beam (A. S. T. M. A7–24). Two rows of rivet holes 1 inch diameter were punched in the web either before or after oxygen cutting. The center lines of the holes were 2 inches from the oxygen-cut edges. In tension tests the specimens exhibited a yield strength of 32,000 lb./in.^{2} (gross area), and failed through the holes with 13% reduction of area. There was no appreciable difference between the specimens punched before and after oxygen cutting.

1928—McEwen[11]

Tests (no details) on structural steel beams with slotted holes to take bracing that were drilled or punched or oxygen cut revealed that the oxygen-cut steel was the strongest.

1912—*Welding Journal (London)*[12]

Oxyacetylene-cut tensile specimens of open-hearth steel plate, $^3/_4$-inch thick, had 3000 lb./in.^{2} higher tensile strength and greater ductility (no details) than sheared specimens.

Discussion.—Tensile specimens oxygen cut from steel containing up to 0.20 C, 0.52 Mn have the same strength and ductility as machined specimens, assuming good workmanship. The yield strength of oxygen-cut specimens is ordinarily up to 10% higher than machined specimens. The width of the specimen (distance between cuts) in low-carbon steel exerts no effect. The cross section of narrow specimens has a larger proportion of heat-affected material than thick specimens, but the average temperature of the narrow specimen during cutting and cooling is higher than that of wide specimens.

The irregularity of oxygen-cut edges in mild steel is insufficient to create significant stress concentrations in static tests. If the oxygen-cut surface is the boundary of a region of stress concentration, such as the vicinity of a hole, the ductility may suffer.

Comparison of elongation and reduction of area of rectangular specimens of different cross sections must be accompanied by the realization that both quantities are functional of the shape and size of the specimens as well as of the material. The quotation of ductilities as percentage of uniformly machined specimens of identical size is a satisfactory method of comparison between oxygen-cut and machined specimens.

1910—Baumann[152]

Manual oxygen-cut specimens of Boiler Plate 0.39 and 0.79 inch thick with cut surfaces machined smooth had the same yield and tensile strengths and ductility as machined specimens.

Bend Tests

1937—Hessler[1]

The steel described in the section on Tension Tests was cut into bend specimens 1.18 inches wide, 10 inches long, which were placed on rolls 4 inches diameter, 4 inches apart, were bent 180° with a plunger 1.18 inches diameter, and then were pressed flat. A cut edge was in tension. The average of three specimens, Table 5, showed that the oxyacetylene-cut specimens had slightly higher ductility than machined specimens. No cracks were observed.

Table 4—Tensile Properties of Milled and Oxyacetylene Cut Mild Steel. Dawson and Miller[8]

Specimen	Dimensions of Specimen, Inch	Type of Cut	Yield Strength, Lb./In.^{2}	Tensile Strength, Lb./In.^{2}	Elongation, % in 8 Inches	Reduction of Area, %
14-Inch column	1.4 x 1.0	Milled	31,300	58,200	30.5	56.8
	1.5 x 1.0	Oxy-cut	36,100	60,300	27.0	51.8
24-Inch I-beam	1.5 x 0.625	Milled	28,000	50,700	32.2	57.2
	1.5 x 0.625	Oxy-cut	32,500	53,000	28.2–29.1	58.8–61.2
$^3/_8$-Inch plate	1.5 x 0.39	Milled	35,700	57,500	27.5–29.9	55.9–64.8
	1.5 x 0.39	Oxy-cut	37,000–39,000	56,900–59,600	27.0–28.9	60.8–67.5

Composition of Steel

	C	Mn	Si	S	P
Column	0.20	0.71	0.03	0.038	0.039
I-Beam	0.20	0.52	0.02	0.037	0.035
Plate	0.22	0.37	0.01	0.031	0.040

Table 5—Bend Ductility of Oxyacetylene Cut Mild Steel. Hessler[1]

	Oxyacetylene Cut	Machined
Elongation, % in 0.79 inch	98.3	96.7
Elongation, % in 1.58 inch	85.0	83.3
Elongation, % in 4 inches	53.7	53.0

1937—Dümpelmann[2]

The steel described in the section on Tension Tests was cut into bend specimens 0.79, 1.00 and 1.18 inches wide. With the rolled surface in tension all specimens (three for each test) bent flat 180° without cracks. Oxyacetylene-cut specimens were quenched in water at 28° C. after 1 hour at 610° C., and subjected to a bend test with the cut surface in tension. The slag was brushed off and the edges rounded with a file. All specimens (three for each test) bent flat 180° without cracking.

1937—International Acetylene Committee[3]

Free bend tests on plain carbon steel boiler plate 3/8 inch thick (no details) with the rolled surface in tension, Table 6, showed that oxygen cutting had no effect on bend ductility.

Table 6—Free Bend Tests on Sheared and Oxygen Cut Boiler Plate[3]

Dimensions of Specimen, Inch	Type of Cut	Cracks	Bend Elongation, %
5/8 x 8	One side sheared, the other side oxyacetylene machine cut	In sheared side	48
1 1/2 x 8	One side sawn, the other side oxyacetylene machine cut	None	52
1 1/2 x 8	One side sheared, the other side oxyacetylene manual cut	In sheared side	52

1937—*Machinery (London)*[4]

Under identical conditions 8% greater load was required to bend mild steel specimens with oxygen-cut edges 180° flat than specimens with sheared edges (no details).

1935—Zimmerman[13]

Cold bend tests were made on specimens 1/2 x 1 x 7 inches with the 1/2-inch face oxyacetylene machine cut. Four steels, Table 7, were investigated in three thicknesses: 1/2, 1 and 2 inches. The cut surface was normal to the pin, which was 1/2 inch diameter for plates 1/2 and 1 inch thick, 1 inch diameter for steels C and D 2 inches thick, and 1 1/4 inches diameter for steels A and B 2 inches thick. Some specimens of the thick plates were cut from mid-thickness, others from the surface of the plate. The edges were not rounded.

All specimens withstood 180° bend around the pin without cracking. In addition, steels C and D and the 1/2- and 1-inch plates of steels A and B could be flattened after the 180° bend without cracking. A specimen from the surface layer of steel B 2 inches thick (exact composition was 0.18 C, 0.50 Mn, 0.04 Si) cracked during flattening, and specimens from the surface and mid-thickness of steel A 2 inches thick (exact composition was 0.21 C, 0.52 Mn, 0.08 Si) also cracked during flattening.

1934—Zimmerman[14]

The results of cold bend tests around a pin 1/2 inch diameter on machined, sheared, sawn and oxygen-cut, hot-rolled steel 1/2 inch thick (0.24 C, 0.46 Mn, 0.10 Si, 0.033 S, 0.017 P, tensile strength = 61,400 lb./in.^{2}, yield strength = 35,700 lb./in.^{2}, elongation = 30% in 2 inches, bend angle = 180° flat) are shown in Table 8. The corners of the specimens were not rounded, and it was not stated where the crack started. The oxygen-cut specimens were not quite so ductile as the machined specimens.

Table 8—Cold Bend Tests of Mild Steel 1/2 Inch Thick. Zimmerman[14]

Type of Cut	Direction of Rolling	Edge of Cut in Tension	Angle of Bend Around 1/2-Inch Pin	Appearance After Flattening
Machined	Longitudinal	Any	180	No cracking
Machined	Longitudinal	Any	180	No cracking
Machined	Transverse	Any	180	No cracking
Machined	Transverse	Any	145	Cracked
Sheared	Longitudinal	Top	95	Cracked
Sheared	Longitudinal	Bottom	95	Cracked
Sheared	Transverse	Top	60	Cracked
Sheared	Transverse	Bottom	55	Cracked
Friction sawed	Longitudinal	Top	90	Cracked
Friction sawed	Longitudinal	Bottom	90	Cracked
Friction sawed	Transverse	Top	80	Cracked
Friction sawed	Transverse	Bottom	70	Cracked
Manual Oxygen-cut	Longitudinal	Top	180	No cracking
Manual Oxygen-cut	Longitudinal	Bottom	150	Cracked
Manual Oxygen-cut	Transverse	Top	100	Cracked
Manual Oxygen-cut	Transverse	Bottom	55	Cracked
Machine Oxygen-cut	Longitudinal	Top	180	No cracking
Machine Oxygen-cut	Longitudinal	Bottom	180	No cracking
Machine Oxygen-cut	Transverse	Top	180	Cracked
Machine Oxygen-cut	Transverse	Bottom	170	Cracked

1934—*Engineering News-Record*[16]

A cold bend test around a pin is recommended for qualifying operators of cutting torches to be used on structural steel. The tension face is oxygen cut, the other face machined. The specimen is 1/2 inch wide, 3/4 inch thick, 12 inches long and is bent around a pin 1 1/4 inches radius for full free-hand cuts or 1-inch radius for guided manual cuts. Although the composition of the steel is not specified, the specimen must withstand 180° bend without cracking.

1932—Dawson[17]

Oxyacetylene manual cut boiler plate, 1/2 inch thick, can be bent 180° flat without cracking (no details).

1931—Hovey[6]

The first cracks observed in cantilever bend tests of the

Table 7—Steels Investigated by Zimmerman[13]

Steel	C	Mn	Si	P	S
A—killed	0.19–0.21	0.50–0.52	0.08–0.11	0.016–0.028	0.040–0.047
B—semi-killed	0.18–0.23	0.36–0.57	0.03–0.04	0.023–0.029	0.025–0.046
C—semi-killed	0.16	0.50	0.03	0.025	0.030
D—rimmed	0.23	0.52	0.05	0.017	0.034

Tensile strength = 59,000 to 67,000 lb./in.^{2}
Elongation, % in 8 inches = 25 to 31.
Bend angle = 180° flat without cracks.

punched and oxygen-cut wind connections described in the section on Tension Tests were in the upset steel around the holes nearest the flanges. The cracks occurred only after the connection had been visibly and permanently deformed, and the average load on the specimen had reached 186% of the yield point. After the specimens had been bent 90° the cracks at the punched holes had spread somewhat but only one minute crack was observed at a drilled hole. No cracks appeared in the oxygen-cut or oxygen-cut-and-planed surfaces.

1930—Wolfe[147]

Static bend tests were made on I-beam clips cut from 24-inch I-beams (no details). In the web of each clip were two rows of $^{13}/_{16}$-inch holes 4 inches apart. The web was cut $6^1/_2$ inches wide at the narrow end, 9 inches wide at junction with flange. Two clips were bolted back to back and tested on a span of $22^1/_2$ inches, the flanges being vertical and in the middle. Four types of specimens were tested: (1) punched then oxygen cut, (2) oxygen cut then punched, (3) punched then sawn, (4) sawn then punched. The yield point and maximum load was 5000–5500 lb. and 7200–7700 lb., respectively, in all tests. All specimens bent about 30° to a smooth curve without cracks.

1929—Krefeld[18]

Wind-bracing brackets consisting of a slit 120-lb. 24-inch standard I-beam (length parallel to flange = 12 inches, outstanding web = $8^1/_2$ inches, 3 rows of rivet holes, punched 1 inch diameter, 4 in a row perpendicular to the flange) were tested as rigid cantilevers. After the holes had been punched the bracket was manual oxyacetylene cut (oxygen pressure = 50 lb./in.^{2}, acetylene pressure = 1 lb./in.^{2}) or hack sawn from the I-beam. At a bend angle of 15° the oxygen-cut specimen exhibited a large crack parallel to the flange and extending from the outside hole to the oxygen-cut edge. The only cracks at about the same angle of bend in the specimen cut by hack saw were tiny ones around a rivet hole.

Table 9—Properties of Steel Investigated by Krefeld[18]

	Yield Strength, Lb./In.^{2}*	Tensile Strength, Lb./In.^{2}	Elongation, % in 2 In.	Reduction of Area, %
Parallel to direction of rolling	30,900	61,200	36	60.5
Transverse to direction of rolling	31,000–34,700	60,000–66,300	27–31.5	40.8–47.5

* By drop of beam. Tensile specimens were 0.505 inch diameter (Navy Type). Izod value of material 2 inches from rivet hole = 10–12 ft.-lb. Steel from unbroken brackets of the same fabrication had better properties transverse to the direction of rolling: yield strength = 40,700 lb./in.^{2}, tensile strength = 79,300 lb./in.^{2}, elongation = 27.5% in 2 inches, reduction of area = 49.4%.

The steel contained 0.21–0.27 C, 0.51–0.63 Mn, 0.03–0.04 Si, 0.022–0.028 P, 0.039–0.048 S, and was secured from material that had failed before installation as oxygen-cut wind brackets. Its mechanical properties are shown in Table 9. Fractures revealed no laminations or unusual quantities of slag inclusions, but the microstructure was banded, which was taken to indicate a high finishing temperature.

Since cold tensile strain followed by reheating to 200–300° C. greatly decreased the tensile and bend ductility of the steel, and since the cold-worked material around the rivet holes was found to reach a temperature 120–250° C. during oxygen cutting of the brackets, it was concluded that oxygen cutting after rivet punching embrittled the steel in the vicinity of the holes. Other authorities have observed that cold-worked mild steel may be embrittled by moderate heating regardless of the source of heat. Either annealing the steel at 450–550° C. after oxygen cutting or oxygen cutting before punching was believed to avoid the development of brittleness in the brackets. If neither is feasible, the holes should be drilled.

1929—Wiss[7]

Cold bend tests (180°) caused no cracks in the specimens described in the section on Tension Tests (no details).

1928—Miller[15]

Bend tests, Table 10 (no details), with the cut surface in tension were made on the specimens used by Dawson and Miller,[4] Table 4. The milled specimens had slightly higher bend ductility than the oxygen cut, the rough manual cuts being less ductile than the smooth manual and machine cuts.

1925—Thum[19]

Plunger bend tests (plunger radius = $^3/_{16}$ inch, close 180° bend) on bars $^3/_8$ inch square cut from structural steel plate (no details) resulted in cracks at 37° and 25°, respectively, for sheared and friction sawn bars, whereas milled or oxyacetylene cut bars exhibited no cracks at 180°.

1924—Sandberg[20]

Low-quality steel vitiated cold bend tests on oxygen-cut specimens.

1910—Baumann[152]

Unmachined, manual oxygen-cut specimens of boiler plate 0.39 and 0.79 inch thick, 0.20-1.6 inches wide, were bent 180° without cracking, with the exception of a single specimen.

Table 10—Bend Tests on Steels Described in Table 4. Miller[15]

Material	Thickness of Specimen, Inch	Type of Cut	Elongation, % in 1 Inch at 180° Bend
14-Inch column	Flange—1.56 inches	Milled	49
14-Inch column	Flange—1.56 inches	Oxyacetylene machine cut	47–70
14-Inch column	Flange—1.56 inches	Oxyacetylene manual cut	50
14-Inch column	Web—1.00 inch	Milled	44
14-Inch column	Web—1.00 inch	Oxyacetylene machine cut	38
14-Inch column	Web—1.00 inch	Smooth oxyacetylene manual cut	43
14-Inch column	Web—1.00 inch	Rough oxyacetylene manual cut	34
24-Inch I-beam	Web—0.63 inch	Milled	53
24-Inch I-beam	Web—0.63 inch	Oxyacetylene machine cut	54
24-Inch I-beam	Web—0.63 inch	Smooth oxyacetylene manual cut	52
24-Inch I-beam	Web—0.63 inch	Rough oxyacetylene manual cut	51
$^3/_8$-Inch plate	0.385	Milled	60
$^3/_8$-Inch plate	0.385	Oxyacetylene machine cut	59
$^3/_8$-Inch plate	0.385	Smooth oxyacetylene manual cut	56
$^3/_8$-Inch plate	0.385	Rough oxyacetylene manual cut	50

Fatigue Tests

Pulsating tension and pulsating bend fatigue tests were made by Graf[21,22] on specimens with oxygen-cut surfaces and on machined or oxygen-cut specimens with holes for comparison. All oxygen cutting was done by a commercial firm (no details).

Series 1. Pulsating tension fatigue tests (200–250 cycles) min. (lower stress = 1000 lb./in.^{2}, criterion = 2 million cycles), on specimens 2.4 x 31 x 1.58 inches thick or 1.18 x 31 x 3.15 inches thick with planed and oxygen-cut sides but with mill scale on the surfaces and with a central drill hole (drilled to 0.65 inch diameter, reamed to 0.67 inch diameter, burr removed) parallel to the cut edge resulted in a fatigue strength of 25,600 to 27,000 lb./in.^{2} Fracture always started at the hole, whether the sides were oxygen cut or planed. The static tensile strength of the steel was 65,000 to 70,000 lb./in.^{2} (no details).

Series 2. Pulsating tension fatigue tests as in Series 1 were made on milled or oxygen-cut specimens $1^5/_8$ inches thick, $35^1/_2$ inches long, 1.18 inches wide in the parallel section which was $13^1/_2$ inches long with fillet radius = 8 inches, without holes. The static tensile strength of the steel was 65,000 lb./in.^{2} One specimen was polished longitudinally on the milled surfaces with emery paper. The fatigue limits were:

28,500 lb./in.^{2}—two oxygen-cut surfaces, two mill scale surfaces; fracture started at an oxygen cut surface.
32,700 to 34,100 lb./in.^{2}—two milled surfaces, two mill scale surfaces; fracture started in mill scale.
32,700 lb./in.^{2}—two polished surfaces, two mill scale surfaces; fracture started in mill scale.

Series 3. Pulsating bend fatigue tests (4-point loading, 28 inches between supports, 4 inches between loads) were made on specimens 1.58 inches wide, 2.4 inches deep, the oxygen-cut or longitudinally planed surfaces being wholly in compression or tension. The static tensile strength of the steel was 65,000 lb./in.^{2} The lower stress in the fatigue tests was 2850 lb./in.^{2} The fatigue limits (upper stress minus lower stress) were:

32,700 lb./in.^{2}—oxygen cut; fracture started in oxygen-cut surface in tension.
44,100 lb./in.^{2}—planed longitudinally; fracture started in mill scale surface on side of specimen in tension zone.

Series 4. Same as Series 3, but the static tensile strength of the steel was 61,000 lb./in.^{2} The fatigue limits were:

28,500 lb./in.^{2}—smooth oxygen cut; fracture started in oxygen-cut surface in tension.
24,200 lb./in.^{2}—rough oxygen cut; same type of fracture.
46,900 lb./in.^{2}—oxygen cut, then planed longitudinally to a depth of 0.16 inch; fracture in tension side.
55,500 lb./in.^{2}—oxygen cut, then ground longitudinally just deep enough to remove roughness; fracture in tension side.
37,000 lb./in.^{2}—smooth oxygen cut carefully annealed for 1 hour at 885° C.; fracture in tension side.

Series 5. Same as Series 4, except that two holes were centrally located perpendicular to the tension and compression surfaces and 1.58 inches between centers. The holes were reamed to 0.39 inch diameter, and the burr was removed with a fine file. The fatigue limits were:

28,500 lb./in.^{2}—smooth oxyacetylene cut; fracture started at holes.
31,300 lb./in.^{2}—planed on all sides; fracture started at holes.

Series 6. Same as Series 4, except that a steel containing 0.13 C, 0.48 Cu, 0.13 Ni, 0.21 Cr, 0.48 Si, 1.14 Mn, 0.07 P, 0.016 S, static tensile strength = 72,000 to 78,500 lb./in.^{2}, was used. The oxygen-cut surfaces of the low-alloy steel were smoother than those of the plain carbon steels used in Series 1 to 5. The fatigue limits were:

37,000 lb./in.^{2}—oxygen cut; fracture started in mill scale or in oxygen-cut surface.
61,200 lb./in.^{2}—planed; fracture started in tension zone.
65,400 lb./in.^{2}—oxygen cut, then ground longitudinally just enough to remove ridges; fracture started on tension surface.

Series 7. Same as Series 3, except that the steel had a static yield strength = 50,000 lb./in.^{2}, tensile strength = 99,700 lb./in.^{2}, elongation = 23% (no details). The fatigue limits were:

39,800 lb./in.^{2}—smooth oxygen cut; fracture started either in the tension zone of the mill scale surfaces, or in the oxygen-cut tension surface.
76,800 lb./in.^{2}—oxygen cut, then longitudinally ground just enough to remove ridges; fracture started on the tension surface or at an edge.

The results indicated that the decrease in pulsating tension and pulsating bending fatigue strength of steel with tensile strengths of 60,000 to 100,000 lb./in.^{2} due to oxygen cutting was nearly as great as the decrease due to a smooth hole, and that the decrease was due to the roughness of the oxygen-cut surface compared with machined surfaces. The fatigue strength of the oxygen-cut surface was approximately 60 to 80% of the fatigue strength of machined or mill scale surfaces. The heat effect in oxygen cutting had little, if any, effect on fatigue strength. Annealing raised the fatigue strength of oxygen-cut surfaces, but was only about half as effective as machining. It is not known whether the effect of annealing lay in the removal of shrinkage stresses, in decreasing the sharpness of the ridges or in altering the structure of the steel.

The repeated bend impact strengths of milled planed and oxyacetylene-cut bars 0.39 inch square of four plain-carbon steels (no details) were about equal in Roessler's[23] tests. The stresses were not estimated and the tests did not extend beyond 17,000 blows. It was concluded that the surface irregularities in the oxygen-cut surfaces rather than the heat-affected structure accounted for the slightly poorer results obtained with oxygen-cut specimens compared with longitudinally planed specimens. In contrast with customary belief, grinding the oxygen-cut surface was not found advantageous.

Hardness

1937—Hessler[1]

The uncut steel described in the section on Tension Tests was 69 to 77 Rockwell B. The maximum hardness of the oxygen-cut surface was 88 B.

1937—Dümpelmann[2]

The Brinell hardness (2.5 mm. ball, 187.5 kg.) was measured, Table 11, on the oxygen-cut surfaces described in the section on Tension Tests. The specimens were 1.18 inches wide. The maximum rise in hardness was about 50 Brinell units. The higher hardness at mid-thickness was attributed to decarburization of the surface layers of the plate during hot rolling.

Table 11—Brinell Hardness of Oxygen-Cut Boiler Plate, 0.79 Inch Thick. Dumpelmann[2]

	Acetylene as Preheating Gas			Hydrogen as Preheating Gas		
	0.12 Inch Below Torch Side	Middle of Cut	0.12 Inch Above Lower Side	0.12 Inch Below Torch Side	Middle of Cut	0.12 Inch Above Lower Side
As cut	156–161	180	156–167	156–167	174–180	150–171
Ground flat	156	174–180	156–167	150–156	161–174	145–156
0.04 Inch machined off	131	131–135	131–135	131–135	135–140	123–131
0.20 Inch machined off	123–127	131	127–131	119–123	127	123–127

1935—Zimmerman[13]

Vickers, Brinell and Rockwell B hardness determinations were made on the oxyacetylene machine-cut plates of the four steels described in the section on Bend Tests. The measurements were made at intervals of 0.050 inch (0.002-inch depth intervals) on taper ground (1:25 taper) surfaces. There was no significant difference in Vickers hardness for the four steels. Consequently, the averages for the four steels are given in Table 12. The scatter in Vickers number was 20 to 30 points for all plates 1 inch thick. The Brinell numbers were 40 units lower than the Vickers at the edge and 15 units lower at a depth of 0.20 inch. The Rockwell B hardnesses (converted to Brinell) were 30 units lower than the Vickers at the edge and 10 units lower at a depth of 0.20 inch. In other words, the Vickers machine yielded the highest values.

Although the hardness of the unaffected steels was not stated, and although the temper colors extended 0.30, 0.46 and 0.50 inch, respectively, back of the cuts in the 1/2, 1 and 2-inch plates, it may be concluded that the maximum increase in hardness at the edge was about 120 Vickers units. The average increase was 70 Vickers units, excluding the single exceptionally high value of 253. With the exception of the value of 253 Vickers, there was no significant difference in hardness among the three thicknesses of steel. At a depth of only 0.025 inch beneath the oxygen-cut surface the hardness of the different plates was remarkably uniform.

1935—Theisinger[136]

A Vickers hardness survey, Fig. 1, of a cross section perpendicular to an oxygen-cut surface (no details of cutting) in steel 1/2 inch thick containing 0.17 C, 0.30–0.60 Mn revealed a maximum hardness at the cut of 170 Vickers, which was slightly greater than the maximum hardness (165 Vickers) observed beneath a bead of bare electrode weld metal deposited on the surface of the plate at 12 in./min., 375 amp, 23 volts.

1934—Zimmerman[14]

The Vickers hardness (10 kg. load) of the hot rolled plate described in the section on Bend Tests, Table 7, was 135 to 138. The maximum hardness determined on taper-ground specimens (1:25 taper) of the cut surfaces was:

Sheared = 245 Vickers.
Friction sawn = 345.
Manual oxyacetylene cut = 204.
Machine oxyacetylene cut = 218.

The hardness of unaffected base metal was reached at a depth of:

0.50 Inch in the sheared specimens.
0.06 Inch in the friction-sawn specimens.
0.45 Inch in the manual oxyacetylene cut specimens.
0.65 Inch in the machine oxyacetylene cut specimens.

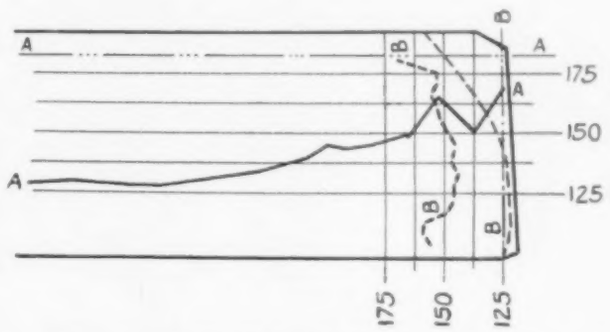


Fig. 1—Vickers Hardness Survey of Cross Section of Steel 1/2 Inch Thick Containing 0.17 C, Perpendicular to Oxygen-Cut Edge. Theisinger[136]

Full Line A—Survey along line parallel to, and about 0.055 inch below, surface of plate.
Dotted Line B—Survey along line parallel to, and about 0.025 inch back of, cut edge.
Dotted Line—Boundary of heat affected zone revealed by Nital etch.

1933—Clarke[5]

Table 3 shows that oxygen cuts in mild steel 3/8 inch thick were 10 to 20 Brinell units harder than a band-saw cut and 60 to 70 Brinell units harder than unaffected steel.

1930—Wiss[24]

The effect of thickness and carbon content on the Brinell hardness (2.5 mm. ball, 187.5 kg. load) of oxyacetylene machine-cut surfaces is shown in Table 13. The strips were probably 1 inch wide. In general, the rise in hardness increased as both thickness and carbon content were increased. However, the relationship was not close. Preheating the high-carbon steels greatly decreased the hardness of the cut surface.

As the width of the strip cut is increased, Table 14, the hardness of the oxygen-cut surface is raised slightly, on account of increased rate of cooling. The strips 0.20 inch wide became red-hot during cutting and the strips 0.39 inch wide attained 500° C. throughout. Annealing above 500° C. reduced the hardness of all oxygen-cut surfaces to that of unaffected base metal.

Holding an oxygen-cut mild steel at 50° C. for up to 40 hours had little effect on the hardness of the cut surface, Table 15.

Table 12—Vickers Hardness (30 Kg. Load) of the Four Oxyacetylene Machine-Cut Steels in Table 7. Zimmerman[13]

Plate Thickness, Inch	Section	Depth below Oxygen Cut Surface, Inch				
		0.00 (Edge)*	0.025	0.05	0.10	0.20
2	Top (torch side)	195	159	148	128	127
2	1/2 Inch down	204	170	151	138	131
2	Middle	217	163	151	135	129
2	1 1/2 Inches down	253	172	158	141	132
2	Bottom	187	156	145	138	137
1	Top	171	154	148	132	131
1	Middle	194	162	142	132	129
1	Bottom	207	163	147	129	120
1/2	Top	214	161	159	143	130
1/2	Bottom	180	158	144	134	132

* About 0.05 inch laterally (0.002 inch in depth) from oxygen cut edge.

Table 13—Effect of Carbon Content on the Brinell Hardness of Unpreheated Oxyacetylene Cuts in Different Steels. Wiss[24]

Carbon Content of Uncut Plate, %	Thickness, Inch	Brinell Hardness Unaffected Base Metal	Brinell Hardness Cut Surface	Hardness Increase Brinell Units	Hardness Increase %	Type of Steel
0.00	0.79	123	105	−18	−10	Electrolytic iron
0.03	24	101	107	6	6	Wrought iron
0.09	0.79	120	160	40	33	
0.13	1.0	114	169	55	42	Mild steel
0.14	0.39	114	142	28	20	Mild steel
0.19	0.59	116	166	50	41	Mild steel
0.22	1.6	126	190	64	53	Mild steel
0.20	0.59	123	168	45	36	Izett II steel
0.20	1.0	129	179	50	38	Izett II steel
0.22	1.6	121	185	64	53	Izett II steel
0.25	0.39	136	166	30	22	Izett II steel
0.15	0.24	162	210	48	30	Cr-Cu low alloy steel
0.11	1.0	165	205	40	24	Silicon-killed steel
0.14	0.59	145	192	47	30	Silicon-killed steel
0.14	1.6	149	216	67	45	Silicon-killed steel
0.16	0.39	160	180	20	13	Silicon-killed steel
0.28	0.39	152	205	53	35	0.3 C steel
0.31	1.0	150	245	95	63	0.3 C steel
0.34	0.59	155	270	115	74	0.3 C steel
0.35	0.39	130	220	90	70	(160 Brinell if preheated to 150° C.)
0.40	0.79	180	240	60	33	(220 Brinell if preheated to 150° C.)
0.51	0.39	244	370	126	52	(260 Brinell if preheated to 300° C.)
0.70	0.39	220	400	180	82	(280 Brinell if preheated to 600° C.)
0.90	3.2	228	390	162	71	

Oxygen cuts made with acetylene in steels 160–260 Brinell were 5 to 20 Brinell units harder than with hydrogen or illuminating gas, because the acetylene cut is faster and hotter than the others. Oxygen cuts in steel containing 0.90 C were 330–345 Brinell with illuminating gas, 340–355 Brinell with hydrogen, and 380–400 Brinell with acetylene.

1924—Sandberg[20]

The Brinell hardness (1 mm. ball, 30 kg. load) of oxygen cuts in the steels in Table 16 was 20 Brinell units higher for acetylene than for coal gas. The increase in hardness was smaller for steel B than for steel A, which was thinner and had a lower carbon content. Later investigators have not confirmed Sandberg's results, which may be accountable, were details of the experiments known.

Impact Tests

1937—Hessler[1]

The averages of two DVMR notch impact specimens of the steel described in the section on Tension Testing were:

Machined = 10.55 mkg./cm.²
Oxyacetylene cut = 8.3 mkg./cm.² (notch in lower side)
Oxyacetylene cut = 13.1 mkg./cm.² (notch in torch side)

1937—Dümpelmann[2]

Table 17 summarizes results on the steel described in the section on Tension Tests. The oxygen-cut surface appears to have been on the compression side of the specimen. The oxyhydrogen cut specimens had higher notch-impact value than the oxyacetylene, and both were superior to the milled specimens.

Table 15—Effect of Aging an Oxygen-Cut Mild Steel (0.19 C) 0.59 Inch Thick (Strip 1.6 Inches Wide) at 50° C. Wiss[24]

Time at 50° C., Hr.	Average Brinell Hardness of Cut	% Increase
Immediately after cutting	165	...
1/2	165.5	0.4
1	166	0.6
2	166.7	1.0
8	171	3.6
20	172	4.2
40	172	4.2

1936—British Acetylene Association[25]

An Izod specimen machined from an oxyacetylene cut crankshaft (no details) with notch in side or face of the cut yielded 19 ft.-lb., whereas the Izod value of the unaffected steel was 10 to 13 ft.-lb.

1934—Zimmerman[14]

The oxygen-cut hot-rolled plate 1/2 inch thick described in the section on Bend Tests had the following Charpy values (averages of 5 specimens):

Machined = 21.4 ft.-lb.
Manual oxyacetylene cut (torch side) = 31.1 ft.-lb.
Manual oxyacetylene cut (lower side) = 37.7 ft.-lb.
Machine oxyacetylene cut (torch side) = 20.3 ft.-lb.
Machine oxyacetylene cut (lower side) = 21.9 ft.-lb.

The specimens were transverse to the direction of rolling (no details). The high values for the manual cut specimens seem to have been related to the deeper heat-affected zone compared with machine oxyacetylene cuts, which had the same Charpy value as the unaffected steel.

1932—de Jessey[23]

Guillery impact tests (speed = 29 ft./sec.) were made on unnotched specimens 0.39 inch square, 2.2 inch long, of 0.08 C steel. Machined specimens and oxygen-cut specimens from which a layer 0.012 inch deep had been machined absorbed 340 ft.-lb. Unmachined, oxygen-cut specimens absorbed 370 ft.-lb., which was reduced to 330 ft.-lb. by annealing 15 min. at 900° C.

1931—Hovey[6]

Impact cantilever bend tests (weight of 4500 lb. dropped 12 inches) were made on the oxygen-cut wind connections described in the section on Tension Tests. The specimens were identical with the tension specimens with the exception that an additional 5 1/4 inches was added to the length perpendicular to the flange for convenience in testing. One blow deflected the specimen 7 3/8 inches on a span of 2 ft. The greatest deformation

Table 14—Effect of Width of Strip Cut on Brinell Hardness of Different Thicknesses of Steels Having Different Carbon Contents. Wiss[24]

Width of Strip Cut, Inch	0.39 Inch Thick		0.59 Inch Thick		1.0 Inch Thick		1.6 Inches Thick	
	0.14% C	0.28% C	0.14% C	0.34% C	0.11% C	0.31% C	0.14% C	0.22% C
0.39	126	190	...	...	...	...	...	...
0.59	135	...	187	...	...	...	...	...
1.0	143	200	193	260	195	228	...	...
1.6	...	225	200	272	203	245	210	183
3.2	...	...	...	...	...	...	221	188

Table 16—Maximum Brinell Hardness of Oxygen Cuts as a Function of Preheating Gas. Sandberg[20]

Steel	Oxyacetylene Cut	Oxy-Coal Gas Cut
A—0.28 C	190	170
B—0.35 C	195	172

Steel	Thickness, Inch	C	Si	Mn	S	P	Brinell Hardness
A	1 3/16	0.28	0.05	0.70	0.045	0.056	125
B	4	0.35	0.018	0.72	0.040	0.048	145

occurred in the vicinity of the holes nearest the flange. Cracks appeared in the upset steel around the punched holes and in the burrs which had not been removed from the drill holes. No cracks were visible in or near the oxygen cut edges. The bending was continued to 90° in a press without result.

1931—Ehret[26]

Charpy tests were made on mild steel specimens with the side opposite the notch either sheared or oxygen cut. The sheared specimens broke, whereas the oxygen-cut specimens bent without fracture.

1929—Krefeld[18]

Drop tests (260 lb. weight dropped 12 to 15 feet) were made on the wind brackets described in Table 9 of the section on Bend Tests. The brackets were tested as rigid cantilevers. The specimen that had been punched, then oxygen cut, cracked in a brittle manner through the punched holes. The specimen that had been punched, then cut to shape with a hack saw, did not crack. Other authorities have observed that cold-worked mild steel may be embrittled by moderate heating regardless of the source of heat.

1929—Wiss[7]

Notch-impact tests on bars 1.18 inches square (no details but probably notch depth was 0.59 inch, notch diameter = 0.16 inch) oxygen cut or planed from the mild steels containing 0.1–0.2 C described in the section on Tension Tests yielded the following results:

Oxygen-cut = 2.21 mkg./cm.^{2}
Planed = 1.93 mkg./cm.^{2}

1928—Bureau of Standards[145]

Impact bend tests were made on the oxygen-cut wind connections described in the section on Tension Tests. The ends of each specimen, which was 0.63 inch thick, 8 inches wide, rested on hard wood blocks. A 50-lb. weight was dropped on the web at mid-span from heights of 1 to 18 ft. At a height of 12 ft. cracks appeared in the punch burr of a hole in the specimen that was punched before oxygen cutting. No cracks appeared in the specimen that was punched after oxygen cutting, even after the weight had been dropped 18 ft. The deflection of the web at the center was 1 3/8 inches for the cracked specimen (die side convex) and 1 inch for the uncracked specimen (punch side convex) after a drop of 18 ft. Subsequent bending in a press 84 to 128° developed cracks at the holes on the convex side of both specimens. The only crack at an oxygen-cut edge occurred through a center punch mark at the edge of the specimen that was punched after oxygen cutting. There was no evidence that oxygen cutting itself or that the sequence of cutting and punching had any influence on the test results.

Table 17—Notch-Impact Value,* Mkg./Cm.^{2} (Width of Specimen = 0.79 Inch; Depth Back of Notch = 0.59 Inch; Depth of Notch = 0.59 Inch). Dümpelmann[2]

Type of Surface	Notch Parallel to Direction of Rolling: Minimum	Maximum	Average	Notch Perpendicular to Direction of Rolling: Minimum	Maximum	Average
Milled	3.3	8.9	6.6	7.0	9.4	8.0
Oxy-cut (acetylene)	8.2	9.5	8.8	8.9	12.5	11.2
Oxy-cut (hydrogen)	10.5	20.7	14.8	11.0	15.8	12.8

* See Table 2. Averages of 3 specimens, diameter of notch = 0.16 inch.

1925—Chapman and Service[27]

Drop tests on rails with oxygen-cut holes in the web resulted in early failure compared with drilled holes (no details). In some tests the oxygen-cut holes reduced the impact value of the rail 90%. Oxygen cutting lowered the shock value of tire steel by 50% (again no details).

1907—Guillet[28]

The Mesnager notch-impact value of manual oxygen-cut specimens of steel containing 0.15 C was 72 to 158 ft.-lb. Unaffected steel had a Mesnager value of 86 to 180 ft.-lb.

Fabrication Qualities

The machinability of oxygen-cut surfaces has not excited much attention. Wiss[24] found that the planing of oxygen-cut surfaces of steel (tensile strength = 68,000 lb./in.^{2}) 0.39 to 1.6 inches thick presented no difficulties whatever. Lathe tests by Chapman and Service[27] on oxygen-cut surfaces of steels containing 0.28 C, 0.70 Mn or 0.35 C, 0.72 Mn also revealed no difficulties. It may be concluded that the hardness of the oxygen-cut surface is a controlling factor.

On account of their sorbitic structure, oxygen-cut surfaces withstand caulking better than machined or milled surfaces, according to Obert.[29] Riveting and caulking tests by Hessler[1] on oxyacetylene machine cuts in steel 0.79 inch thick containing 0.095 C, 0.44 Mn were entirely satisfactory. In a drift test Hessler widened a hole in the steel 0.79 inch diameter, 0.39 inch from an oxygen-cut edge with a conical pin to a diameter of 1.18 inches without failure.

Table 18—Steels Used by Séférian and Leroy[30]

Steel	C	Si	Mn	Cr	Cu	S	P
1	0.05	Nil	0.27	..	..	0.051	0.013
2	0.33	0.02	0.54	..	..	0.024	0.019
3	0.13	Nil	0.49	..	0.40	0.040	0.016
4	0.20	0.27	0.70	0.52	0.57	0.031	0.026

Wiss[7] showed that oxygen-cut surfaces in mild steel can be case carburized successfully.

Corrosion Tests

Tests in running tap water, 1% NaCl, and 1% H_2SO_4 (see review of literature on Corrosion Resistance of Welded Joints, THE WELDING JOURNAL, 16 (8) Suppl., 42 (1937)), on planed and oxygen-cut mild steel have been in favor of oxygen cutting, perhaps on account of the oxide coating and small specific surface of the oxygen cuts.

The zone of localized corrosion that occasionally appears in oxygen-cut steel has been studied by Séférian and Leroy.[30] Specimens of four open-hearth steels, Table 18, 8 x 2 1/2 x 0.47 inch thick, with one edge oxyacetylene machine cut (99.5% O_2, specimens cooled in

air), the other edge sawn, were completely immersed in 20% H_2SO_4 by weight (sp. gr. = 1.142) or artificial sea water (300 gm. sodium chloride, 1.876 gm. sodium phosphate, 12.404 gm. boric acid with sodium carbonate to bring the solution to pH = 8.0 to 9.2), both at 30° C. Each specimen lay in an inclined position in an individual glass beaker containing 750 ml. solution. After each 48-hr. period the position of the specimen was changed. Changes in weight were found to have no significance and were not recorded.

In the acid (4 periods of 48 hours each for steels 1, 3 and 4, and 2 periods of 48 hours each for steel 2) the zone of localized corrosion extended 0.04 to 0.14 inch from the cut on the torch side and 0.04 to 0.06 inch from the cut on the lower side. In steel 1 (0.05 C) the most intense corrosion (no details) occurred in the zone in which the microstructure exhibited a mixture of small and large grains as a result of the temperature attained during cutting. Troostitic constituents in the heat-affected zone back of the cut were believed to account for the localized corrosion in steels 2, 3 and 4. Both the mixed grain size and troostic types of corrosion were believed to be accentuated by couple formation. The artificial sea water had little effect (no details).

Corrosion and electrode potential measurements by Hunsicker[31] showed, Table 19, that smooth manual oxyacetylene cuts were equivalent in corrosion to machine oxyacetylene cuts and were superior to rough manual oxyacetylene cuts. The specimens were 0.59 x 3.15 x 0.39 inch thick in mild steel having a tensile strength of 60,000 lb./in.^{2} The potential measurements were made with a quinhydrone electrode at 18° C. in 1% NaCl solution (0.171 gm. equiv. NaCl in 1 liter of solution) containing 0.02 cm.^{3} oxygen per liter of solution. The potential was measured after 72 hours. The corrosion test was made in a stirred 1*N* solution of HCl. The specimens were hung on glass hooks, and the test continued for 8 hours at 20° C. In both tests the uncut surfaces of the specimen were protected by sealing wax.

Table 19—Electrode Potential and Corrosion Measurements on Oxygen-Cut Mild Steel. Hunsicker[31]

Specimen	Loss in Weight, Gm./M.^{2}/Hr., in 1 *N* HCl	Electrode Potential in 1% NaCl, Mv.
Machine oxygen cut	34.5	690
Smooth manual oxygen cut	34.0	685
Rough manual oxygen cut	38.0	740

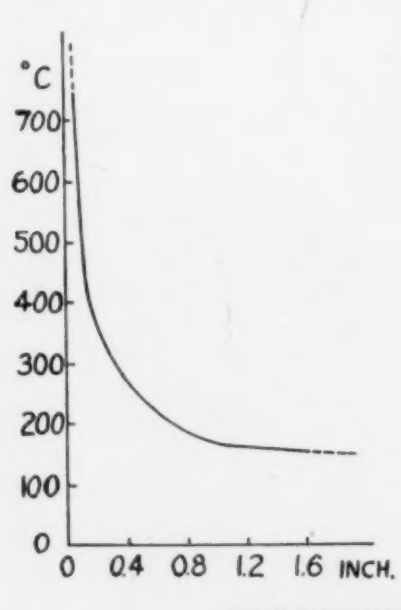


Fig. 2—Temperature Distribution at Mid-Thickness in a Mild Steel Plate 0.47 Inch Thick. The Maximum Temperatures Attained Are Shown as Ordinates, the Abscissa Being Distance from the Oxygen-Cut Edge. Platinum-Platinum Rhodium Couples 0.008 Inch Diameter Were Used. Séférian and Leroy[30]

Shrinkage Stresses

There is no reliable quantitative information on residual stresses created by oxygen cutting (see review of literature on Shrinkage Stresses in Welding, THE WELDING JOURNAL, **16** (11), Suppl., 37 (1937)). The temperature distribution at the middle of a mild steel plate 0.39 inch thick during machine oxyacetylene cutting was determined by Séférian and Leroy,[30] Fig. 2, but the measurements do not extend beyond 750° C. Apparently the temperature distribution is steeper during cutting than during welding.

Nichols and Kiernan[135] found, Table 20, that the maximum temperature 1/8 to 2 inches back of machine oxygen-cut surfaces in steel 6 1/2 inches thick (0.27 C, 0.52 Mn, 0.17 Si) are higher with acetylene than with hydrogen. The oxygen pressure was 160–172 lb./in.^{2} Temperatures were measured at intervals of 15 seconds with the aid of iron-constantan thermocouples embedded at mid-thickness and portable potentiometers. The highest temperatures recorded were 650° C. in hydrogen cutting and 490° C. in acetylene cutting. It was estimated that the temperature-distance curves for both processes would intersect at the cut surface at 1000° C., which seems rather low.

Table 20—Temperature Distribution Back of Oxygen Cut in Steel Containing 0.27 C. Nichols and Kiernan[135]

Distance Back of Cut, Inch	Maximum Temperature, ° C. During Cutting: Acetylene	Hydrogen
1/4	590	500
3/8	450	350
5/8	330	250
1 1/8	220	180
2 1/8	135	125

In agreement with other investigators who found that the oxygen-cut edge was subjected to tensile residual stresses, Krefeld,[18] using a Berry gage, 2-inch gage length and the subdivision method, concluded that the manual oxygen-cut surface of the web of a 24-inch, 120-lb. I-beam was in elastic tension to the extent of 30,000 lb./in.^{2} The cut surface had been permanently compressed during cutting, as revealed by Fry etching lines which extended 1 1/2 to 1 3/4 inches from the cut surface. The lines made an angle of 45° with the oxygen-cut edge and were longer than similar lines produced by punching. Geruso and Hannant[32] believed that if the preheat flame is played on the line of the cut before cutting, mild shrinkage stresses only are created. Preheating (500–700° C.) or stress annealing (600° C.) removes shrinkage stresses in oxygen-cut steel, according to Roessler.[22]

METALLURGY

Heat-Affected Zone

The structure of the heat-affected zone of oxygen-cut steel has been studied by a large number of investigators who agree that banded structure is obliterated near the cut and that there is no zone of coarse grain size. However, there is disagreement on the question of carbon migration near the cut edge (see section on Carbon Migration) and little information on the structure of oxygen-cut higher-carbon steels (see section on Mechani-

cal Properties of Oxygen-Cut Higher-Carbon Steels). The depth of the heat-affected zone as revealed in the microstructure has been found to vary considerably with cutting variables.

Measurements of the depth of the heat-affected zone in cool mild steel 1 inch thick containing 0.085 C have been made by de Jessey,[33] who used an oxygen nozzle 0.06 inch diameter with five concentric preheating jets (arrangement of jets not stated). The plates were 6 wide and the cuts were made at intervals of 1.6 inches with relatively low-purity oxygen. As the cutting speed was increased the depth of heat-affected zone decreased, Table 21. Tables 22 to 24 show that the depth of the heat-affected zone decreases with decrease in acetylene consumption and with increase in oxygen purity. There was a critical oxygen pressure at which the depth of the heat-affected zone was a minimum. The initial effect of increasing the oxygen pressure is to cool the cut. With further increase in pressure the increased amount of metal that is burned predominates in increasing the depth of heat-affected zone.

Table 21—Effect of Cutting Speed on Depth of Heat-Affected Zone in Mild Steel. de Jessey[33]

Cutting Speed, Ft./Hr.	Depth of Heat-Affected Zone (Inch) as Revealed by Microstructure
16.3	0.051
49	0.039
82	0.032

Oxygen purity = 98.8%.
Oxygen pressure = 37 lb./in.²
Acetylene consumption = 5.5 cu. ft./hr.

Table 22—Effect of Acetylene Consumption on Depth of Heat-Affected Zone. de Jessey[33]

Acetylene Consumption Cu. Ft./Hr.	Depth of Heat-Affected Zone (Inch) as Revealed by Microstructure
3.5	0.032
11.8	0.037
19.7	0.063

Oxygen purity = 99%.
Oxygen pressure = 37 lb./in.²
Cutting speed = 46 ft./hr.

Table 23—Effect of Oxygen Purity on Depth of Heat-Affected Zone. de Jessey[33]

Cutting Oxygen Purity, %	Depth of Heat-Affected Zone (Inch) as Revealed by Microstructure
98 and 99	0.032
97	0.039
96	0.047

Cutting speed = 46 ft./hr.
Oxygen pressure = 40 lb./in.²
Acetylene consumption = 5.5 cu. ft./hr.

Table 24—Effect of Oxygen Pressure on Depth of Heat-Affected Zone. de Jessey[33]

Cutting Oxygen Pressure, Lb./In.²	Depth of Heat-Affected Zone (Inch) as Revealed by Microstructure: Cutting Speed = 46 Ft./Hr.	Cutting Speed = 65 Ft./Hr.
18.5	0.047	0.032
46	0.034	0.028
67	...	0.036
100	0.047	0.039

Oxygen purity = 98.8%.
Acetylene consumption = 5.5 cu. ft./hr.

In agreement with de Jessey and Plieninger,[34] the Chemisch-Technisch Reichsanstalt[35] found that cutting speed affected the depth of heat-affected zone, Table 25. However, cutting speed was considered the most important factor. The cuts were made with a straight line cutting machine in steel 0.79 inch thick containing 0.06–0.10 C. The same results were obtained with oxygen of 99.6% purity as with a purity of 97.6%. Increasing the preheating flame (no details) increased the depth of the heat-affected zone about 0.024 inch.

Table 25—Effect of Cutting Speed on Depth of Heat-Affected Zone as Revealed by Microstructure[35]

Cutting Speed, Ft./Hr.	Depth of Heat-Affected Zone
65	0.051
39	0.078
28	0.102
18	0.158

Plieninger[34] in 1912 found that the heat-affected zone as revealed by microstructure in a cut in mild steel (0.15 C) 1.6 inches thick made with oxygen of 99% purity was 0.032 inch. A deeper heat-affected zone was observed in a slow cut made with impure oxygen (74% purity). Strangely enough, despite the greater heat input in this cut, martensite was observed on the edge. In 1907 Guillet[28] found that the heat-affected zone was 0.12 inch deep back of an oxyhydrogen cut in steel 0.59 inch thick containing 0.15 C. The structure at the edge was slightly overheated.

The depth of the heat-affected zone revealed by metallographic etching increased as the carbon content was increased, according to Wiss[7,24] and Leroy,[36] Table 26. Using the procedure described in the section on Drag (cutting speed = 55 ft./hr., 98.75% O_2, 37 lb./in.², 8.7 cu. ft./hr. C_2H_2), de Jessey[33] found that the depth of the microstructurally affected zone was 0.032 inch at 0.10 C, 0.036 inch at 0.65 C, 0.043 inch at 0.80 C, 0.051 inch at 1.00 C, decreasing to 0.032 inch at 1.2 and 1.4 C. The heat-affected zone in mild steel 1.0 inch thick, 0.10 inch drag, oxygen cut at a speed of 43 ft./hr. (8.8 cu. ft. acetylene per hr.) was 0.06 inch. Hilpert[38] found that the heat-affected zone in oxygen-cut steel containing 0.4 C was 0.055 inch deep (no details) and that there were no cracks.

Table 26—Depth of Heat-Affected Zone in Oxygen-Cut Plain Carbon Steels (for Cuts on Wide, Cold Material). Wiss,[7,24] Leroy[36] and Holler and Schneider[37]

Plate Thickness, Inch	Cutting Speed, Ft./Hr.	Depth of Heat-Affected Zone, Inch: Steels with Up to 0.30 C	Steels with 0.30 to 1.0 C
0.20	82	0.004–0.012	0.012–0.020
1.0	49	0.020–0.028	0.032–0.06
10	19½	0.06–0.12	0.12–0.20
32	8	0.12–0.20	0.20–0.32

Table 27 contains additional information on total depth of heat-affected zone. Within the limits tested, thickness had no consistent effect.

Using city gas for preheating, Wiss[24] found that the depth of heat-affected zone in oxygen-cut mild steel (0.1 C) was about the same as with acetylene, Table 28.

Table 27—Depth of Heat-Affected Zone (Inch) in Oxygen-Cutting Plain Carbon and Low-Alloy Steel. Hilpert[38]

Plate Thickness, Inch	0.22 C Steel	Cr-Cu Steel
0.24	...	0.075
0.39	0.047	...
0.59	0.051	...
1.0	0.067	...
1.6	0.055	...

Steel	C	Si	Mn	Cr	Cu	Ni
0.22 C	0.22	0.08	0.58	0.02	0.1	0.1
Cr-Cu	0.15	0.25	0.8	0.4	0.5–0.8	..

Table 28—Effect of Plate Thickness on Depth of Heat-Affected Zone in Oxygen-Cut Mild Steel Using City Gas as Preheating Fuel. Wiss[24]

Plate Thickness, Inch	Depth of Heat Affected Zone as Revealed by Microstructure	
	Torch Side of Cut	Middle and Bottom
0.39	0.039	0.012–0.016
0.59	0.051	0.020–0.032*
1.0	0.085	0.020–0.032*
1.6	0.071	0.028–0.043

* Too powerful preheating flame.

In Zimmerman's[13,14] investigations of the hardness and bend ductility of machine oxygen-cut mild steels (see section on Bend Tests) the depth of modified metal, Table 29, increased with the thickness. The microstructure revealed slightly greater depth of heat-affected

Table 29—Depth of Modified Metal in Oxygen-Cut Mild Steels. Zimmerman[13]

Thickness, Inch	Depth of Modified Metal (Inch) as Estimated by Surface Color	Microetch (Middle of Cut)	Microetch (Middle of Cut)
1/2	0.30	0.05	0.030
1	0.46	0.06	0.043
2	0.50	0.09	0.080

zone at the top and bottom corners, owing to concentration of heat on the torch side and to decreased thermal conduction at both corners. Hardness changes in the vicinity of the cut corresponded with the changes in microstructure. Unlike Zimmerman and other investigators, Wiss[24] detected no increase in depth of heat-affected zone at the bottom of oxygen-cut surfaces, but found a deeper zone at the start of the cut than elsewhere on account of the time required for preheating. In oxygen cuts in wrought iron 24 inches thick, the zone of refined grain structure was 0.20 inch thick; in electrolytic iron 0.39 inch thick the refined zone was 0.04 inch thick, hydrogen being used for preheating. Séférian and Leroy[20] correlated the corrosion characteristics of oxygen-cut steel with the microstructure (see section on Corrosion).

In oxyacetylene cutting mild steel, Curtiss[39] found, Table 30, that the depth of heat-affected zone as revealed by microstructure increases directly with increase in thickness of cut up to 8 inches. In thicker steel the depth increases less rapidly with increase in thickness. Greater depths, in accordance with the formula, $d = 0.025 + 0.03\ T$, are quoted by the International Acetylene Association[149] for the altered structure in plain carbon steels cut perpendicular to the surface. In the formula T is plate thickness, d is depth of oxygen-cutting effect, both expressed in inches. De Jessey[34] found that beyond a thickness of 2 inches, the depth of heat-affected zone increased very slowly.

Table 30—Effect of Plate Thickness on Depth of Heat-Affected Zone in Oxygen-Cut Mild Steel. Curtiss[39]

Thickness, Inch	Depth of Heat-Affected Zone (Inch) as Revealed by Microstructure
1	0.025
2	0.04
4	0.07
10	0.15
22	0.28

A number of investigators have attempted to divide the heat-affected zone into two or three parts: the edge structure, the fine grain structure a short distance from the edge, and the zone of mixed grain size merging into the unaffected steel. Wiss,[24] for instance, distinguished between (1) a coarse-grained zone at the edge, (2) a recrystallized, heat-treated zone and (3) the zone of junction with unaffected steel. The attempts have not been convincing nor have they accounted for the effects of segregation and carbon content. Rather wide differences in depth of heat-affected zone may be observed in apparently similar specimens. For example, Hessler[1] found that the depth varied from 0.063 to 0.20 inch in a machine oxygen-cut steel 0.79 inch thick containing 0.095 C, 0.44 Mn. Moss[40] found that the depth of heat-affected zone as revealed by microstructure varied from 0.04 inch in steel containing 0.36 C, 0.94 Mn, 0.31 Si, and 0.06 inch with 0.27 C, 1.40 Mn, to 0.08 inch in steel with 0.25 C, 3.3 Ni, 0.64 Mn. The depth seems to increase as the critical temperature of the steel is lowered. All the steels were machine oxygen cut and were 1 inch thick. As was to be expected, Hilpert[38] could detect no mechanical distortion in the grain structure of an oxygen-cut surface.

Carbon Migration

For want of a better term, "carbon migration" has been adopted to express the higher carbon content that has been found in oxygen-cut surfaces than in unaffected base metal. Although the majority of investigators detected higher carbon content in the cut surface than in unaffected material, they are not in agreement about the extent of the increase or its causes. There is also a minority who report either no change in carbon content or a decrease in carbon content at the cut edge. The available information is summarized chronologically in this section, the information on carbon increase being dealt with first.

In support of increased carbon content

1912—P. L. M. Congress

An authority is quoted by Amédéo[41] as having found that the depth of carburization of oxygen-cut mild steel 1 inch thick may be 3/16 inch as revealed by microstructure. Unless ledeburite or hypereutectoid cementite is visible or the specimen has been annealed, microscopic examination is unreliable and misleading as an indication of carbon content of the oxygen-cut surface.

1913—Amédéo[42,43]

Microstructural examination of cut and cutting slag from a number of steels (0.15 C to "hard steel") revealed that:

1. The lower portions of the cut surface are slightly carburized in all steels. The top of the cut is carburized to about the same extent as the bottom.

2. Metal adhering to lower edge of cut has higher carbon content than base metal.

3. If upper edge of cut is melted by the preheating flame the resulting edge upon solidification is decarburized, although the unmelted metal beneath is carburized. Consequently, Amédéo concluded that the steel is not melted in oxygen cutting, otherwise it would be decarburized.

4. The metal in the sparks is often decarburized on account of the oxidizing action of the air.

5. Carburization occurs alike with hydrogen or acetylene as preheating fuel.

6. Oxyacetylene cutting torches with concentric preheating jets exert a greater carburizing action than with separate preheating jet.

7. Carburization is increased by increasing the power of the preheating flame.

It was believed that the carbon monoxide evolved in the burning of the steel in the kerf carburized the solid iron with which the gas came in contact.

1921—Haynes[44]

Oxygen-cut surfaces of 0.2 C steel contained 0.9 C whether hydrogen or acetylene was used for preheating (no details).

1921—Haynes, Roth and Ness

Miller[45] reported that Haynes, Roth and Ness found 0.40 C and 1.46 C in the surface layer of oxygen-cut steels containing 0.24 C and 1.35 C, respectively.

1922—Nichols and Kiernan[135]

Successive layers 1/8 inch thick were planed from machine-cut surfaces in three annealed forgings 5 1/2 inches thick, Table 31. The cuts were made at 15 ft./hr. with oxygen at 100–125 lb./in.^{2} Most of the cuts had a higher carbon content than the uncut steel. The plain carbon steels were troostitic in the vicinity of the cut, but there was a thin layer containing hypereutectoid cementite (alkaline sodium picrate etch) on the edge of the 0.52 C steel. It was concluded that carbon was absorbed from the carbon monoxide liberated in the oxidation of the steel in the kerf. The maximum depth of carbon penetration was 1/8 inch.

1924—Balliet[46]

When acetylene is used for preheating, oxygen-cut surfaces are case hardened (no details).

1925—Miller[45,47]

Successive layers 0.002 inch thick were milled from oxygen-cut surfaces of steel 4 to 6 inches thick containing 0.50 C, 0.65 Mn, 0.16 Si, 0.045 S, 0.024 P. The edge layer contained 0.66 C, the next layer 0.63 C, the third layer 0.52 C, and the fourth layer 0.49 C, whether hydrogen or acetylene was used for preheating. Since the kerf was 1/4 inch wide and the total depth of the carburized zone was not over 0.025 inch, there was more than enough carbon liberated as monoxide from the kerf material to account for the increase in carbon content of the cut surface. Miller did not analyze the gases in the kerf, which must contain a very large proportion of oxygen compared with CO and CO_2.

If the steel to be cut was preheated bright red, the oxygen-cut surface was decarburized, as revealed by microscopic examination. Preheating to a temperature a little below dull red resulted in no visible change in the carbon content of the cut surface. The absence of carburization in the preheated parts was attributed to the greater rate of reaction of oxygen with carbon in the preheated steel. Presumably it was believed that the normal carburizing effect was exerted on the preheated steel, but the carburized edge became decarburized after the cutting torch had passed. Some of the cuts on hot steel exhibited microscopic cavities resembling shrinkage cavities along the edge of the cut. The cavities were attributed to gases liberated during cooling.

Since the carbon responsible for the carburizing effect is derived from the steel itself, there is no carburizing effect in oxygen-cutting, carbon-free iron and little effect with low-carbon steel. Oxygen-cut surfaces of Armco iron billets 4 inches square revealed a little carburization (no details). The surfaces were heavily carburized as revealed by microstructure, if a billet 4 inches thick of steel containing 0.50 C was placed on top of the Armco iron billet and the cut was made through both billets. If the 0.50 C billet were below the Armco iron billet, the Armco surfaces were scarcely carburized.

Extraordinary carburization was observed in some cuts, as high as 5 to 6% C being found in projections on oxygen-cut steel surfaces. The hypereutectoid zone of cuts in steel containing 0.45–0.50 C was 0.004–0.007 inch thick. Cracks 0.01 inch deep due to shrinkage

Table 31—Carbon Content (%) of Oxygen-Cut Surfaces. Nichols and Kiernan[135]

Steel	Preheating Fuel	Position of Sample in Cut	Average Distance from Cut, Inch							
			1/64	3/64	5/64	7/64	9/64	11/64	13/64	15/64
0.3 C	Acetylene	Top	0.16	0.29	0.25	0.25	0.28	0.23	0.26	0.29
0.3 C	Hydrogen	Top	0.34	0.29	0.23	0.22	0.25	0.25	0.26	0.24
0.3 C	Acetylene	Bottom	0.45	0.54	0.25	0.25	0.27	0.26	0.23	0.25
0.3 C	Hydrogen	Bottom	0.35	0.31	0.24	0.24	0.23	0.23	0.26	0.25
0.5 C	Acetylene	Top	0.64	0.48	0.50	0.51	0.49	0.50	0.49	0.46
0.5 C	Hydrogen	Top	0.66	0.50	0.46	0.50	0.49	0.49	0.52	0.51
0.5 C	Acetylene	Bottom	0.59	0.31	0.52	0.56	0.50	0.50	0.50	0.49
0.5 C	Hydrogen	Bottom	0.65	0.51	0.48	0.52	0.51	0.51	0.54	0.53
Nickel	Acetylene	Top	0.38	0.38	0.36	0.37	0.39	0.37	0.37	0.37
Nickel	Hydrogen	Top	0.48	0.47	0.41	0.36	0.36	0.37	0.37	0.35
Nickel	Acetylene	Bottom	0.41	0.36	0.36	0.36	0.39	0.36	0.36	0.36
Nickel	Hydrogen	Bottom	0.46	0.44	0.40	0.35	0.38	0.36	0.36	0.38

Steels Used by Nichols and Kiernan

	0.3 C Steel	0.5 C Steel	Nickel Steel
Carbon	0.27	0.52	0.38
Manganese	0.52	0.80	0.58
Silicon	0.17	0.22	0.15
Phosphorus	0.015	0.016	0.005
Sulphur	0.023	0.023	0.039
Nickel	0.05	0.02	3.44

Table 32—Carbon Pick-Up and Depth of Heat-Affected Zone in Oxygen-Cutting Plain Carbon Steel. Wiss[7]

Thickness, Inch	Carbon Content, Base Metal	Carbon Content, Cut Edge	Depth of Heat-Affected Zone, Inch	Remarks
2	0.1	0.12	.....	.....
2	0.2	0.3	.....	.....
2	0.3	0.55	.....	.....
About 0.39	0.4	0.5	0.026	.....
2	0.4	0.60 (1–1.5*)		
0.39	0.51	0.65	.....	Carbon content of cut edge was hypereutectoid before annealing at 850° C. for 1/2 hr.
0.39	0.7	1.0	.....	
2.6	0.9	1.3(2–4)	.....	
2 3/8 Diameter	1.3	Over 1.7	0.047 Bottom 0.063 Top	Machine cut, hardened or annealed; no cracks; smooth cut

* Extreme edge.

stresses were often observed in the carburized layer.

1925—Horn[48]

The carburized layer on oxygen-cut steel is 0.008 to 0.024 inch deep. The carbon is derived from carbon monoxide in the preheating flame.

1927—Chemisch-Technisch Reichsanstalt[35]

The surface layer of an oxyacetylene machine cut in steel 0.79 inch containing 0.06–0.10 C made with oxygen of 99.6% purity had 0.01% higher carbon content (presumably 0.07–0.11 C) than the uncut steel. No details of the method of sampling were supplied. At the temperature of cutting it was believed that the solid metal on the sides of the kerf was in equilibrium with a melt of higher carbon content. During the rapid cooling following cutting there is insufficient time for equalization of carbon content between solid and melt. Consequently, according to this hypothesis, the melt solidifies on the surface as a layer of higher carbon content than uncut steel. The hypothesis was not supported by metallographic examination, which failed to reveal a layer of material at the edge that had a "cast" structure.

1930—Wiss[24]

Basing his conclusion on microstructure, Wiss stated that oxygen cutting increases the carbon content of the cut edge, Table 32, without supplying details of the experimental procedure. The layer of ledeburitic structure on the cut surface of the 1.3 C steel was 0.004 inch thick. Similar layers were sometimes observed on low-carbon steel. No cracks or shrinkage holes were observed on the cut surface of any of the steels. It was believed that the ledeburitic layer, which was 0.0004 inch thick on mild steel and 0.0032 inch thick on steel containing 1.3 C, accounted for the carburizing effect in oxygen-cut, low-carbon steel. The ledeburitic layer acquired its carbon from the carbon monoxide in the oxyacetylene preheating flame. Nevertheless, even with hydrogen for preheating, some carburization is observed, although less than with acetylene. The lower temperature of the oxyhydrogen flame was believed to account for the smaller amount of carburization compared with oxyacetylene.

Like Amédéo, Wiss observed that in low-quality oxygen cuts in which the torch side of the kerf had been melted, the solidified melt adhering to the top of the cut was decarburized.

1931—Curtiss[39]

Oxygen cuts made with either hydrogen or acetylene in mild steel plates, Table 33 (thickness not stated), had a higher carbon content at the cut edge than in unaffected steel. In fact, the oxyhydrogen cuts revealed more pronounced carbon migration than the oxyacetylene. All shavings from the first cut were annealed (no details) to create a pearlitic structure. In this way the possible effect of sorbitic structure on the analytical procedure was eliminated. Annealing (no details) equalized the carbon concentration in the oxygen-cut steel.

The hypothesis put forward to explain the higher carbon content at the cut edge was based on the supposition that, since the solubility of carbon in iron increases with increase in temperature (neglecting delta iron), the carbon from the cool interior of the steel migrates to the hot cut edge during cutting. Subsequent rapid cooling prevents redistribution of carbon. The only evidence in favor of the hypothesis is drawn from the oxygen-cutting results themselves. Analogous carbon migration is not reported in analogous processes, such as spot welding or flame hardening. The hypothesis also fails to account for the ledeburite observed by some investigators on the oxygen-cut surfaces of unalloyed steel.

1932—Bainbridge[49]

The depth of penetration of carbon in oxygen cutting is only about one-half the depth of the heat-affected zone.

1935—de Jessey[50]

Carburization of the oxyacetylene-cut surface of mild steel 1 inch thick, 0.10-inch drag, occurs to a depth of 0.004 inch.

1935—Bainbridge[51]

The slight increase in carbon content at oxygen-cut surfaces is due to migration of carbon from the interior of the steel, and is not derived from the fuel.

Table 33—Migration of Carbon to Oxygen-Cut Edge. Curtiss[39]

Steel	Pre-heating Gas	Treatment	Carbon Content, % in Cut 1	Cut 2	Cut 3	Cut 4
A	Acetylene	.....	0.32	0.17	0.17	0.10
A	Acetylene	Annealed	0.19	0.19	0.18	0.17
B	Acetylene	.....	0.27	0.27	0.23	0.22
B	Acetylene	Annealed	0.21	0.23	0.22	0.23
C	Acetylene	.....	0.16	0.12	0.09	0.10
C	Acetylene	Annealed	0.08	0.08	0.10	0.11
D	Acetylene	.....	0.12	0.12	0.11	0.10
D	Acetylene	Annealed	0.10	0.11	0.10	0.10
E	Acetylene	.....	0.18	0.18	0.17	0.17
E	Hydrogen	.....	0.21	0.19	0.16	0.16
G	Acetylene	.....	0.20	0.16	0.16	0.15
G	Hydrogen	.....	0.22	0.18	0.15	0.15
J	Acetylene	.....	0.28	0.26	0.24	0.24
J	Hydrogen	.....	0.31	0.2[illegible]	0.24	0.23

Cut 1 = surface layer 1/32 inch deep planed from cut surface.
Cuts 2, 3, 4 = successive layers each 1/32 inch deep.

1936—Schneider[52]

The increased carbon content at the oxygen-cut edge is derived from the steel itself (no details), not from the flame.

1936—Yarkho[53]

Yarkho found that the layer 0.02 inch thick containing the oxygen cut in a steel 1 inch thick containing 0.37 C, 0.60 Mn, 0.23 Si, contained 0.38 C when the cut was made with oxyacetylene preheating and 0.54 C when an oxy-benzine preheating flame was used. Acetylene was much less powerful than benzine in carburizing the cut surface.

1937—Daniel and Durant[54]

The increased carbon content in the oxygen-cut surface is derived from the metal that is burnt in the kerf, not from the preheating fuel.

1937—Tetzlaff[55]

If it is assumed that under oxygen-cutting conditions iron is oxidized preferentially to carbon, the increased carbon content of oxygen-cut surfaces is accounted for.

1937—Bass[56]

Carbon migrates to oxygen-cut surfaces in steels containing over 0.30 C.

1938—Moss[40]

The oxygen-cut surfaces of medium-carbon steels are carburized to a depth of 0.002–0.003 inch.

1938–1939 International Acetylene Association[149]

There is a moderate increase in carbon content on oxygen-cut surfaces, usually to a depth of not over 0.002–0.003 inch. Carbon migration in the steel, not carbon absorption from the acetylene flame, is the cause of the increase.

In support of no change in carbon content

1907—Guillet[28]

There was no decarburization visible on oxygen-cut surfaces of steel 0.59 inch thick containing 0.15 C. Hydrogen was used for preheating.

1922—Johnston[57]

On the basis of microstructure Johnston[57] concluded that there was no appreciable increase in carbon content at the edge of cuts made with oxyacetylene cutting torches in steel 6 inches thick containing 0.46–0.51 C, 0.60 Mn, 0.13–0.14 Si. The microstructure of the edge of the cut after it was annealed at 760° C. in such a way as to prevent decarburization had the same appearance as annealed, uncut metal. Of thirteen torches tested, only one was unable to cut the steel.

1926—Krebs[58]

Although his results, Table 34, indicated a slight increase in carbon content at the machine oxygen-cut surface of 0.44 C steel (thickness not stated), Krebs concluded that there was no pick-up of carbon at the cut edge and no appreciable change in the content of other elements in the steel (no details).

Table 34—Composition of Machine Oxygen-Cut Edge. Krebs[58]

	Carbon Content, %
Shavings from surface of cut to depth of 1/32 inch	0.463
Shavings 1/32 to 1/16 inch deep	0.433
Unaffected steel	0.438

1936—Andrews and Jamison[59]

It was claimed without evidence that oxygen-cut edges were not carburized when propane was used as fuel.

In support of decarburization

1924—Herbert[60]

In contrast with other investigators A. Herbert, Ltd. observed the following depths of decarburization: 0.003–0.006 inch in mild steel 1/2 to 2 1/2 inches thick up to 0.35 C for smooth cuts and 1/32 inch for jagged cuts; 0.0005 inch in steel 3 inches thick with 0.50 C for smooth cuts and 0.05 inch for jagged cuts; 0.05 inch for steel 1 inch thick containing 1.0 C, the hardness under the decarburized skin being 444 Brinell. The cuts were made with a 3-hose torch by machine. The change in hardness with depth below the machine-cut surface of a steel containing 0.52 C, 0.82 Mn (dimensions not stated), Table 35, was comparatively rapid. At the cut edge there was a decarburized zone 0.007 inch deep. The hardness of the unaffected steel was 170 Brinell.

Table 35—Depth-Hardness[60] Measurements on Oxygen-Cut Steel Containing 0.52 C, 0.82 Mn

Depth (Inch) Below Cut Surface to Which the Steel Was Machined for Hardness Determination	Brinell Hardness
0.010	418
0.02	364
1/16	269
1/8	207
3/16	187
1/4	170

1934—Szumowski[61]

The carbon content and hardness of machine oxy-acetylene-cut surfaces in locomotive frames 3 1/2 inches thick, Table 36, indicate that the surface was slightly decarburized.

1937—Liedloff[62]

Summarizing the oxygen-cutting process in a comprehensive chart, Liedloff states that there is extraordinarily little decarburization of the oxygen-cut surface (no details).

1937—Groom[54]

Oxygen cuts in steel containing 0.5 C were deeply decarburized (no details).

Composition of Slag

The solid or liquid material that is blown from the kerf during oxygen cutting is known as the slag. The

Table 36—Carbon Content and Brinell Hardness of Machine Oxygen-Cut Locomotive Frames. Szumowski[61]

Location	Brinell Hardness	Carbon Content, %
Extreme edge of cut	170–180	...
Extreme edge of cut (smoothed)	163–170	...
0.02 Inch below cut surface	156–163	0.186
0.06 Inch below cut surface	156–163	0.248
0.10 Inch below cut surface	159–163	0.258
0.14 Inch below cut surface	159–165	0.287
0.18 Inch below cut surface	149–156	0.296
0.22 Inch below cut surface	140–143	0.274
Unaffected steel	140–143	...

Cutting speed = 38 ft./hr.
Oxygen pressure = 78 lb./in.^{2}
Acetylene consumption = 0.23–0.28 cu. ft./in.^{2}
Distance of tip from surface = 0.16–0.20 inch.
Width of kerf = 0.16 inch.

Table 37—Slag Obtained in Oxyhydrogen Cutting Mild Steel 1.57 and 0.79 Inch Thick; Length of Cut = 21.6 Inches. Plieninger[34]

Oxygen Purity, %	Weight of Slag, Gm. Slag and Iron	Iron	Fe, %	FeO, %	Fe_2O_3, %	Fe_3O_4, %	Remainder for FeO, %	Theoretical Oxygen Consumption, Cu. Ft./Ft.	Observed Oxygen Consumption, Cu. Ft./Ft.
Oxygen Pressure = 60 Lb./In.², Oxygen Not Preheated, Plate 1.57 Inches Thick									
99.0	506	80	15.8	52.5	31.1	45.0	38.5	1.22	4.3
97.0	526	72	12.1	50.3	37.6	54.5	20.7	1.41	5.8
94.4	550	58	9.8	48.9	40.8	59.2	30.5	1.80	7.7
89.2	607	37	6.9	46.7	45.6	66.1	26.2	1.85	10.6
85.0	649	14	3.4	48.4	48.0	69.6	26.8	2.15	13.7
81.0	668	11	2.6	46.8	50.2	72.7	24.2	2.26	15.0
Oxygen Pressure = 60 Lb./In.², Oxygen Preheated to 400° C., Plate 1.57 Inches Thick									
99.0	611	94	22.2	47.3	29.8	43.2	33.9	1.59	4.1
94.0	616	52	7.5	44.0	48.0	69.6	22.4	1.88	5.9
81.0	650	47	5.0	45.7	48.9	70.9	23.7	2.10	12.2
74.0	761	16	2.6	44.1	52.7	76.4	20.4	2.58	17.1
Oxygen Pressure = 36 Lb./In.², Oxygen Not Preheated; Plate 0.79 Inch Thick									
99.0	209	28	30.2	49.5	29.6	42.9	36.2	0.57	1.59
94.0	238	31	11.5	50.3	37.6	54.5	33.4	0.73	2.42
85.0	251	14	5.9	53.3	39.8	57.7	35.4	0.82	3.81

slag consists of iron oxide and up to 40% unoxidized iron. At room temperature the oxide is probably Fe_3O_4, but at cutting temperature the oxide is a liquid solution of oxygen and iron of variable composition containing more than 20% oxygen, assuming equilibrium conditions. However, equilibrium conditions very likely are not approached, especially since the slag after leaving the kerf is exposed to surface oxidation by the air during cooling. The thin scale remaining on the cut surface is probably of the same multi-layer constitution as scale on steel heated red-hot in air. The following paragraphs summarize information in the literature.

1910—Aston[63]

The unit heat of oxidation of silicon, manganese and carbon is greater than that of iron and contributes to increasing the temperature of the slag. Although the effect is small, the oxides of silicon and manganese lower the melting point of iron oxide.

1912—Plieninger[34]

As the oxygen purity is decreased the percentage of free iron in the slag decreases, Table 37. The total quantity of slag and width of kerf decreased as the oxygen purity was increased. Preheating the oxygen to 400° C. before it entered the torch slightly increased the percentage of free iron in the slag. Slag from plates 0.79 inch thick was less oxidized than slag from plates 1.57 inches thick. If the oxygen pressure was increased to 86 lb./in.² in cutting the thicker plate, the percentage of free iron in the slag decreased. The order of analysis of the slag was (1) iron by copper ammonium chloride, (2) FeO by $SnCl_2$ and $KMnO_4$, (3) Fe_2O_3 by CO_2 and $KMnO_4$ titration, (4) Fe_3O_4 by difference.

The slag from an oxygen-cut nickel steel (22.25 Ni) 1.57 inches thick contained 14.5 Ni when the oxygen purity was 99%, and 16.1 Ni when the oxygen purity was 95%.

1914—Amédéo[41,43]

Slag is more highly oxidized from thick plates than from thin. The slag always has a higher carbon content than the steel (no details). The sparks are metallic particles that have not remained in the kerf sufficiently long to be fully oxidized and have been oxidized during passage through the air after leaving the cut. Sometimes the sparks are decarburized, sometimes carburized.

If the slag from the bottom of the cut is suddenly cooled in water as it leaves the kerf further oxidation of free iron is arrested and the oxide content is lower than that of slag allowed to cool in air. Microscopic examination revealed that the slag from the bottom of a cut in thick steel consisted of a homogeneous oxide phase in which tiny dendrites of metallic iron were embedded. Slag from steel of medium thickness consisted of an outer layer of so-called Fe_3O_4 separated from an inner layer of so-called FeO by a two-phase zone. The slag from steel of medium thickness contained the maximum quantity of free iron, because the disintegrating action of the oxygen in forming intergranular networks of oxide around the grains of iron is most intense. It was conjectured that in thick plates more of the iron must be oxided to heat the lower portion, whereas in thin plates oxidation is "rapid and complete."

1915—Bryce[64]

Oxygen-cutting slag contains Fe_2O_3, Fe_3O_4, FeO and free iron (no details). The iron is removed as crystals in an enveloping coating of slag.

1931—Ehret[26]

Oxygen-cutting slag consists of Fe_3O_4 with some FeO and free iron (no details). There is more free iron in slag from thin plates than from thick.

1932—*Oxy-Acetylene Tips*[65]

Residues of normal oxygen cutting are largely Fe_3O_4 with some metallic iron, the proportion of which increases with increase in thickness that is cut.

1933—Malz[66]

The free iron content of slags from oxygen-cut mild steel, Table 38, increases as the thickness is decreased and reaches a maximum at a critical, medium nozzle diameter, which was considered to be the most economical from the standpoint of combustion of iron. In cutting thick material the speed is low, the surface exposed to oxidation is large and the free iron content of the slag is correspondingly low. Malz concluded that the higher cutting speeds in thin steel cut loose large amounts of unoxidized iron. For any thickness there was a critical size of nozzle that removed the maximum total amount of iron as metal and oxide for unit oxygen

consumption. The cuts were made with oxygen of 99.5% purity and at such a speed that the drag was just zero.

Table 38—Composition of Slag from Machine Oxyacetylene Cut Mild Steel. Malz[66]

Plate Thickness, Inch	Diameter of Oxygen Nozzle, Inch	Composition of Slag, % Free Iron	FeO	Fe_2O_3
0.20	0.020	11.9	57.8	30.3
0.20	0.055	20.4	57.0	22.6
0.20	0.105	23.0	56.0	21.0
0.39	0.020	9.0	56.0	35.0
0.39	0.055	18.5	55.3	26.2
0.39	0.105	20.5	54.3	25.2
0.79	0.020	5.9	54.0	40.1
0.79	0.055	15.8	53.3	30.9
0.79	0.105	17.0	52.0	31.0
1.18	0.020	4.6	52.1	43.3
1.18	0.039	10.3	51.8	37.9
1.18	0.055	14.2	51.5	34.3
1.18	0.075	16.0	51.1	32.9
1.18	0.105	13.7	50.6	35.7
1.18	0.116	11.6	50.4	38.0
2.36	0.032	6.0	49.2	44.8
2.36	0.055	10.2	48.6	41.2
2.36	0.075	11.6	48.2	40.2
2.36	0.116	8.4	47.4	44.2
3.15	0.036	5.2	48.7	46.1
3.15	0.055	8.5	48.2	43.3
3.15	0.075	10.3	47.7	42.0
3.15	0.105	8.9	47.1	44.0

Composition of Steels

Thickness	C	Si	Mn	P	S
0.20	0.08	0.01	0.44	0.086	0.032
0.39	0.17	0.04	0.47	0.066	0.014
0.79	0.07	0.02	0.38	0.100	0.051
1.18	0.11	0.07	0.41	0.071	0.034
2.36	0.12	0.03	0.44	0.07[illegible]	0.041
3.15	0.15	0.28	0.54	0.0[illegible]	0.034

Method of Quantitative Analysis

1. Iron by solution in copper ammonium chloride followed by precipitation with ammonia and titrated by Zim[illegible]ermann-Reinhardt method using $KMnO_4$.
2. Residue from $CuNH_4Cl$ treatment was dissolved in HCl. FeO determined by titration with $KMnO_4$.
3. Fe_2O_3 reduced to FeO and titrated as in (2). Instead of Fe_2O_3 the higher oxide may have been Fe_3O_4, which was also computed, or it may have been a mixture of Fe_3O_4 and Fe_2O_3.

1935—Bainbridge and Clarke[51]

Scale on machine oxygen-cut surfaces is more finely divided than on manual cuts.

1936—Schneider[52]

The approximate composition of oxygen-cutting slag is 50% FeO, 35% Fe_2O_3 and Fe_3O_4 and 15% free iron.

1936—Moss[67]

The slag consists of 60% Fe_3O_4 and 40% free iron, the latter being eroded from the sides of the kerf by the oxygen jet, with which is intermingled particles of slag.

1937—Slottman[68]

The slag consists of 30 to 40% free iron, the remainder being Fe_3O_4.

Discussion

A survey of literature on the combustion of iron in oxygen reveals that rapid oxidation in a calorimeter bomb yields a product close to Fe_3O_4. Mixter found that of 8 grams of iron ignited by an iron wire in dry oxygen at 12–15 atm., 0.2 gm. remained unoxidized, the remainder being converted to Fe_3O_4. Slow oxidation of iron in oxygen yielded Fe_2O_3 in Deiss and Leysaht's experiments. Oxidation in oxygen cutting is extremely rapid and may account for the still lower oxygen content of the slag compared with the product of the calorimeter bomb.

Since the dissociation pressure of Fe_2O_3 is less than 1 atm. below 1325° C., iron heated with excess air or any oxygen-air mixture below 1325° C. should be covered with a surface layer of Fe_2O_3. Above 1325° C. the outer layer should be Fe_3O_4. Oxidation of the slag during passage beyond the kerf may afford opportunity for the formation of some Fe_2O_3 and Fe_3O_4 observed in cutting slags by several investigators.

The oxidation of cementite, prepared by electrolysis of plain carbon steel, in a thermobalance with a temperature rise of 200° C. per hour, according to Endo, is complete at about 650° C., which is 100° C. lower than for deoxidized iron. The ignition of cementite was not studied.

Segregations

As early as 1923 Dawson[69] stated that cracking of oxygen-cut surfaces occurs mainly in dirty or defective steel. Ample confirmation of Dawson's statement is provided by Hessler's[1] recent investigation, in which a steel 0.79 inch thick containing 0.095 C, 0.44 Mn, 0.024 P, 0.032 S was machine oxyacetylene cut. The plate had a banded structure and a number of slag streaks. These were opened up where they intersected the oxygen-cut surface. It was claimed that martensite could be detected in the bands that initially were pearlitic. Miller[47] considered that oxides were more active in creating cracks than manganese sulphide. He showed that dirty steel billets 6 x 8 inches or 12 inches square were much more likely to crack during flame cutting than clean steel of the same composition. By the term "dirty" Miller meant segregations revealed by deep etching. Miller emphasized, nevertheless, that all dirty steels did not necessarily crack during oxygen cutting. Roessler[23] also found that slag inclusions favor cracks in oxygen cuts.

Besides tending to create cracks, segregations of slag may create horizontal grooves (Crowe and Deming[146]) or roughen the cut, as Wiss[7] pointed out. For example, Ehret[26] found it easier to produce smooth cuts in open-hearth steel than in Bessemer because the former is more homogeneous. Kantner[70] showed that a large cavity may be formed in a cut surface due to the melting of segregated impurities. In Wiss's[7] opinion, the gap had to be 0.02 inch or more wide before a cavity could form.

Segregations of carbon in steel (not cast iron) have little effect on flame cutting. Notwithstanding, it is common practice[71] in cutting heavily case-carburized armor plate (case several inches deep) to use the torch on the soft side. Wiss had no difficulty oxygen cutting case-carburized steel or duplex steel (0.7 C steel welded to 0.1 C steel, 1.10 inches thick). Manual cuts in the duplex steel injured the pressure-welded zone for some reason. Laminated steel with interposed layers of red lead gave the oxygen cutters no difficulty.[71]

Kinsey[72] maintained, without proof, that it was more difficult to oxygen cut hardened tool steel than annealed steel of the same composition. He believed cementite offered more resistance to oxygen cutting than pearlite.

THE EFFECT OF CARBON ON THE OXYGEN CUTTING OF HIGHER-CARBON STEEL

Preheating and Annealing

Steels with over 0.30 C should be preheated before flame cutting, according to Helmkamp,[73] who did not know the best preheating temperature but recommended

260–320° C. as being below the ignition point of the steel. Wikoff[74] advised preheating to 590° C. before flame cutting higher-carbon steels (no details) in order to prevent excessive hardening of the cut edge. The AMERICAN WELDING SOCIETY's Code for Fusion Welding and Flame Cutting in Machinery Construction[75] recommends that steels with over 0.30 C be preheated before oxygen cutting. Only steels with 0.75 C or over required preheating in Maeurer's[76] tests, the preheating temperature being 400° C. or over for steel $2^1/_2$ inches thick. Titscher[77] recommended preheating to 200° C. for all steels containing 0.30 C or more, and believed that the cuts should be annealed 30 min. at 800–900° C. Unless preheating is applied surface cracks may be found in oxygen-cut steel containing over 0.25 C, according to the International Acetylene Association,[73] who also recommended reheating to 620° C. or preheating to 430° C. to reduce the hardness of oxygen-cut surfaces in higher carbon steels. Similarly, Ullmer[79] preheated any steel over 0.25 C to 600° C. to prevent cracks and to decrease oxygen consumption and increase cutting speed as well. Preheating 0.51 C steel to 250° C. reduced carbon pick-up, but increased the grain size and the depth of heat-affected zone according to Wiss.[7,24]

An annular spray of water delivered by a perforated ring about 3 inches diameter surrounding the torch was described by Steel[82] for flame-cutting gear rims from plate 1 to 2 inches thick containing 0.50 C. The water cooling retained the original grain structure close to the edge and thus reduced the amount of metal that must be machined off to reach unaffected steel. Ketchbaw[83] preheated steel containing 0.80–0.90 C, $1^3/_4$ inches thick, to 650° C. before machine oxygen cutting. Preheating prevented the formation of minute cracks that were observed on cuts made in cold steel. The cut was completed while the steel was still at a red heat. Ketchbaw did not explain the prevention of cracks by preheating, nor did Buchanan[79] who preheated any steel over 2 inches thick in order to avoid cracks. In oxygen cutting wing-type ingots of tool steel (0.90–1.25 C) Firth and Knox[84] observed surface cracks in the cuts (10 x 12 x 30 to 12 x 16 x 58 inches) if the temperature was below 760° C. and the cuts were rough if the temperature was above 870° C. Gibson[85] had no difficulty shape cutting 0.35 C steel $6^1/_2$ inches thick (30 ft. long) if the steel were preheated to 320° C. Preheating to 650° C. not only reduced cracking but also conserved oxygen in oxycutting steel containing 0.50 C or over, according to Evleth.[86] Dawson[69] recommended preheating red-hot for forgings containing over 0.45 C. In this way the structure is improved. Before the cut cools, the part should be uniformly reheated to 790° C. and cooled slowly. Bainbridge,[49] who preheated any steel containing over 0.40 C, believed that the skin cracks sometimes found in cuts in high-carbon steel were not due entirely to carbon content. Cracks had been observed in 0.4–0.5 C steel but not in 0.8 C steel under some conditions (no details) and cracks were more prevalent on the concave side of a horseshoe cut than in a straight cut. Boggs[87] recommended the preheating of steels containing over 0.25 C, which should also be normalized after cutting. The preheating temperature for steel containing over 0.30 C should be 260–320° C. (500–600° F.) but not usually above 600° C. (1100° F.), according to Moss.[67] If the preheating temperature is too high the cut is difficult to complete.

Annealing has often been recommended as an obvious corrective of hard oxygen-cut surfaces, but not, as Krebs[88] maintained, to avoid cracks. Moss[40] describes multi-jet flame-softening torches with which to (1) preheat the line of the cut in advance of the cutting torch in order to retard the rate of cooling after cutting, (2) reheat the cut by means of torches on one or both sides of the plate, thus raising the temperature of the cut to the vicinity of the critical range and assuring retarded cooling, (3) reheat the cut surface to the vicinity of the critical range after the cut has been completed and has cooled more or less completely. The mechanical properties of plain carbon and alloy steels subjected to these treatments are summarized in the sections on Mechanical Properties of Oxygen-Cut Higher-Carbon Steels, and in several of the Alloy Steel Sections. About 2 cubic feet of each gas are consumed per linear foot for each side of the kerf on which torches are used in applying Treatment 1 to steel $^3/_4$ to $1^1/_4$ inches thick. The gas consumption is a little lower for Treatment 2 and 30 to 50% less for Treatment 3. Treatment 1 is not applicable for steel thicker than $1^1/_2$ inches, because the heat does not penetrate to the lower side of the plate in the time available. Treatment 2 is successful up to 4 inches thick but is limited by difficulties with overhang. Treatment 3 is particularly adapted to heavy plate. Bainbridge[81] referred to a tempering flame in the machine-cutting head for low-alloy structural steels to reheat the steel to 400° C. after fast cutting (steel 1 inch thick cut at 60 ft./hr.). A patented process for softening oxygen cuts was described by Claude,[89] which involved a softening flame moved an inch or so ahead of the cutting torch. Annealing restored the banded structure at the oxygen-cut edge of structural steels in Wiss's[24] tests, but did not create the coarse grain structure at the cut edge that was often found at annealed sheared edges.

There was no detrimental effect exerted on rails by oxygen cutting, according to Bainbridge,[81] particularly if the cut was torch annealed. On the other hand, Wiss[24] always observed cracks in machine oxygen-cut rails (0.4 C), and Daley[139] did not favor flame-cutting rails for main track. Melhardt,[90] among others, used no preheating whatever to oxygen-scarf rails (0.55–0.60 C) preparatory to oxyacetylene welding. A railway writer[141] used an oxygen-cutting machine at a rail-reconditioning plant to remove battered ends in 40 seconds. Deep holes in rails caused by batter were removed with the oxygen torch to a depth of 1 inch by Budach,[142] and filled with a hard steel rod. Fractures in rails also were cut away with the oxygen-cutting torch. Swinnerton[143] oxygen cut old rails to length for remelting.

The production of ice skates of 0.60 C steel by means of flame cutting was attempted by an English firm[90] but without success for, although no cracks were observed, a bead of slag accumulated on the underside of the material, which was $^3/_{16}$ inch thick. No combination of speed and oxygen pressure prevented the accumulation, which was thought to have been occasioned by the comparatively low melting point of the high-carbon steel. Peck[91] observed cracks shortly after oxy-coal gas cutting of steel rings containing 1% carbon. Although annealing before cutting was ineffective in preventing the cracks, preheating to a temperature sufficient to prevent hardening eliminated cracking. On the other hand, Hilpert[38] could find no cracks whatever in oxygen-cut surfaces of steels containing 0.4 C or 1 C and 3 Mn. In an effort to detect the most minute cracks Hilpert coated the cut surface with bronze, which penetrates the finest cracks. None was revealed, however.

Cutting Speed

Drag in automatic flame cutting as a function of carbon content was studied by de Jessey.[50] The cutting orifice was 0.06 inch diameter. There were five preheating jets. The oxygen pressure (99% pure) was 37 lb./in.^{2} for cutting and 14 lb./in.^{2} for preheating. The six

Table 39—Hardness and Bend Properties of Production Machine Oxygen-Cut Higher-Carbon Steels with and without "Flame Softening." Moss[40]

Temperature of Plate Before Cutting, ° F.	Thickness,† Inch	C	Mn	Si	P	S	Brinell Hardness* Uncut	Oxygen Cut	Flame Softened	Bend Ductility Uncut	Oxygen Cut	Flame Softened
70	1/2	0.28	0.68	0.21	...	...	153–179	203	153–166 (pre)	...	164° (C)	180° (A)
70	1	0.28	0.71	0.21	...	...	156–174	310–341	.....	40% in 1/2 in. (F)	10% in 1/2 in. (F)	...
70	1/2	0.31	0.73	0.25	...	...	159	202–248	.....	...	...	...
..	7/8	0.33	0.73	0.28	0.018	0.035‡	171	269–409	156–203 (post)	180° (C)	17°–38° (A)	180° (A)
..	7/8	0.36	0.70	0.23	...	...	163	238–241	.....	180° (C)	38° (C)	...
..	7/8	0.38	0.86	0.31	0.021	0.034	176	277–522	188–212 (post)	180° (C)	10°–15° (A)	180° (A)
50	1 3/8	0.40	0.57	0.10	0.02	0.04	153–170	364–375	146–179 (post)	180° (C)	...	...
70–80	4 1/8	0.70	0.77	0.24	0.033	0.032	179	532	235 (I.P.)	...	...	...
..	7/8§	0.73	0.73	..	...	...	222–259	583–639	254–325 255–280	34°–43° (A)	0°	3° (A) (post) 62° (A) (post)

(A)—Anvil bend test; specimen hammered with 6 to 8-lb. sledge over a pin (radius of pin = thickness of specimen = 1/2 inch).
(C)—Cold bend test.
(F)—Free bend test.
(pre)—Preheat torch preceded cutting torch to retard the rate of cooling.
(post)—Softening torch followed cutting torch to maintain the cut a brief period near the critical range.
I.P.—Cut reheated by softening torch after cooling to room temperature. The reheating was for a brief period near the critical range.
* Converted from Rockwell C scale for the harder surfaces and from Rockwell B for the softer.
† Width of strip cut and speed of cut not stated.
‡ 0.21 Cu.
§ 100 lb. ARA Type B rail.

plates were 1 inch thick 6 inches wide, and contained 0.20 to 1.41 C. The results showed that the preheating flame had to be increased as the carbon content of the steel was raised. At low speeds of cutting the preheating had to be particularly intense to produce smooth cuts in the higher-carbon steels. For a given speed, the acetylene consumption for smooth cuts varied inversely as the thermal conductivity of the steel, according to de Jessey. No relation was found between carbon content and drag. Rivière[92] stated (no details) that an increase in carbon content decreases the oxygen-cutting speed. Mitchell[140] cut heavily carburized steel plates from the soft side, because high carbon content increased the difficulty of cutting. The oxygen pressure at the regulator in lance cutting should be 25 lb./in.^{2} higher for low-carbon steel than for medium-carbon steel up to 0.5 C, according to Thum.[93] In 1921 machine oxygen cuts[94] were being made at the rate of 10 ft./hr. in steel containing 0.60 C 18 inches thick using 190 cu. ft. oxygen per ft. of cut at 125 lb./in.^{2} The speed is about 40% less and oxygen consumption about 40% greater than for good current practice on low-carbon steel. According to Ussher[95] steels containing over 0.3 C are usually cut up to 50% faster than mild steel. According to Napolitan,[96] whereas white cast iron containing 3 to 4% C can readily be oxygen cut, gray cast iron of the same carbon content can be cut only with the melting technique. (See review of literature on Flame Cutting Cast Iron, THE WELDING JOURNAL, **16** (3), Suppl., 26–28 (1937)). Graphite was a source of difficulty in flame cutting, it was concluded.

Mechanical properties of oxygen-cut higher-carbon steels

There is little information on the mechanical properties of oxygen-cut, high-carbon steel. The effect of increasing the carbon content from 0.28 to 0.40% in oxyacetylene cutting, according to Moss,[40,149] is to increase the hardness and decrease the ductility, Table 39. There is carburization of the cut surface to a depth of 0.002–0.003 inch (no details) and hardening to a depth of about 0.05 inch. The hardness of the cut surface is usually over 300 Brinell in these steels. With 0.70 C the hardness of the cut surface is over 500 Brinell. Flame softening decreased the hardness and increased the bend ductility. Microstructural examination of the oxygen-cut steels revealed a white layer about 0.001 inch thick at the cut edge which was related to carburization during cutting. The white layer persisted through the flame-softening treatment. Back of the white layer was martensite, which became tempered martensite after flame softening. The grain size in the heat-affected zone was reported to be finer than in the unaffected steel. The British Acetylene Association[25] said of a steel containing 0.35 C, 0.72 Mn that the alteration of microstructure near the oxygen-cut edge is not undesirable and is less obvious that in lower-carbon steel because the initial structure of the latter is coarser.

Table 40—Tensile Tests of Large Specimens. Moss[14]

Specimen	Composition of Steel C	Mn	Si	Width, Inch	Thickness, Inch	Yield Strength, Lb./In.^{2}	Tensile Strength, Lb./In.^{2}	Elongation, % in 18 Inches
Machined	0.33	0.8	0.3	3 1/2	0.88	43,000–44,000	79,800–81,800	15.3–12.9
Oxygen cut	0.33	0.8	0.3	3 1/2	0.88	41,600–42,100	68,000–74,200	3.1–6.2
Oxygen cut and flame softened	0.33	0.8	0.3	2.93	0.88	47,100	80,300	18.9
Machined	0.38	0.8	0.3	3 1/2	0.89	46,400–47,800	90,600–90,700	20.9–19.4
Oxygen cut	0.38	0.8	0.3	3 1/2	0.89	48,000–48,200	71,900–73,800	2.4–3.3
Oxygen cut and flame softened	0.38	0.8	0.3	3 1/2	0.89	52,700–57,200	91,200–92,800	15.6–12.2

Large machine oxygen-cut tensile specimens were found, Table 40, to have comparatively little ductility unless the edges were flame softened (no details). The decrease in tensile strength of the oxygen-cut specimen contracts with the slight increase ordinarily reported for oxygen-cut, low-carbon steel. Drift tests on a steel $^{7}/_{8}$ inch thick containing 0.38 C, 0.86 Mn, 0.31 Si, 0.021 P, 0.034 S were made by forcing drift pins by means of a steam hammer through holes originally $^{13}/_{16}$ inch diameter spaced at the quarter points 2 inches back of the edge of plates 16 inches square. The direction of rolling was not stated. The pins increased the diameter of the holes in increments of $^{1}/_{8}$ inch. Planed or flame-softened edges withstood 100% drifting (final diameter of hole = $1^{9}/_{16}$ inches) without cracking. Sheared edges cracked at an enlargement of about 60% and unsoftened oxygen-cut edges cracked at an enlargement of about 40%.

Bainbridge[97] cites Izod tests of machine oxygen cuts in a normalized forging (thickness not stated) containing 0.40 C, 0.78 Mn, 0.19 Si, 0.024 S, 0.029 P in which specimens notched in the cut surface averaged 19.0 and 19.6 ft.-lb., respectively, for blow delivered parallel and perpendicular to the grain of the steel. The corresponding values for unaffected base metal were 10.4 and 13.8 ft.-lb., which are rather low. The sorbitic structure of the cut edge was believed to account for its improved Izod value. The tensile, bend, hardness and Charpy impact values of oxygen-cut and machined specimens (no details), Table 41, were determined by Yarkho[53] for a steel containing 0.37 C, 0.60 Mn, 0.23 Si, 0.027 P, 0.033 S, 1 inch thick (no details).

Table 41—Mechanical Properties of 0.37 C Steel. Yarkho[53]

Character of Cut Edges	Rockwell B Hardness	Tensile Strength, Lb./In.²	Elongation, %	Bend Angle†	Charpy Impact Value, Mkg./Cm.²
Milled	89	76,500	20.1	180° flat	3.3
Planed	85.4	78,000	20.1	180° flat	...
Oxyacetylene cut	85.6	76,500	18.9	110°	3.3*
Oxy-benzine cut	86.6	77,000	18.7	104°	...

* 3.0 mkg./cm.² after annealing at 600° C.; 4.3 mkg./cm.² after annealing at 900° C.
† 0.59 inch thick.

A Vickers hardness survey was made by Theisinger[136] of a cross section perpendicular to an oxygen-cut surface (no details of cutting) in steel $^{1}/_{2}$ inch thick containing 0.44 C, 0.50–0.80 Mn, Fig. 3. The maximum Vickers hardness at the top of the cut edge was 275, which was slightly greater than the maximum hardness (265 Vickers) observed beneath a bead of bare electrode weld metal deposited on the surface of the plate at 12 in./min., 23 volts, 375 amp. It was assumed that absorption of carbon from the acetylene accounted for the high hardness at the top edge. Dawson[69] made a machine oxyacetylene cut 6 inches long through a cold forging 4 inches thick containing 0.37 C, 0.50 Mn, 0.14 Si, 0.027 S, 0.018 P (143 Brinell) and found that the cut surface, after being lightly ground to remove oxide, was only 207 Brinell. Perhaps Dawson used a 10 mm. ball and 3000 kg. load in determining the hardness, which is rather low. No cracks were observed. As the carbon content was increased from 0.27 to 0.52% in Nichols and Kiernan's[135] tests, the maximum Brinell hardness of the machine-cut surface in annealed steels $5^{1}/_{2}$ inches thick increased from 179 to 286. The hardness of the uncut steels was 130 (0.27 C, 0.52 Mn) or 182 (0.52 C, 0.58 Mn). There was a thin hypereutectoid layer on the cuts in the higher-carbon steel. The cutting speed was 15 ft./hr.

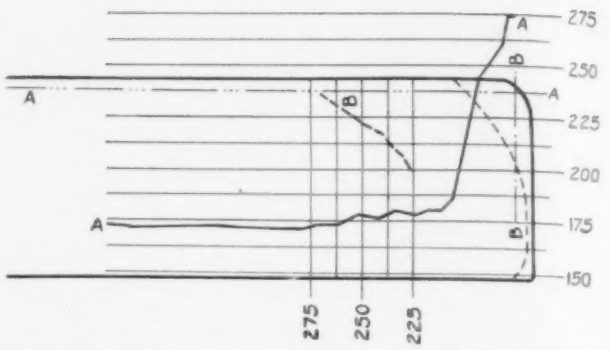


Fig. 3—Vickers Hardness Survey of Cross Section of Steel $^{1}/_{2}$ Inch Thick Containing 0.44 C, Perpendicular to Oxygen-Cut Edge. Theisinger[136]

Dotted line—Boundary of heat affected zone revealed by Nital etch.
Full Line A—Survey along line parallel to, and about 0.03 inch below, surface of plate.
Dotted Line B—Survey along line parallel to, and about 0.05 inch back of, cut edge.

In bending rails (0.47–0.54 C, 1.38–1.43 Mn, 0.24–0.32 Si, 0.038 S, 0.053–0.069 P) with oxyacetylene-cut lower flanges to curves, two Russian investigators[98] found that some rails broke during bending, others broke during unloading. Tests on a span of 3 feet with central static load showed that uncut rails bent without cracking whereas oxygen-cut rails broke without plastic deformation. The fracture resembled hardened steel to a depth of 0.03–0.04 inch below the cut surface.

THE EFFECT OF ALLOYING ELEMENTS ON THE OXYGEN CUTTING OF STEEL

Phosphorus

A smooth oxyacetylene flame cut was made without difficulty by Wiss in an iron containing 2% P. Wiss[7] believed that phosphorus had no effect on oxygen-cutting steel.

Cr-Cu-Si-P

The hardness and bend ductility of production machine oxygen-cut Cr-Cu-Si-P steel was found by Moss,[40] Table 42, to be satisfactory in thin material but not so satisfactory in thick material.

Table 42—Hardness and Bend Properties of Cr-Cu-Si-P Steel. Moss[40]

Thickness, Inch	Composition of Steel							Brinell Hardness*		Bend Angle, Degrees†	
	C	Mn	Si	P	S	Cu	Cr	Uncut	Oxygen Cut	Uncut	Oxygen Cut
$^{1}/_{4}$	0.07	0.15	0.72	0.12	0.018	0.44	0.70	132–168	132–203	180	180
1	0.10	0.20	0.73	0.13	0.021	0.41	0.88	134–175	150–258	180	94–180‡

* Converted from Rockwell scale.
† Anvil bend test; specimen $^{1}/_{2}$ inch wide hammered with 6 to 8-lb. sledge over a pin $^{1}/_{2}$ inch radius.
‡ In thicknesses of $^{3}/_{4}$ to 1 inch the steel will yield 180° if it is cut at a reduced speed. Otherwise flame softening is needed. For example, flame softening (softening torch preceded the cutting torch) reduced the hardness to 145–200 Brinell and there was no difficulty in securing 180° in the anvil bend test.

Table 43—Hardness of Oxyacetylene Cut Spring Steel. Matting[99]

Kind of Cut	Composition of Steel C	Si	Mn	P	S	Condition of Steel Before Being Cut	Rockwell Hardness of Uncut* Steel	Rockwell Hardness of Steel at x Inch Back of the Cut 0.04	0.08	0.12	0.20	0.32	0.39	Depth of Heat-Affected Zones Inch Preheat†	Cut‡
Hand	0.50	1.82	0.56	0.01	0.01	Unhardened	98.5B	93–97B	94–98B	98–99B	93–95B	95–96B	98–99B	0.12	0.032
Machine	0.54	2.03	0.60	0.01	0.01	Unhardened	99B	99–100B	97–100B	97–99B	97–98B	96–98B	98–99B	0.08	0.024
Hand	0.52	1.86	0.70	0.01	0.02	Hardened	41C	38–38.5C	42.5C	43–44C	44C	43.5–44C	43–43.5C	0.10	0.055
Machine	0.52	1.83	0.68	0.01	0.01	Hardened	40.5C	25–26C	28–31C	39C	41C	42C	42–43C	0.07	0.020

* Converted from Brinell (5 mm. ball, 750 kg. load, 30 sec.).
† Due to preheat flame; torch side of cut.
‡ Middle of thickness.

Sulphur

Wiss[7] found that an iron-sulphur alloy with 35% S could be oxyacetylene flame cut without difficulty. Sparks and SO_2 fumes were plentiful. The oxygen-cutting qualities of plain carbon steel, at any rate, Wiss concluded, were not affected by sulphur.

Nitrogen

There is almost no information on the oxygen cutting of steels containing nitrogen. Hessler[1] stated that he found tiny nitride needles in the zone of an oxygen-cut, low-grade steel $^7/_8$ inch thick (0.095 C, 0.44 Mn, 0.024 P, 0.032 S) that had reached 100 to 200° C. during cutting. The zone was 0.016–0.028 inch wide and appeared in both manual and machine-cut strips. Haynes[44] correctly combated the belief that nitrogen as an impurity in the oxygen may harden the cut. However, Miller[46] believed it possible that some of the carburization he observed on oxygen-cut edges of Armco iron billets 4 inches square may have been due to nitrogen (no details).

Oxygen

Oxygen in steel should not have much effect on oxygen cutting, although scale on the surface to be cut increases the preheating time. Since Armco Iron scales more rapidly than low-carbon steel it may be concluded that high oxygen content ought not retard oxygen cutting.

Aluminum

Wiss[7] was unsuccessful in oxyacetylene cutting mild steel in the core of which aluminum (several per cent) had segregated. On the other hand, calorized steels were easy to cut if the calorized layer was relatively thin (no details). Steel with 10–15% Al could not be flame cut even if both oxygen and steel were preheated. Pure aluminum could not be oxygen cut. Bonhomme[144] stated that up to about 6% Al has no effect on oxygen cutting. Beyond 6% solid Al_2O_3 considerably increases the difficulty of oxygen cutting.

Molybdenum

Pure molybdenum does not cut so rapidly as iron, Wiss[7] found. He stated that 5.5% Mo added to a steel containing 8 W, 1.4 Cr, 1 C prevented it from being cut by the oxyacetylene cutting torch. The last statement is believed to be incorrect (private communication, Air Reduction Sales Company, October 1938).

Silicon

Wiss[7] was able to oxyacetylene flame cut transformer iron with 4% Si and very low carbon, but the operation was slower than with mild steel. There was no change in the magnetic properties or structure at the edge of cut made through a packet of 300 sheets each 0.0138 inch thick and coated with varnish. If, however, 0.2 C was present with 4 Si, the steel could not be flame cut. High silicon iron (15 Si, 0.5 C) and silicon itself could not be flame cut with or without preheat.

The hardness of flame-cut spring steel, Table 43, was practically the same as unaffected base metal in Matting's[99] tests. The hardness measurements were made on the torch side of the cut. Machine cutting hardened the unhardened steel and softened the hardened steel to greater extents than hand cutting, and created smaller heat-affected zones. Examination of the ground surface of the cut with a microscope (60×) revealed no cracks.

Si-Mn

Production machine oxygen cuts made by Moss[40] at 70 to 80° F. in a steel $3^1/_{16}$ inches thick containing 0.40 C, 1.51 Mn, 2.00 Si, 0.018 P, 0.028 S, 140 Brinell annealed, were 469 Brinell. The hardness was reduced to 241 Brinell (converted from Rockwell) by reheating the cut surface near the critical range with a flame-softening torch.

Manganese

Tensile and bend specimens were cut by Dümpelmann[100] from a plate 0.39 inch thick containing 0.12 C, 1.00 Mn, 0.62 Si, 0.26 Cu, 0.04 Ni, 0.00 Cr, 0.088–0.098 P, 0.024 S. The tensile specimens were 0.39, 0.79, 1.18 or 1.58 inches wide. Milled specimens had a tensile strength of 76,500 to 77,500 lb./in.^{2} parallel to the direction of rolling, and 76,500 and 78,000 lb./in.^{2} perpendicular to the direction of rolling. Illuminating gas, acetylene and hydrogen were used as preheating gases.

The detailed results showed that the yield strength (57,000 lb./in.^{2}), tensile strength, elongation (25% in 7 or 11 inches), and reduction of area (60%) were practically the same for flame cut as for the milled specimens. The width of the specimen, that is, the distance between cuts, had no effect. Bend tests on specimens 1.18 inches wide, 10 inches long cut parallel and perpendicular to the direction of rolling bent 180° in practically all cases (no details). Some cracks caused by laminations were observed. The notch-impact value of standard German round-notch specimens with notch on lower side of, or in, the cut surface was 6.8 to 7.4 mkg./cm.^{2} parallel to the direction of rolling, and 11.7 to 14.2 mkg./cm.^{2} perpendicular to the direction of rolling. Milled and oxygen-cut specimens had practically the same notch-impact value. The maximum Brinell hardness (2.5 mm. ball, 187.5 kg. load) was 202 for acetylene, 219 for hydrogen and illuminating gas. Base metal was 156 Brinell. The depth of the heat-affected zone was 0.04 inch.

Quite different results have been obtained by Moss,[40]

Table 44, who used steels of higher carbon content. Drift tests on a steel $^{7}/_{8}$ inch thick containing 0.35 C, 1.43 Mn, 0.20 Si, 0.028 P, 0.027 S were made by forcing drift pins with the aid of a steam hammer through holes originally $^{13}/_{16}$ inch diameter spaced at the quarter points 2 inches back of the edge of plates 16 inches square. The direction of rolling was not stated. The pins increased the diameter of the holes in increments of $^{1}/_{8}$ inch. Planed or flame-softened, oxygen-cut edges withstood 100% drifting (final diameter of hole = $1^{9}/_{16}$ inches) without cracking. Sheared edges cracked at an enlargement of about 60%, and unsoftened, oxygen-cut edges cracked at an enlargement of about 40%. Microstructural examination of the oxygen-cut steels revealed a white layer about 0.001 inch thick at the cut edge which was related to carburization during cutting. The white layer persisted through the flame-softening treatment. Back of the white layer was martensite, which became tempered martensite after flame softening. The grain size in the heat-affected zone was reported to be finer than in the unaffected steel.

The ductility and, to a smaller extent, the strength Moss[40] found were decreased by oxygen cutting to an extent that production machine oxygen cuts are troublesome to machine and deficient in some fabricating qualities unless the cuts are heat treated. Discussing these results, A. E. Gibson stated that no cracks are observed in steels containing less than 0.30 C, 1.50–1.75 Mn. Furthermore, flame softening is not required if the parts are to be heat treated after welding.

Cold bend tests, Table 46, on oxyacetylene-cut surfaces of a medium-manganese steel convinced Wilkinson and O'Neill[134] that grinding the edges of the cut to a radius of $^{1}/_{8}$ inch is essential if satisfactory bend properties are desired. A white layer similar to that observed by Moss was found on the cut surfaces of steel $1^{1}/_{8}$ inches thick containing 0.26 C, 0.04 Si, 1.48 Mn, 0.32 Cu, 0.024 S, 0.034 P (yield strength = 54,000 lb./in.^{2}, tensile strength = 85,000 lb./in.,^{2} elongation = 24% in 8 inches). The cut was made with hydrogen as preheating fuel. The hard, white layer was up to 0.008 inch deep, 725 Vickers hardness. The hard layer seemed to consist of martensite and troostite, was present in patches over the cut surface, and, it was surmised, consisted of metal which had been molten during cutting and which had subsequently solidified under conditions of rapid cooling. Annealing in vacuum at 925° C. converted the structure at the edge to soft ferrite nearly free from pearlite. The ferrite layer was about 0.003 inch thick and merged abruptly into a layer of pearlite containing no hypoeutectoid ferrite. The pearlitic zone, 0.002 inch thick, merged gradually with the structure of unaffected base metal, which was attained at about 0.015 inch from the edge of the cut. A similar phenomenon was observed by de Jessey[33] in 0.3 C steel, $6^{1}/_{2}$ in. thick, after annealing, there was a low-carbon band at the edge backed by a pearlite streak. De Jessey believed that longer annealing would have homogenized the structure.

The annealed specimen, after being carburized at 925° C., revealed that the outermost layer was abnormal, suggestive of relatively high oxygen content. The layer effect in the annealed specimen therefore was ascribed to oxygen gradients, the precise action of which was not stated. Wilkinson and O'Neill did not state whether the high oxygen content of the accretions was derived from the stream of cutting oxygen. The mechanical properties of the annealed oxygen-cut surfaces were not determined.

Manganese is easier to oxyacetylene flame cut than iron, according to Wiss.[7] The hardness of flame-cut surfaces of die steel containing 3 Mn, 0.9 C, 1 inch thick was:

Manual cut, acetylene preheating flame—over 600 Brinell.
Machine cut, acetylene preheating flame—500–530 Brinell.
Manual cut, hydrogen preheating flame—430–460 Brinell.
Uncut base metal—230 Brinell.

Since steel containing 0.25–0.35 C, 1.25–1.70 Mn is air hardening, Jennings[101] recommends that special precautions be taken in flame cutting it to avoid cracks. Preheating the line of the cut is advisable with heavy sections. There is no difficulty in oxyacetylene flame cutting 14% Mn steel, according to Wiss.[7,24] Manual-cut surfaces, however, are coated with ledeburite (high carbon brittle eutectic), which sensitizes the surface to cracks.

Table 44—Tensile Bend and Hardness Properties of Oxygen-Cut Medium-Carbon, Pearlitic Manganese Steels. Moss.[40] Tensile Tests on Steel $^{7}/_{8}$ Inch Thick, Containing 0.33 C, $1^{1}/_{2}$ Mn (Presumably) and Machined or Oxygen Cut to a Width of $3^{1}/_{2}$ Inches

Specimen	Yield Strength, Lb./In.^{2}	Tensile Strength, Lb./In.^{2}	Elongation, % in 18 Inches
Machined	52,300–52,600	93,100–92,400	17.2–18.8
Oxygen cut	52,600–52,700	67,800–71,100	1.4–1.7
Flame softened Oxygen cut	57,100–60,000	91,400–92,900	15.0–18.1

Hardness and Bend Tests

Thickness, Inch	Composition of Steel C	Mn	Si	P	S	Brinell Hardness* Uncut	Oxygen Cut	Flame Softened	Bend Ductility† Uncut	Oxygen Cut	Flame‡ Softened
$^{1}/_{2}$§	0.26	1.23	0.23	0.019	0.024	165–183	302–332	179	180° (B) 38–46% (F)	0° (B) 8% in $^{1}/_{2}$ in. (F)	O.K. (B) (pre)
$1^{1}/_{2}$	0.27	1.61	0.13	...	...	174	435–444	...	55–70° (B)	11–15° (B)	180° (A) (pre)
1	0.29	1.55	0.25	0.022	0.025	150–200	269–444	179–285	180° (A)	20–72° (A)	180° (A) (pre)
$^{1}/_{4}$	0.30	1.43	0.22	0.030	0.034	156–200	203–421	179–245	180° (A)	42–132° (A)	180° (A) (pre)
$^{7}/_{8}$	0.33	1.43	0.20	0.028	0.027	181–196	344–486	166–241	180° (A)	7–32° (A)	180° (A) (post)
$^{5}/_{8}$ (T)	0.39	1.30	0.18	0.02	0.04	166–179	460–555	179	...	0° (A)	180° (A) (post)

(T)—Cut at about 50° F.
*—Converted from Rockwell.
† (A)—Anvil (impact) bend.
(B)—Cold bend.
(F)—Free bend.
‡ (pre)—Preheating torch preceded the cutting torch to retard the rate of cooling.
(post)—Softening torch followed cutting torch to maintain the cut a brief period near the critical range.
§—A steel containing of 0.29 C, 1.19–1.23 Mn, 0.25 Si reached 444 Brinell on the cut surface. Oxygen cutting of the steel without flame softenng was reported to be unsatisfactory at a meetng of the Structural Steel Welding Committee in 1933.

No ledeburite was observed on machine-cut surfaces, the hardness of which was 320 Brinell (base metal—220 Brinell). The increase in hardness in the machine cuts was ascribed to a thin layer of martensite on the surface. Beyond the martensitic zone was a zone in which carbide precipitation had occurred in the austenite. The heat-affected zone in flame cut 14% Mn steel is not so deep as in mild steel because the thermal conductivity of the 14% Mn steel is low. If the cutting torch is followed by a stream of water, the edge remains austenitic but, nevertheless, carbide precipitation occurs in the grain boundaries. Therefore, Wiss believed that flame cut 14% Mn steel should be heat treated by heating the complete part uniformly to, and quenching from, a high temperature.

Trissal[102] stated that 14% Mn steel melts but does not burn (no sparkling) during flame cutting. Yet he stated that the flame-cut surface must be ground in order to remove oxidized metal or ledeburite. As little heat as possible should be used to minimize carbide precipitation. According to Davis,[103] a higher oxygen pressure than usual is necessary in flame cutting 14% Mn steel, but if an excessive amount of oxygen is used it cools the cut, the slag freezes, and cutting ceases, according to R. P. Winton (private communication, June 1938).

Industrial Gases[137] recommended higher preheat intensity for 14% Mn steel than for mild steel, but achieved high cutting speeds, Table 45, although the oxygen pressure was high. Risers of 14% mn. steel weighing 1 ton were cut by giving the torch an oscillating motion and by pumping water through the kerf during cutting to reduce the temperature and prevent cracks, which otherwise spread into the casting whether it was preheated or not.

Table 45—Oxygen Pressure and Cutting Speed for 13% Mn Steel[137]

Thickness of Steel, Inch	Cutting Oxygen Pressure, Lb./In.^{2}	Cutting Speed, Ft./Hr.
$^5/_8$	60	60
$1^1/_2$	75	52
5	80	45

Ni-Mn

Orr[104] had no difficulty in oxygen cutting steel $^1/_8$ to $^3/_8$ inch thick containing 0.25–0.40 C, 10–12 Ni, 6–8 Mn.

Cu-Si-Mn-Mo

Tests were made by Dümpelmann[105] on flame-cut steel 0.39 inch thick containing 0.13 C, 1.00 Mn, 0.60 Si, 0.42 Cu, 0.1 Mo. The tests and test results were practically identical with those obtained on flame-cut steel containing 0.12 C, 1.00 Mn, 0.62 Si, 0.26 Cu, described in the section on Manganese. The distance between cut surfaces (0.39 to 1.6 inches) had no effect on tensile strength, elongation or reduction of area. Unaffected base metal was 180 to 187 Brinell. The maximum Brinell hardness (2.5 mm. ball, 187.5 kg. load) of the cut surface was 211 for oxyacetylene cuts, 219 for oxyhydrogen cuts and 202 for oxy-illuminating gas cuts.

Chromium

With up to 3.5 Cr there is no difficulty in oxidizing the metal during flame cutting, according to Kinzel and Crafts.[106] The slag flows freely and there is little danger of cracking. The steels with high carbon and chromium (up to 10% Cr) are air hardening and must be heat treated to soften the kerf.

For relatively small sections of simple shape, according to W. Crafts (private communication, June 1938), the maximum safe carbon content is not much lower than for plain carbon steel, that is, 0.30 to 0.35 C for both 1 and 3% Cr steels. The comparable carbon contents for higher chromium steels are appreciably lower. If the shape is unfavorable, or the cross section is large, or the carbon content exceeds the safe limit, preheating or retarded cooling is advisable.

A detailed study of hardness in oxygen-cut steel (0.20 C, 0.20 Cr, 0.97 Mn, 0.046 Si, 0.055 P, $1^5/_{16}$ inch thick, 168–174 Brinell) by Yatsevitch[107] showed that the depth of the heat-affected zone was 0.052 inch except in the top $^1/_4$ inch of the cut where the depth reached 0.156 inch. The maximum Brinell hardness was 303 at the bottom edge, 215–223 in the middle of the cut, and 244 at the top edge (torch side). Evidently the cooling rate was slowest in the middle of the thickness although there the cut is backed on all sides by cold metal. The cutting was done with city gas at 10-lb. pressure (oxygen pressure = 27 lb.). The cutting speed was 8 inches per minute and the cutting orifice was No. 58 drill. Risers of 4–7 Cr castings can be oxyacetylene flame cut if the casting is preheated to 480° C. (900° F.), according to Priestley.[108]

Dwyer[110] recommends that risers on castings of 4–6 Cr steel should be preheated to 480° C. (900° F.) for flame cutting to avoid cracks, but should not be preheated above 540° C. (1000° F.), otherwise the casting is subject to air hardening during cooling. Only if preheated white-hot can pure chromium be oxyacetylene flame cut, according to Wiss.[7] Steels with 1.5 Cr are easily flame cut but, even if preheated, steels with 10–20 Cr cannot be cut. Wiss[24] also found that the cut edge of steels with 2.2 Cr, 0.3 C or higher is martensitic (thickness not stated). Oxyacetylene-cut steel with 2.1 Cr, 1.2 C was ledeburitic at hand-cut edges, but machine-cut edges were not ledeburitic. De Jessey[50] was able to oxygen machine-cut rolled steels 1 inch thick containing 0.71 C, 0.22 Si, 0.28 Mn, 1.95 Cr, and 0.24 C, 2.90 Si, 0.41 Mn, 3.73 Cr, but was unable to cut steel containing 0.26 C, 2.54 Si, 0.42 Mn, 6.20 Cr. There was no drag at cutting speeds of 25–35 ft./hr. but the cut surface was rough. Smoother cuts were obtained at 55 ft./hr. but the drag was about $^1/_8$ inch. The depth of the heat-affected zone determined by microstructure was only 0.03–0.04 inch at 45–55 ft./hr. compared with 0.06 inch in mild steel at the same speed. Perhaps the high critical points of the chromium steels account for the difference. The effect of silicon was not discussed. The 6.2% Cr steel could not be cut even if preheated or if the oxygen pressure was increased or the orifice enlarged. An English welding magazine[109] found steels containing more than 8% Cr impossible to flame cut, because the "chrome" slag opposed the propagation of the oxidation of iron.

Cr-Cu-Mn

The hardness and microstructure of oxy-coal gas cut surfaces in a steel $1^1/_8$ inch thick containing 0.23 C, 0.13 Si, 0.94 Mn, 0.031 S, 0.042 P, 0.32 Cr, 0.39 Cu (yield strength = 53,000 lb./in.^{2}, tensile strength = 83,000 lb./in.^{2}, elongation = 26% in 8 inches, reduction of area = 49%) has been studied by Wilkinson and O'Neill.[124] The Vickers hardness of the cut surface, 0.04 to 0.20 inch from the top surface, did not exceed 300 at a cutting speed of 30 ft./hr., but increased to 400 at a speed of 55 ft./hr. Often at the higher cutting speeds there was a very hard layer up to 0.008 inch deep, which was 725 Vickers. The hard layer seemed to consist of martensite and troostite and, with incorrect torch

Table 46—Cold Bend Tests on Oxyacetylene-Cut Surfaces of Low-Alloy Steels 1 1/8 Inches Thick. Wilkinson and O'Neill[104]

Treatment After Cutting	Surface in Tension T = Top B = Bottom	Steel Containing 0.24 C, 0.10 Si, 1.52 Mn, 0.07 Cu, 0.02 Cr, 0.033 S, 0.036 P: Bend Angle, Degrees	Observations	Steel Containing 0.20 C, 0.09 Si, 0.94 Mn, 0.37 Cu, 0.46 Cr, 0.030 S, 0.040 P: Bend Angle, Degrees	Observations
			Cutting Speed = 50 ft./hr.		
Scale removed	T	23	Broken	67	Broken
Scale removed	B	23	Broken	53	Small cracks
Edges ground, radius = 1/16 inch	T	61–100	Small cracks	180	Ends 2 inches apart; small cracks
Edges ground, radius = 1/16 inch	B	180	Ends 1/2–1 1/2 inches apart; small cracks	180	Ends touching; free from cracks
Edges ground, radius = 1/8 inch	T	180	Ends touching; cracks	180	Ends touching; small cracks
Edges ground, radius = 1/8 inch	B	180	Ends touching; free from cracks	180	Ends touching; free from cracks
			Cutting Speed = 30 ft./hr.		
Scale removed	T	42	Small cracks; but specimen with edge ground to radius of 1/16 inch was free from cracks even at a cutting speed of 50 ft./hr.	180	Ends touching; cracks
Scale removed	B	180	Ends touching; free from cracks	180	Ends touching; free from cracks
Edges ground, radius = 1/16 inch	T	180	Ends 1 1/8 inches apart; small crack	180	Ends touching; free from cracks
Edges ground, radius = 1/16 inch	B	180	Ends touching; free from cracks	180	Ends touching; free from cracks
			Properties of Uncut Steels		
Yield strength, lb./in.²			49,500		49,700
Tensile strength, lb./in.²			83,500		80,500
Elongation, % in 8 inches			25		28
Reduction of area, %			50		59

The specimens were 12 x 2 3/8 x 1 1/8 inches. The cuts were made in the direction of rolling. The specimens were bent on a span of 6 1/2 inches with a plunger 3 inches diameter, the cut surfaces forming the sides of the specimen.

adjustment, was found on plates as little as 1/2 inch thick. Annealing in vacuum at 925° C. converted the hard layer to a soft layer of ferrite at the cut surface above a zone of pearlite. The banded structure was ascribed to oxygen gradients in the steel.

The bend ductility of oxygen-cut surfaces (preheating fuel not stated) in a Cr-Cu-Mn steel was satisfactory, Table 46, only if the cut edges were ground.

Séférian and Leroy[30] have determined the corrosion resistance of flame-cut Cr-Cu steel (0.20 C, 0.52 Cr, 0.57 Cu, 0.70 Mn, 0.27 Si) in saline solution and 20% H_2SO_4 (See section on Corrosion).

Nickel

The S. A. E. nickel steels[111] of the 2000 series can be flame cut cold unless the carbon is above 0.30–0.35%, in which case preheat to 260–315° C. (500–600° F.). If the carbon content is above 0.50, preheat to 260–315° C., and anneal at 790–820° C. (1450–1500° F.) after cutting. Sometimes annealing need not be done if the cut parts are slowly cooled from the preheat and promptly machined. Much depends on the size and shape of the parts and on the service for which they are intended. Wiss[7,24] had no difficulty in oxyacetylene flame cutting steels with up to 7% Ni, e.g., 0.4 C, 5 Ni, but with 34% Ni there was great difficulty if the carbon content exceeded 0.5%, e.g., 34 Ni, 0.7 C.

In support of his belief that the hardness of flame-cut surfaces of alloy steels is generally no greater than that of similar surfaces of unalloyed steel of the same carbon content, Wiss showed that oxyacetylene cuts in steels containing 0.12 C, 3.7 Ni (174 Brinell) and 0.28 C, 3.2 Ni (207 Brinell) were 270 and 375 Brinell, respectively. The annealed cuts were 217 and 255 Brinell, respectively.

Machine oxygen cuts were made by Nichols and Kiernan[135] in a nickel steel forging, 8 1/2 x 6 1/2 x 5 1/2 inches, containing 0.38 C, 0.58 Mn, 0.15 Si, 0.005 S 0.039 P, 3.44 Ni that had been annealed 1 1/2 hours at 850° C. The cuts were made with hydrogen as well as acetylene at 15 ft./hr. through the 5 1/2 inch dimension. The maximum Brinell hardness of the cut surface was 364 with acetylene and 286 with hydrogen, the unaffected steel being 195 Brinell. The acetylene cut was martensitic, the hydrogen cut troostitic. However, a white-etching layer was observed at the hydrogen cut, the structure of which was not explained.

Tensile, bend and hardness tests of medium-carbon, structural nickel steels have been made by Moss,[40] Table 47. Drift tests on a steel 7/8 inch thick containing 0.39 C, 0.66 Mn, 0.12 Si, 3.32 Ni, 0.037 P, 0.20 S yielded results identical with those described in the sections on Higher-Carbon Steels and Manganese Steels.

Oxygen cutting greatly reduced the ductility and, to some extent, the strength of the nickel steels, but hardened the edge to 500 Brinell. The subject of cracks was not discussed by Moss. Flame softening restored uncut properties to the oxygen-cut edge.

Microstructural examination of the oxygen-cut edge of the steel containing 0.33 C, 3.21 Ni, 0.26 Cu, revealed a structureless (magnification = 1500 diam., presumably nital etch) zone less than 0.001 inch thick at the edge, which was called austenite and persisted in the flame-softened specimens. Beneath the white, structureless zone was martensite, which became tempered martensite in the flame-softened specimen.

In the Bureau of Standards' investigation[57] of cutting torches in 1922 it was found that some torches could not be made to cut a cast steel billet 10 inches thick containing 0.38 C, 1.21–1.28 Mn, 0.25–0.28 Si, 0.96 Cu, 2.62–2.70 Ni, <0.01 Cr, with the techniques customary at that time. Difficulty was attributed to thickness rather than to composition. The oxyhydrogen torch made smoother cuts than the oxyacetylene. The use of torch and lance

Table 47—**Properties of Production Machine Oxygen-Cut Nickel Steels (Steels Contained about 3¼ Ni, 0.8 Mn). Moss[40]**

Tensile Tests

Specimen	Width, Inch	Thickness, Inch	Carbon Content	Yield Strength, Lb./In.²	Tensile Strength, Lb./In.²	Elongation, % in 18 Inches
Machined	3.0	0.785	0.33	69,600	103,000	15.0
Oxygen cut	2.9	0.785	0.33	67,800	88,300	1.7
Flame softened, oxygen cut	2.9	0.783	0.33	72,100	102,400	12.2
Machined	3.5	0.88	0.29	67,000–67,900	99,400–99,700	15.3–13.9
Oxygen cut	3.5	0.89	0.29	68,600–73,300	88,000–89,200	1.7–1.8
Flame softened, oxygen cut	3.4	0.89	0.29	64,800–68,100	96,600–99,600	11.7–14.7

Hardness and Bend Tests

Thickness, Inch	Composition of Steel						Brinell Hardness*			Bend Angle, Degrees, in Anvil Bend Test		
	C	Mn	Si	P	S	Ni	Uncut	Oxygen Cut	Flame Softened	Uncut	Oxygen Cut	Flame Softened
⅞	0.29	0.66	0.12	0.037	0.020	3.32	226–240	430–504	196–237	180°	9–20°	180° (post)
¾	0.31	0.66	..	0.017	0.025	3.39	.....	437–486	179†	180°	...	180° (post)
⅝	0.33	0.71	‡	0.025	0.025	3.21	214–219	462–539	195–212	180°	5–44°	180° (post)

* Converted from Brinell.
† Flame softened cuts in this grade of steel are softer than the as-rolled steel.
‡ 0.26 Cu.
(post)—Softening torch followed the cutting torch to maintain the cut a brief period near the critical range.

simultaneously in cutting 3% Ni steel ingots 20 to 24 in. thick is described by *Industrial Gases.*[151]

The International Acetylene Association[3] found that hand and machine oxygen-cut surfaces in steel ⅜ to 1½ inches thick containing 0.18 C, 0.43 Mn, 2.44 Ni had 36 to 68% elongation in free-bend test. There were no serious cracks but some hardening on the torch side. The depth of altered structure was 0.05 inch.

In 1914 Plieninger[34] had no trouble oxyacetylene cutting steel containing 22.25% nickel, 1.6 inches thick (99% purity oxygen). The cut edge was steel-gray in color and was martensitic, the depth of the heat-affected zone being 0.11 inch. A layer of pure nickel 0.004 inch was found at the very edge. Filings from this layer analyzed 48.9% Ni. Nickel steels are not difficult to cut, a French writer[112] found, but the nickel is not completely oxidized and forms a hard crust on the lower face of the plate along the line of the cut. The hard layer may be softened by passing the torch over the reverse side of the plate after cutting. The oxygen pressure should be 60 lb./in.² for nickel steels 1¼ to 1⅝ inches thick. Nickel steels up to 33% Ni are not difficult to cut, according to de Jessey;[50] the heat-affected zone is small (only 0.006–0.008 inch deep, no details).

On the contrary, Holler and Schneider[37] found that the heat-affected zone revealed in the microstructure was 3 to 10 times deeper in nickel steels (1 to 5% Ni, 0.1–0.3 C) than in plain carbon steels of the same carbon content. The affected zone was 0.04–0.06 inch deep in 0.20-inch nickel steel (no details) at a cutting speed of 16½ inches per minute, and was 0.20–0.32 inch deep in 10-inch plate at a speed of 1.2 inches per minute.

Lacroix[113] used an oxyacetylene torch to cut a meteor containing 91.13 Fe, 8.39 Ni, 0.38 Co, 0.23 P, 0.07 Mn, 0.06 C, 0.01 S. The slag consisted entirely of Fe_3O_4. The maximum hardness in the vicinity of a cut in structural nickel steel plate ¼ inch thick (no details but unaffected steel was 200 Brinell), according to Rose[132] was 258 Brinell using an oxy-city gas torch at a speed of 15 inches per minute. There was practically no difference in hardness between top and bottom surfaces. The heat-affected zone was about ¼ inch deep. The oxyacetylene cutting torch has been successful in removing surface defects (de-seaming) 3½% nickel-steel slabs.[114]

The conditions in flame cutting are similar to those prevailing in flame hardening, with respect to depth of heat-affected zone, but not with respect to cooling velocity. It is well known that nickel steels of the S. A. E. 2340 class can be flame hardened to 512 Brinell without difficulty, the depth of the hardened zone being slightly greater in nickel steels than in plain carbon steels because the nickel steels have a lower critical range and are deeper hardening than the unalloyed steel.

Wiss[7] showed that oxyacetylene flame cutting of nickel steel, Table 48, raises the surface hardness. As a general rule, Wiss believed that the hardnes of flame-cut surfaces of alloy steels is no greater than that of plain carbon steel of the same carbon content. The Brinell indentation was made on the cut surface. De Jessey's machine-cutting experiments on steels 1 inch thick are summarized in Table 49. The cutting orifice was

Table 48—**Surface Hardness of Oxyacetylene Flame-Cut Nickel Steel. Wiss[7]**

Steel C	Steel Ni	Brinell Hardness Base Metal	Flame-Cut Surface	Annealed Flame-Cut Surface
0.12	3.7	174	270	217
0.28	3.2	207	375	255

Table 49—**Machine Flame Cutting of Nickel Steel, de Jessey[50]**

Composition of Steel C	Si	Mn	Ni	Speed of Cut, Ft./Hr.	Acetylene Consumption Cu. Ft./Hr.	Drag, Inch
0.09	0.3	0.49	1.87	30	10	0.06
				75	10	0.43
0.31	0.38	0.49	2.17	25	10	0
				75	11	0.51
0.13	0.29	0.39	5.20	25	11	0
				75	11	0.47
0.10	0.35	0.53	6.19	25	11–14	0
				75	10–14	0.43
0.24	0.29	0.91	33.76	35	16–18	0
				75	18	0.32
0.20	0.15	0.70	...	25	4–10	0
				75	4	0.28

0.06 inch diameter and there were 5 preheating jets. It was found necessary to intensify the preheating flame to cut the nickel steels compared with unalloyed, low-carbon steel. The acetylene consumption appeared to increase as the thermal conductivity of the steel decreased. The cut made at 35 ft./hr. and 18 cu. ft. acetylene per hr. in the 34% Ni steel was as smooth as cuts made at the same velocity in plain carbon steel. The depth of the zone in which the microstructure was altered by the cutting heat was 0.06 inch in the plain carbon steel (10 cu. ft. acetylene per hr.), 0.08 inch in the 6% Ni steel (10 cu. ft. acetylene per hr.), but only 0.008 inch in the 33% Ni steel (18 cu. ft. per hr.), the cutting speed being about 40 ft./hr.

Cr-Ni

Hardness and bend tests were made by Moss[40] on production machine oxygen cuts in a steel 2 inches thick containing 0.28 C, 0.23 Mn, 0.08 Si, 0.014 P, 0.021 S, 3.29 Ni, 1.29 Cr. The cutting was done at 70 to 80° F. Flame softening was accomplished by reheating the cool cut to about the critical range.

Vanadium

Low-carbon vanadium steels should be heat treated after oxygen cutting, in the opinion of the International Railway Congress Association.[115] With less than 0.18 C and about 0.10 V, according to A. W. Demmler (private communication, May 1938) there is no need for heat treatment after oxygen cutting. Janiszewski[116] briefly describes the oxygen cutting of carbon-vanadium tool steel dies and their welding to machine steel in making composite dies.

Copper

Aside from Séférian and Leroy's[30] corrosion tests and the statement by Bonhomme[144] that steels with up to 3% Cu give no trouble in oxygen cutting, there is no information available.

Cu-Ni-Mo

According to Miller and Lichtenwalter,[117] there is no difficulty in oxygen cutting steel containing 0.5–1.5 Cu, 0.5–1.0 Ni, 0.10 Mo, min. (Grade 1 = 0.12 C max., Grade 1 A = 0.20–0.30 C), but the cut edge should be machined to a depth of $^1/_{32}$ to $^1/_{16}$ inch before welding.

Table 50—Mechanical Properties of V-Butt Welds on Oxygen-Cut Scarves. Clarke[5]

Specimen	Yield Strength, Lb./In.²	Tensile Strength, Lb./In.²	Elongation, % in 2 Inches	Izod Value, Ft./Lb.	Angle of Bend, Degrees*
Unwelded base metal	42,200	63,000	49	...	180
Weld, oxyacetylene cut scarves	41,500	63,000	25	54.75	180
Weld, oxy-coal gas cut scarves	40,300	63,200	14	...	180
Weld, scarves sheared, then machined†	41,700	64,500	13	54	180

* Radius of pin = 1 inch.
† 70° V-butt weld.

Tungsten

The flame cutting of tungsten steels is not difficult, according to Wiss,[7] if the tungsten content is not above 10% with or without 5% Cr at 0.8% C. Steel with 17 W can be flame cut only if it is preheated and the oxygen pressure is increased or if the cutting technique for cast iron is adopted.[150] Steels with over 20% W can not be flame cut even with preheat.[7,150] Pure tungsten must be preheated to a very high temperature before it can be flame cut (no details). Firth and Knox[84] were able to oxygen cut a steel containing 0.30 C, 10 W with great difficulty.

WELDING ON OXYGEN-CUT SCARVES

In the review of literature on Welding Coated Steel (American Welding Society Journal, **18** (2), Suppl., 33–43, Feb. 1939) there was no indication that oxide-coated steel provided special difficulty in fusion welding. Consequently, it would be expected that oxygen-cut surfaces would be acceptable for fusion welding. Compared with machined scarves, oxygen-cut scarves may be a trifle less smooth and are accompanied by a heat-affected zone. The roughness of oxygen cuts has never been a source of complaint. It is conceivable that in high-carbon steels the hardened oxygen-cut surface might encourage cracks, but none has been reported. The coating of scale on the oxygen-cut surface is the only source of difficulty that has been mentioned. The following sections show that the scale exerts either no effect or only slight effect on the resulting welds, and that

Table 51—Tensile and Bend Properties of Welds on Oxygen-Cut Scarves. Tetzlaff[55]

Scarves	Electrode	Butt Weld Tensile Strength, Lb./In.²	Butt Weld Elongation, % in 1.18 Inches	Bend Angle, Degrees	Fillet Weld Strength,* Lb./In.²
Both sheared	Bare	71,000	11.7	44	66,000
One sheared, one oxygen cut	Bare	66,500	15	66	55,000
Both oxygen cut	Bare	61,500	11.7	68	61,500
Both sheared	Light coated	65,000	10	45	69,500
One sheared, one oxygen cut	Light coated	66,000	16.7	74	61,500
Both oxygen cut	Light coated	64,000	15	81	57,000
Both sheared	Heavy covered	72,000	20	89	59,500
One sheared, one oxygen cut	Heavy covered	61,000	31.6	94	61,000
Both oxygen cut	Heavy covered	65,500	31.6	98	59,000
Both sheared	Cored	75,500	18.3	61	82,500
One sheared, one oxygen cut	Cored	66,000	11.7	67	72,500
Both oxygen cut	Cored	64,500	18.3	75	69,000

* Based on throat section.

Type of Electrode	Properties of Weld Metal: Tensile Strength, Lb./In.²	Elongation, %	Number of Layers	Current, Amp.
Bare	57,000–64,000	...	2	150–185
Light coated	57,000–64,000	12–15	2	145–200
Heavy covered	57,000–64,000	25–30	2 Butt 3 Fillet	140–165
Cored	68,000–74,000	18–25	2	170–210

Table 52—Properties of Unbeveled Butt Welds on Oxygen-Cut Scarves. Obert[29]

Plate Thickness, Inch	Yield Strength, Lb./In.²	Tensile Strength, Lb./In.²	Reduced Section Specimen: Location of Fracture	Free Bend Elongation, %	Nick Break	X-Ray
5/8	38,200–38,600	64,700–65,900	Partly in weld, partly in heat-affected zone	33–43	Complete fusion	Satisfactory
1	39,700	64,500	Partly in weld, partly in heat-affected zone	63	Complete fusion	Satisfactory
1 1/2	31,100–33,000	54,000	1 1/2 inch from weld	47–48	Complete fusion	Satisfactory

misgivings about the propriety of welding on oxygen-cut scarves are disappearing.

Tests

The comprehensive tests made by Clarke[5,51] (1933, 1935) on butt and fillet welds on oxygen-cut scarves showed there was no difficulty if an electrode were used that would flux the scale. Using flux-coated electrodes, Clarke found the same tensile, bend and notch impact properties in V-butt and lap, side and end fillet welds in steel 0.43–0.50 inch thick (tensile strength = about 67,000 lb./in.²) with scarves cut with oxyacetylene or oxy-coal gas torches as on sawn scarves. The microstructure of welds made with the flux-coated electrodes was free from porosity and inclusions. In view of the absence of details, such as composition of weld metal and steel, angle of V and number of layers, only one of the numerous series of tests need be reproduced, Table 50. The welded specimens were 1 1/2 inches wide, were machined flush and fractured in the tension test outside the weld.

If, instead of the flux-coated electrode, a bare or lightly coated electrode was used, the welds had poor mechanical properties, particularly if the scarves were oxygen cut. A band of oxide inclusions in the junction zone corresponding to the oxygen-cut scarf was observed in the microstructure of these welds. The bare and lightly coated electrodes were unable to flux the scale that remained on the unmachined, oxygen-cut scarves.

Further evidence of the ability of the flux-coated electrodes to produce excellent welds on scaled scarves was secured in tests of welds made on scarves prepared with a sharply oxidizing preheating mixture (no details). Downhand V-butt welds made with ordinary care on the oxygen-cut scarves without disturbing the scale during the deposition of successive beads had the same tensile, bend and Izod properties as welds made on clean, machined scarves. Overhead butt welds made on the scaled scarves had slightly better properties than overhead welds made on machined scarves. After each bead had been deposited the scarves were found clear of scale for about 1/16 inch outside the weld. The flux had dissolved the scale. Butt welds made with flux-coated electrodes on oxygen-cut scarves prepared with the aid of a reducing preheating mixture appeared to have properties equivalent to welds made on machined scarves (no details).

On the other hand, lightly coated electrodes were quite unsuitable for use on the scaled scarves prepared with an oxidizing preheating flame. Whereas welds made with the lightly coated electrodes on machined scarves had a tensile strength of 61,000 lb./in.² with an elongation of 13% in 4 inches, welds on the scaled scarves had tensile strengths of 54,000 and 53,000 lb./in.,² respectively, with scarves made with oxidizing flame and neutral flame, the corresponding elongations being 7 and 10.4% in 4 inches. Again there was a band of slag inclusions along the scarves of the welds, which indicated that the lightly coated electrodes were unable to flux the scale. However, the electrodes burned more quietly and with less spatter on the scaled scarves than on clean scarves.

A comparison was made by Tetzlaff[55] (1937) between downhand butt and fillet welds made on sheared or oxygen-cut steel 0.32 inch thick (tensile strength = 58,000–71,000 lb./in.,² composition not stated). The sheared scarves were prepared on a roller shear at the rate of 39 ft./min. The machine oxyacetylene-cut scarves were made at the rate of 79 ft./hr. (tip distance = 0.08 inch, oxygen pressure = 47 lb./in.,² acetylene pressure = 7 lb./in.²). The butt and fillet weld tension specimens are shown in Fig. 4. The fillet welds were tacked with 0.02 inch gap. The butt welds were machined flush. The bend specimen was 2 inches wide, the diameter of rollers = 4 inches, diameter of plunger = 0.78 inch, distance between center of rollers = 6 inches. All electrodes were 0.16 inch diameter.

The results, Table 51, led Tetzlaff to conclude that there was no difference between welds on oxygen-cut and sheared scarves. The lower strength and higher ductility of the welds made on oxygen-cut scarves were not explained. There was good penetration in all welds. Fractures did not favor the scarves.

Obert[29,123] (1934) also was satisfied with the quality of butt welds, Table 52, made by the metal arc process in unbeveled, machine oxygen-cut mild steel (no details). The relatively low strength of the thickest welds may have been related to the difficulty of making an I-weld in thick plate. Obert conjectured that the slightly higher carbon content in the oxygen-cut surface lowered the melting point and facilitated welding, while the iron oxide on the surface is reduced by the reducing flame in gas welding or by the electrode coating.

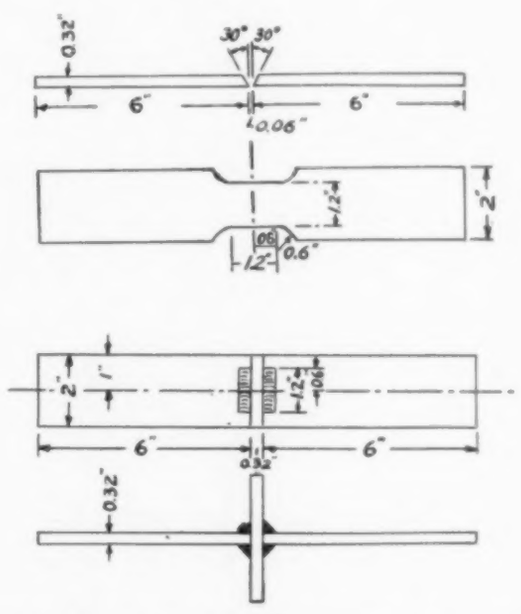


Fig. 4—Fillet Welded and Butt-Welded Tension Specimens Used by Tetzlaff[55]

The scale on flame-cut surfaces was believed by Shepherd and Carpenter[128] to be basic in character and to be readily removed by an acid electrode coating. Their belief was confirmed by the high tensile strength and ductility of butt welds and all-weld metal in plates $^{9}/_{16}$ inch thick having a tensile strength of 85,000 lb./in.^{2} The scarves were machined or oxygen cut, using coal gas or acetylene. Since no details of composition or welding procedure were supplied, it is impossible to estimate to what extent the lower strength (70,000 lb./in.^{2}) and ductility of the welds were due to type and specimen and composition of weld metal. Stress annealing did not affect the results.

Without supplying details, the British Acetylene Association[25] (1936) announced that arc and oxyacetylene welds in mild steel had the same tensile, bend, Izod and repeated impact values whether the scarves were machined or oxygen cut. Fillet welds also had the same bend properties whether the edges were machined or oxygen cut. With correct welding procedure the scale on the oxygen-cut surface is not dispersed as minute FeO particles throughout the weld. A firm[153] in Holland found practically no difference between V and X welds in low-alloy structural steel $^{1}/_{4}$ to $^{5}/_{8}$ inch thick made on sheared or oxygen-cut scarves using acetylene, city gas, or propane, hardness, bend angle, and DVMR notch-impact value (11.5-11.9 mkg./cm.^{2}, notch in scarf) were essentially the same for all.

The rounded edge of oxygen-cut bevels was believed by Brown[5] (1933) to account for undercut which he observed in V-butt welds made in mild steel $^{3}/_{8}$ inch thick on scarves prepared by oxyacetylene and oxy-coal gas cutting. The best bend properties were obtained with welds made on machined scarves, coal-gas cuts being better than oxyacetylene. The tension specimens were machined flush and consequently did not fail in the scarf (no details). It was pointed out that rounded edges are indicative of low-grade oxygen cutting. Bainbridge[49] found no difference in strength among welds made on scarves beveled by mechanical means, oxy-coal gas and oxyacetylene cutting torches (no details).

Bend fatigue and service tests convinced Melhardt[90,118] (1935) that oxyacetylene butt welds made in rails with oxygen-cut ends were satisfactory. Cummins[119] (1938) arrived at the same conclusion on the basis of service experience.

General Observations

A survey of welding codes reveals two outlooks: (1) If oxygen cutting is permissible for the steel it is satisfactory for welding scarves with no more preparation than the removal of loose slag. This outlook was expressed by Kommerell[120] in 1930 and is embodied in the German specifications for welded plate-girder railway bridges[121] for mild steel (tensile strength = 53,000 lb./in.^{2}). The AREA Specifications for Welding[122] in 1933 stated that gas-cut edges "must be thoroughly cleaned to expose clean steel" before welding and practically the same statement appears in the Regulations of the Polish Ministry of Interior[123] (1934, Part 7, Sec. 3) and A. W. S. Code for Fusion Welding and Gas Cutting in Building Construction[124] (1935). The British Standard Specification No. 538 (1934) for Metal Arc Welding as Applied to Steel Structures, Clause 6b, states, "Edges to be welded may be cut to shape with a gas-cutting machine. Hand cutting may be substituted only with the Engineer's approval." The same clause appears in B. S. S. 693 (1936) for Oxyacetylene Welding as Applied to Steel Structures.

The second outlook is: (2) Oxygen-cut scarves must be machined before welding is permissible. According to Hessler,[1] two German Specifications for Stationary Boilers (1929) stated that sheared or oxygen-cut edges of drums must be machined before welding. The German Railway Specifications[1,21] require that, if low-alloy steels or plain carbon steels having a tensile strength of at least 68,000 lb./in.^{2} are used, a strip 0.04 inch deep must be removed from oxygen-cut edges on steel up to 0.39 inch thick, increasing to a depth of 0.39 inch for steels 4 inches thick or more. If the steel is not over 1.18 inches thick, machining may be omitted provided the cut surface is to be used as a welding scarf or is not to be subjected to stress in service, or is to be normalized.

Apart from official statements a number of authorities in the past have been hesitant about permitting welding on oxygen-cut scarves. In 1925 Miller[47] pointed out that cutting scale on the scarves confuses oxyacetylene welders, who mistake the melting of the oxide ahead of the rod for the melting of base metal. Since the oxide has a comparatively low melting point, proper penetration may not be secured. However, Critchett[148] pointed out that a carburizing flame in oxyacetylene welding reduces the oxide scale to metallic iron. *Boiler Maker*[125] (1931) believed it was permissible to use the cutting torch to cut a hole in a wrapper sheet near staybolts, but the hole should be reamed or chipped clean before the sludge pipe was welded to the edge of the hole. In 1932 Leopold[126] ground the oxygen-cut edges of corroded and weakened parts of an old bridge before arc welding the reinforcing parts to the cut edges.

On the basis of experiments of which he supplied no details, Adam[127] (1933) found that welds made on scarves cut by the oxyhydrogen and oxy-coal gas processes were equal in quality to welds made on machined scarves, but he believed that oxyacetylene cut scarves were not quite so satisfactory. Lillicrap's[128] warning that the bend properties of welds made on oxygen-cut scarves were poor and that oxyacetylene-cut scarves yield weaker welds than scarves cut with the aid of coal gas has not stood the test of time. The more recent statement by the Director of Naval Construction of the British Admiralty[129] is that machine oxygen-cut surfaces can be welded with no adverse effects on the resulting welds. "The welding of manual oxygen-cut scarves is not so satisfactory."

An interesting example of the transition from outlook 2 to outlook 1 is supplied by the A. S. M. E. Code for Power Boilers. In older editions of the Code the following statement appeared: "Flame-cut surfaces must be chipped, milled or planed to a depth of one-quarter of plate thickness." In the 1937 edition of the Code, Paragraph 104 on Preparation for Welding, Section (*a*) states that plates may be prepared for welding by machining, shearing or flame cutting if the steel contains not over 0.35 C. "If shaped by flame cutting the edges must be uniform and smooth and must be freed of all loose scale and slag accumulations before welding. The discoloration which may remain on the flame-cut surface is not considered to be detrimental oxidation."

Outlook 1 was expressed by King and Montgomerie[130] in 1932. They found from experience in shipyards that "ship plates, having been cut by the oxyacetylene process so that they fit to the required degree, arc welding can be done without further dressing of the edges." As Granjon[131] expressed it, if the scale on the scarves is allowed to become entrapped in the weld, it is an indication that the welder has not been trained sufficiently.

ACKNOWLEDGMENTS

Contributions to the review have been received from:

J. J. Crowe, Air Reduction Sales Company.
N. Daniel, British Oxygen Company, Ltd., London, England.
G. M. Deming, Air Reduction Sales Company.
Professor R. H. Harcourt, Stanford University.
Dr. O. E. Hovey, Engineering Foundation.
Professor W. J. Krefeld, Columbia University.
F. T. Llewellyn, United States Steel Corporation.
C. W. Obert, Linde Air Products Company.

BIBLIOGRAPHY

1. Hessler, R., *Wärme*, **60**, 137–148 (1937).
2. Dümpelmann, R., *Autog. Metallb.*, **30**, 49–52 (1937).
3. *Master Boiler Makers' Assn. Proc.*, **24**, 100–112 (1937).
4. *Machinery (London)*, **50**, 732–733 (1937).
5. Clarke, E. J., *Trans. Instn. Engrs. Shipbuilders Scotland*, **77**, 13–39 (1933).
6. Hovey, O. E., AMERICAN WELDING SOCIETY JOURNAL, **10** (6), 24–25 (1931); *Engg. News-Record*, **106**, 729–730 (1931).
7. Wiss, E., *Autog. Metallb.*, **22**, 46–53, 58–66, 74–79, 90–96 (1929).
8. Dawson, J. R., and Miller, S. W., *Engg. News-Record*, **102**, 760 (1929); *Heat Treating & Forg.*, **15**, 457–459 (1929); Roberts, D. E., AMERICAN WELDING SOCIETY JOURNAL, **7** (11), 94–103 (1928); *Oxy-Acetylene Tips*, **9**, 133–136 (1929).
9. Miller, L. H., *Canadian Engr.*, **56**, 105–106 (1929).
10. *Railway Engr.*, **49**, 229 (1928).
11. McEwen, J. A., AMERICAN WELDING SOCIETY JOURNAL, **7** (12), 113–114 (1928).
12. *Acetylene Light & Weld. Jnl.*, **9**, 202 (1912).
13. Zimmerman, J. H., *Proc. Intnl. Acet. Assn.*, **36**, 79–90 (1935); *Mech. Engg.*, **57**, 423–427 (1935); AMERICAN WELDING SOCIETY JOURNAL, **15** (2), 21–25 (1936).
14. Zimmerman, J. H., *Proc. Intnl. Acet. Assn.*, **35**, 88–99 (1934).
15. Miller, S. W., *Proc. Intnl. Acet. Assn.*, **29**, 94–109 (1928).
16. *Engg. News-Record*, **113**, 12–14 (1934).
17. Dawson, J. R., AMERICAN WELDING SOCIETY JOURNAL, **11** (6), 5–6 (1932).
18. Krefeld, W. J., *Columbia Univ., N. Y., Dept. Civil Engg., Bull.* No. 3, 133 pp., July 1929; *Engg. News-Record*, **104**, 684–685 (1930).
19. Thum, E. E., *Iron Age*, **115**, 1414 (1925); *Engg. News-Record*, **94**, 939 (1925); *Iron Trade Rev.*, **76**, 1262 (1925); *Oxy-Acetylene Tips*, **3**, 200–205 (1925).
20. Sandberg, C. P., *Industrial Gases*, **5**, 61–67 (1924).
21. Graf, O., *Autog. Metallb.*, **29**, 49–57 (1936).
22. Graf, O., *Autog. Metallb.*, **30**, 321–323 (1937).
23. Roessler, L. v., *Autog. Metallb.*, **23**, 34–41 (1930).
24. Wiss, E., *Autog. Metallb.*, **23**, 150–157, 166–173, 182–191, 205–211, 303–306 (1930).
25. *Twelfth Intnl. Cong. Acetylene, Proc.*, 105–137 (1936).
26. Ehret, P., *Arts et Métiers*, **84**, 74–87, 120–126, 169–179 (1931).
27. Chapman, E. E., and Service, H. H., AMERICAN WELDING SOCIETY JOURNAL, **4** (6), 25–40 (1925).
28. Guillet, L., *Génie Civil*, **51**, 241–244 (1907); Dohmen, A., *Schiffbau*, **8**, 831–836 (1907).
29. Obert, C. W., *Proc. Intnl. Acet. Assn.*, **35**, 101–108 (1934).
30. Séférian, D., and Leroy, A., *Twelfth Intnl. Cong. Acetylene, Proc.*, 747–762 (1936).
31. Hunsicker, *Autog. Metallb.*, **31**, 67–71, 89–97 (1938).
32. Geruso, R. L., and Hannant, T. N., *Steel*, **89**, 39–40, July 16, 1931.
33. de Jessey, L., *Twelfth Intnl. Cong. Acetylene, Proc.*, 489–490 (1936); *Bull. Soc. Ing. Soudeurs*, Paris, **3**, 511–524 (1932).
34. Plieninger, R., Untersuchungen über das Verfahren: Eisen mittels des Sauerstoffstrahles zu durchtrennen, Dr.-Ing. Diss., T. H. Fridericiana, Karlsruhe, 56 pp., J. Frey, Zurich (1912); *Z. komp. u. flüssige Gase*, **16**, 6–11, 21–25, 45–50, 72–76, 87–97, 131–139 (1914).
35. *Chem. Tech. Reichsanstalt Jahresbericht*, **6**, 36–49 (1927).
36. Leroy, A., *Mem. Soc. Ing. Civils France*, **83**, 511–533 (1930).
37. Holler, H., and Schneider, R., *Autog. Metallb.*, **28**, 1–6, 17–23 (1935).
38. Hilpert, A., *Z. VDI.*, **75**, 649–652 (1931).
39. Curtiss, L. M., *Proc. Intnl. Acet. Assn.*, **32**, 146–151 (1931); AMERICAN WELDING SOCIETY JOURNAL, **11** (1), 19–22 (1932); *Weld. Engr.*, **16** (11), 29–32 (1931).
40. Moss, H. H., AMERICAN WELDING SOCIETY JOURNAL, **17** (1), 7–20, (1938).
41. Amédéo, R., *Acet. Light & Weld. Jnl.*, **14**, 34–35, 50–51, 66–67, 86–87, 114–115, 130–132, 146–147, 162–163, 178–179 (1917); **15**, 6–7, 26–27, 47–48, 66–67, 86–87, 102–103, 120, 140–142, 157–158, 199–200 (1918).
42. Amédéo, R., *Acet. Light. & Weld. Jnl.*, **10**, 174–175 (1913).
43. Amédéo, R., *Acet. Light. & Weld. Jnl.*, **11**, 32–33, 104 (1914).
44. Haynes, P. E., *Scientific Amer. Monthly*, **3**, 142–145 (1921).
45. Miller, S. W., AMERICAN WELDING SOCIETY JOURNAL, **4** (12), 8–25 (1925).
46. Balliet, L., *Oil & Gas Jnl.*, **23** (22), 34, 106 (May 1, 1924); *Metal Trades*, **11**, 393–396 (1920); *Pacific Marine Rev.*, Nov. 1920; *Oil Age*, **22** (1), 51–52, 55; (2), 47–49, 66 (1925).
47. Miller, S. W., AMERICAN WELDING SOCIETY JOURNAL, **4** (9), 29–45 (1925).
48. Horn, H. A., Das Trennen der Metalle vermittels Sauerstoff, W. Knapp, Halle, 83 pp. (1925).
49. Bainbridge, C. G., *Weld. Jnl. (London)*, **29**, 365–372 (1932).
50. de Jessey, L., *Eleventh Intnl. Cong. Acetylene Rome, Proc.*, **1**, 454–464 (1934); *Soudeur-Coupeur*, **14** (1), 5–11, Jan. 1935.
51. Bainbridge, C. G., and Clarke, E. J., Iron & Steel Inst. Weld. Symposium, II, 321–331 (1935).
52. Schneider, H., *Twelfth Intnl. Acet. Cong., Proc.*, 1473–1480 (1936); *Tech. Blät. Deutsch. Bergwerks Zig.*, 23, 38, Jan. 15, 1933.
53. Yarkho, V. I., Scientific Research in Welding, No. 2, edited by G. A. Nikolaev, Moscow, 1936, pp. 64–73.
54. Daniel, N., and Durant, C. E., *Weld. Industry*, **5**, 24–34, 78–80 (1937).
55. Tetzlaff, L., *Schiffbau*, **38**, 34–40 (1937).
56. Bass, H., AMERICAN WELDING SOCIETY JOURNAL, **16** (10), 18–21 (1937).
57. Johnston, R. S., *Bur. Stds. Tech. Paper*, **206**, 108 pp. (1922).
58. Krebs, A. and C., Cutting with Oxygen, Wyman-Fogg Co., Boston, 76 pp. (1926).
59. Andrews, F. H., and Jamison, E. A., *Trans. Amer. Foundrymen's Assn.*, **44**, 324–336 (1936).
60. *Engg.*, **117**, 411–412 (1924).
61. Szumowski, A., *Spawanie i ciecie Met.*, **7**, 74–78, 107–109 (1934).
62. Liedloff, K., *Maschinenbau*, 16, Betriebsarchiv Blatt 7/8 (1937).
63. Aston, J., *Wisconsin Engr.*, **14**, 163–170 (1910).
64. Bryce, C. K., *Proc. Engrs. Soc. West. Penn.*, **31**, 723 (1915).
65. *Oxy-Acetylene Tips*, **11**, 167–172 (1928); Moss, H. H., AMERICAN WELDING SOCIETY JOURNAL, **12** (1), 18–24 (1933).
66. Malz, H., Die Grenzen der Schneidgeschwindigkeit beim Brennschneiden, Verlag K. Triltsch, Würzburg, 91 pp. (1933).
67. Moss, H. H., *Mining & Metallurgy*, **17**, 376–379 (1936).
68. Slottman, G., AMERICAN WELDING SOCIETY JOURNAL, **16**, (2), 9–13 (1937).
69. Dawson, J. R., AMERICAN WELDING SOCIETY JOURNAL, **2** (3), 34–43 (1923).
70. Kantner, *Autog. Metallb.*, **21**, 18–25 (1928); *Das Eisenbahnwerk* (22), 1927.
71. *Iron Age*, **112**, 1303–1305 (1923).
72. Kinsey, A. S., *Trans. Amer. Foundrymen's Assn.*, **29**, 596–606 (1920).
73. Helmkamp, R. F., AMERICAN WELDING SOCIETY JOURNAL, **13** (2), 4–9 (1934).
74. Wikoff, A. G., AMERICAN WELDING SOCIETY JOURNAL, **9** (4), 102 (1930).
75. AMERICAN WELDING SOCIETY JOURNAL, **14** (5), Suppl., 1–8 (1935).
76. Maeurer, F. J., *Weld. Engr.*, **7** (5), 17–23 (1922).
77. Titscher, J., *Autogen Schweisser*, **6**, 1–4, 18–21 (1933).
78. Intnl. Acet. Assn., Report Oxy-Acet. Committee., 51–55 (1930); 15 (1932).
79. Ullmer, H., AMERICAN WELDING SOCIETY JOURNAL, **8** (11), 14–16 (1929).
80. Melhardt, H., *Autog. Metallb.*, **29**, 17–27 (1936).
81. Proc. Rwy. Engrs. Oxy-Acetylene Welding & Cutting Conf., London, 42–48 (1936).
82. *Steel*, **103** (7), 52–53, Aug. 15, 1938.
83. Ketchbaw, T. D., *Welding*, **2**, 297–299, 302 (1931).
84. Firth, L. G., and Knox, L. D., *Proc. Intnl. Acet. Assn.*, **35**, 52–57 (1934).
85. Gibson, A. E., *Proc. Intnl. Acet. Assn.*, **38**, 104–107 (1937).
86. Thomas, R. D., Evleth, E. M., Donald, R. S., and Woofter, H. A., AMERICAN WELDING SOCIETY JOURNAL, **12** (3), 20 (1933).
87. Boggs, R. W., *Iron Age*, **126**, 416–417, 446 (1930).
88. Krebs, A., *Autog. Metallb.*, **18**, 142–144 (1925).
89. *Pratique Ind. Mécaniques*, **11**, 440 (1929).
90. *Industrial Gases*, **15**, 117 (1934).
91. Peck, R. W., *Metallurgist*, **4**, 78 (1929).
92. Rivière, M., *Soudeur-Coupeur*, **14** (8), 1–5, Aug. 1935.
93. Thum, E. E., *Heat Treat. & Forg.*, **12**, 57–59 (1926).
94. *Engg.*, **112**, 274–275, 307–311 (1921).
95. Ussher, A., *Weld. Engr.*, **20** (11), 20–25 (1935).
96. Napolitan, F. J., *Foundry*, **48**, 567–568 (1920).
97. Bainbridge, C. G., *Metallurgia*, **7**, 101–102 (1933); *Indust. Gases*, **13**, 44–45 (1932).
98. Kalashnikova, Z. V., and Shchapov, H. P., *Metrostroi* (Moscow), No. 4, 44–47 (1937).
99. Matting, A., *Autog. Metallb.*, **24**, 277–280 (1931).
100. Dümpelmann, R., *Autog. Metallb.*, **30**, 219–223 (1937).
101. Jennings, C. H., How to Weld 29 Metals, Westinghouse E. & M. Co. (1937).
102. Trissal, J. M., *Ind. & Weld.*, **10** (2), 64–67 (1937); the report was made by R. P. Winton, *AREA Bull.*, **39** (402), 622–625 (1928).
103. Davis, Z. T., AMERICAN WELDING SOCIETY JOURNAL, **1** (10), 34–37 (1922); **2** (3), 31–33 (1923).
104. Orr, P., *Machinery (N. Y.)*, **38**, 489–491 (1932).
105. Dümpelmann, R., *Autog. Metallb.*, **30**, 289–296 (1937).
106. Kinzel, A. B., and Crafts, W., Alloys of Iron and Chromium, McGraw-Hill, N. Y. (1937).
107. Yatsevitch, M., *Welding*, **1**, 534 (1930).
108. Priestley, W. J., AMERICAN WELDING SOCIETY JOURNAL, **15** (1), 14–17 (1936).
109. *Weld. Jnl. (London)*, **24**, 220 (1927).
110. Dwyer, P., Gates and Risers for Castings, Penton Publ. Co., Cleveland, 285 (1935).
111. *Nickel Steel Topics*, **6** (4), 10, Aug. 1937.
112. *Rev. Soud. Autog.*, **20** (175), 1613 (1928).
113. Lacroix, A., *Compt. Rend. Acad. Franc.*, **185**, 313–317 (1927).
114. Dobrowolski, Z., *Spawanie i ciecie Met.*, **11**, 18–22, Jan. 1938.
115. *Intnl. Rwy. Cong. Assn. Bull., New Series*, **13**, 2122 (1930).
116. Janiszewski, K., *Iron Age*, **136** (24), 25–27, Dec. 12, 1935.
117. Miller, H. L., and Lichtenwalter, T. R., *Steel*, **101** (16), 62–63, Oct. 18, 1937.
118. Melhardt, H., *Autogen Schweisser*, **6**, 41–45, 59–64, 69–71, 87–89, 108–111 (1933); *Z. ÖIAV*, **85**, 163–168 (1933).
119. Cummins, R. R., AMERICAN WELDING SOCIETY JOURNAL, **17** (1), 46–49, Jan. 1938.
120. Kommerell, *Bautechnik*, **8**, 481–485 (1930).
121. Kommerell, O., Erläuterungen zu den Vorschriften für geschweissten Stahlbauten, II Vollwandige Eisenbahnbrücken, 68 (1936).
122. *AREA Proc.*, **34**, 295 (1933).
123. AMERICAN WELDING SOCIETY JOURNAL, **13** (7), 12–15 (1934).
124. AMERICAN WELDING SOCIETY JOURNAL, **14** (3), Suppl., 1–16 (1935).
125. *Boiler Maker*, **31**, 56 (1931).
126. Leopold, *Bautechnik*, **10**, 628–631 (1932).
127. Adam, J. L., *Trans. Instn. Engrs. Shipbuilders Scotland*, **76** 483–484 (1932–33).
128. Lillicrap, C. S., *Trans. Instn. Naval Arch.*, **75**, 45–58, 77–78 (1933).
129. Iron & Steel Inst. Welding Symposium, II, 67–77 (1935).
130. King, J. F., and Montgomerie, J., *Trans. Instn. Naval Arch.*, **74**, 1–6 (1932).
131. Granjon, R., *Rev. Soud. Autog.*, **28** (267), 12, May 1936.
132. Rose, J. B., *Welding*, **1**, 832–834 (1930); AMERICAN WELDING SOCIETY JOURNAL, **8** (9), 6–26 (1929).
133. *Engineering*, **146**, 521 (1938).
134. Wilkinson, T. B., and O'Neill, H., *Instn. Mech. Engrs. Journal*, 10–11, Jan. 1939, Advance Copy of Paper: Observations on the Arc Welding and Gas Cutting of High-Tensile Low-Alloy Structural Steels, 18 pp., March 1939.
135. Nichols, W. R., and Kiernan, The Effect of Gas Cutting on Steel; Thesis, Massachusetts Institute of Technology, 1922.
136. Theisinger, W. G., Heat Effect in Welding, DSc. Thesis, Graduate School of Engineering, Harvard Univ., 1935.
137. *Industrial Gases*, **10**, 92–95 (1929). **19**, 63 (1938).
138. Shepherd, T. C. R., and Carpenter, S. W., Iron & Steel Inst. Welding Symposium, II, 161–179 (1935).

139. Daley, C. A., *Proc. Intnl. Acetylene Assn.*, **36**, 229 (1935).
140. Mitchell, A. F., *Proc. Engrs. Soc. West. Penn.*, **31**, 711–718 (1915).
141. Rwy. Engg. & Maintenance, 246–249, 261, April 1938, 29, 239 (1933).
142. Budach, R., *Verkehrstechnik*, **53**, 434–438 (1936).
143. Swinnerton, N. W., *Welding Industry*, **5**, 180–188 (1937).
144. Bonhomme, W., *Rev. Univ. Mines*, **8**, T.14, 444–456 (1938).
145. *Engg. News-Rec.*, **101**, 666–667 (1928).
146. Crowe, J. J., and Deming, G. M., *Proc. Intnl. Acetylene Assn.*, **29**, 219–239 (1928).
147. Wolfe, W. S., *Engg. News-Record*, **105**, 101 (1930).
148. Critchett, J. H., American Welding Society Journal, **14**, (5), 2–6 (1935).
149. Oxy-Acetylene Committee Publications, Section VIII, The Effect of Flame Cutting on Steel, International Acetylene Association, N. Y., 1938–1939, 47 pp.
150. *Oxy-Acetylene Tips*, **15**, 273–278 (1936).
151. *Industrial Gases*, **19**, 184 (1938); *Oxy-Acetylene Tips*, **17**, 229–230 (1938).
152. Baumann, R., *Mitt. Forschungsarbeiten Geb. Ingwes. VDI,H.* **83–84**; 52–53 (1910).
153. *Smit Laschtijdschrift*, **3** (1) 27-34 (1939).

SUGGESTED RESEARCH PROBLEMS

1. Short time tensile tests at temperatures above and below room temperature (−50° C. to + 500° C.) should be made on oxygen-cut steels. Notch impact tests of oxygen-cut steels should be made at sub-zero temperatures. The tests should be made on machined specimens as well as on oxygen-cut specimens. The effect of stress annealing and heat treatment before and after cutting might be studied. Any program should include flame softening, if medium-carbon and low-alloy structural steels are investigated in addition to mild steels. It should be remembered that the difficulty in retaining high notch-impact value may increase as the temperature of the steel being cut is lowered.

2. There are no standard tests for the properties of oxygen-cut surfaces. The present review indicates the importance of bend ductility and hardness. The extent to which the mechanical properties of oxygen-cut surfaces in borderline steels (medium carbon or alloy) vary with change in width of strip cut, cutting speed, thickness and testing conditions has not been studied. For example, if the cutting speed is increased 50% in cutting the borderline steel 2 inches thick, the cooling rate will be increased but the thickness of heat-affected zone will decrease. The net effect on mechanical properties should be determined. The effect may be different under different conditions of testing, but freak tests in which the oxygen-cut surface is exposed to deformation of a type that it would never conceivably be called upon to withstand in practical applications need not be considered in developing a standard test.

3. Fatigue tension tests should be made on specimens with different types of notches, for example, semicircular notches, machined or cut with the oxygen torch. It may be that the sorbitic or fine pearlitic structure at the edge of the oxygen-cut notches will provide superior notch fatigue strength. If steels are tested which react adversely to oxygen cutting, heat treatment, such as flame softening, should be applied. Preferably all oxygen cutting should be done with mechanically controlled torches, particular attention being given to the variables discussed in Welding Handbook, 1938 Edition, Chapter 16.

4. It is known that oxygen-cut edges generally are under residual tensile stress due to shrinkage. The effect of increasing thickness on the shrinkage stresses has not been studied. Another problem is the determination of the minimum preheating temperature for oxygen cutting alloy steels. Should the preheating provide plasticity or merely be above the temperature at which martensite can form? It should be realized that the practical solution to preheating has been found, namely: furnace heating or flame softening. The theoretical and quantitative aspects of preheating remain to be investigated.

5. What is the effect of cutting speed on temperature distribution near the cut during cutting (maximum depth of penetration of the isotherm for 700° C.), on bend ductility, and on carbon migration in steels of different carbon contents, for example, 0.10 to 0.90 C.? There is a strong desire for more exact information on temperature gradients in oxygen cutting. The investigation would lead to practical methods of control over depth or degree of hardness of the cut surface.

6. Decrease in cutting oxygen purity is known to decrease the speed and quality of oxygen cutting to a pronounced extent. It would be a step forward in interpreting the influence of oxygen purity if the rate of reaction of steel with oxygen of different purities was determined.

7. Do carbon content and alloying elements affect drag, cutting speed, or gas consumption under given cutting conditions? As an inert element, such as nickel or copper, is added to steel, the proportion of iron decreases and the drag at a given cutting speed should increase, because the ignition temperature may be raised and the melting point is lowered. It may be that the speed of reaction is more important than the ignition temperature.

8. The composition of the cutting slag is known to be related to drag and other characteristics of oxygen cutting, yet the methods of analysis used by investigators of cutting slag have not been the best. Cutting slags from machine oxygen cuts made at different drags in different thicknesses of mild steel should be analyzed by methods that will distinguish between metallic iron and oxides and among iron oxides of different formula. In collecting the slag every effort should be made to prevent oxidation of the slag after it leaves the cut.

9. The migration of carbon and alloying elements during the flame cutting of alloy steels has not been studied comprehensively. It is not known whether the elements Al, Cu, Cr, Mn, Si, P, Mo, W, V enter the slag or become concentrated in a surface layer during oxygen cutting. In oxygen cutting high-nickel steel, is there a low-nickel intermediate layer between the unaffected steel and the nickel-rich surface layer analogous to the low-carbon intermediate layer that is believed to be present in oxygen-cut medium-carbon steel? What is the structure and composition of the thin "white layer" that is observed at the edge of unpreheated oxygen cuts in some medium-carbon alloy steels? Does flame softening or a reduction in cutting speed remove the layer? In oxygen cutting plain copper steels does a copper-rich layer appear at the edge and cause hot cracks (intergranular penetration) during subsequent welding? The quenching of low-carbon steel from 1450° C. has been found to cause embrittlement, which was associated with partial fusion at grain boundaries (*Revue Métallurgie*, 1923, pp. 777–795).

10. The cutting torch as an inspection tool for determining the homogeneity of steel. Irregularities or discontinuities in shape of the drag lines are indicative of segregation or internal inhomogeneity in general. Experiments should be made to determine the sensitivity of drag lines to segregation; that is, the type and degree of segregation necessary to create irregularities in drag. Experiments also should be made to determine the torch adjustment and speed that are most sensitive to a given segregation. Perhaps two plates clamped tightly together with an intermediate sandwich of crushed slag might serve as a norm for segregated steel. The deseaming torch has possibilities as an inspection tool for homogeneity, since slag streaks and cracks become characteristically white in the path of a deseaming torch.

THE ENGINEERING FOUNDATION

Welding Research Committee

Sponsored by the American Welding Society « « » » and American Institute of Electrical Engineers

Supplement to the Journal of the American Welding Society, August 1939

FINAL REPORT

Structural Steel Welding Committee

Origin

The Structural Steel Welding Committee was organized as a committee of the American Bureau of Welding in 1926 for the purpose of "Obtaining reliable information upon which to base safe unit working stresses in the designing of welded structures."

A more complete statement of the origin and purposes is included in the Foreword of the first report of the Structural Steel Welding Committee, dated September 1931, and in the Introduction of that report.

Personnel

The personnel of the Committee is included in this report as Exhibit A. It is only fitting that in the final report tribute should be paid to those great men who have passed beyond, who were instrumental in organizing the committee and determining its broad scope. In this connection, special tribute is due to the first Chairman, Mr. James H. Edwards.

Acknowledgments

The Committee gratefully acknowledges:

1. Cash contributions of $22,500 generously contributed by the American Institute of Steel Construction, individual members of National Electric Manufacturers Association, and individual members of the Compressed Gas Manufacturers Association, Gas Association, Gas Products Association and the International Acetylene Association.
2. Specimens prepared by 39 shops.
3. Supervision by individuals and companies.
4. Contributions of testing laboratories.
5. Pilot tests.
6. Investigations carried out by Fellows at Lehigh and Cornell Universities.
7. Special reviews of the literature made by the American Institute of Steel Construction.
8. Services of the fiscal agent, National Research Council, rendered without charge for a period of 13 years and special service rendered by the AMERICAN WELDING SOCIETY.

For a detailed statement of acknowledgments in connection with work done up to September 1931, reference is made to pages 3 to 7 inclusive of the first report of the Committee published in 1931.

Financial Statement

In an investigation of this nature, a bare statement of funds received and expended does not give a complete picture of the total cost. Contributed services and materials amounted to many times the cash contributions.

Expenditures were made in accordance with a budget prepared by the Executive Committee and approved by the whole Committee. The National Research Council acted as fiscal agent for the funds of the Committee, and payments were made by the Council on requisitions and vouchers bearing the endorsement of the Chairman or Vice-Chairman of the Committee, and the Director of the Division of Engineering and Industrial Research of the National Research Council.

A financial statement is included in this report as Exhibit B.

Published Reports

A list of the published reports of the Committee is included in this report as Exhibit C.

Significance of the Work

1. Probably the greatest single accomplishment of the Structural Steel Welding Committee was the establishment of the first procedure control in structural welding including qualification requirements for welding operators. This quickly led to the establishment of the A. W. S. Code for Fusion Welding and Gas Cutting in Building Construction and the general acceptance of that Code by Municipalities in the United States.
2. The first program and pilot tests resulted in the direct contact by 42 fabricating shops with structural welding and the qualification of more than 60 welders.
3. Safe design values under static loading for various types of fusion welded joints as commercially welded in structural steel fabricating shops and a statement as to the degree of uniformity of the strength of welded joints that may be expected from shops throughout the country. The uniformity in the strength of welded joints from thirty-nine shops inspired confidence in the safety of welded structures that might be fabricated by any reputable structural concern.
4. Welded building frames have been constructed using the methods and joints proposed as a result of the

Lehigh University investigations and these methods and joints are included in the A. W. S. Handbook.

5. The thorough training of four young men who are now connected with the welding industry in responsible positions and the lesser training of a great many more men in the inspection and testing of a large number of welded specimens.

Work at Lehigh and Cornell Universities

Inasmuch as a considerable portion of the recent expenses of the Committee has been for work at Lehigh and Cornell Universities, special brief reports from these universities are included in this report as Exhibits D and E, respectively.

Future Work

Undoubtedly a great deal of work still remains to be done. A brief outline of some work which suggests itself as a result of investigations now completed or under way, is included in this report as Exhibit F. It is contemplated that the present Structural Steel Welding Committee will be discharged and that a new committee under the auspices of the Welding Research Committee of Engineering Foundation, will be formed to take over any unfinished work of the old Committee and to formulate a new program. There is some reason to believe that this work will be continued as soon as new financing is completed.

Respectfully submitted,
Structural Steel Welding Committee

EXHIBIT A

Personnel of Committee

Chairman: L. S. Moisseiff, Consulting Engineer
Vice-Chairman: H. M. Priest, Railroad Research Bureau, U. S. Steel Corp.
Secretary: W. Spraragen, Secretary, Welding Research Committee
L. Allen, Bethlehem Steel Company
G. A. Caldwell, Mississippi Valley Structural Steel Company
A. M. Candy, Hollup Corporation
J. J. Crowe, Air Reduction Sales Company
C. S. Davis, Consulting Engineer
E. A. Doyle, The Linde Air Products Company
E. H. Ewertz, Consulting Engineer
F. M. Farmer, Electrical Testing Laboratories
F. H. Frankland, American Institute of Steel Construction
O. E. Hovey, Consulting Engineer
P. G. Lang, Jr., The Baltimore & Ohio R.R. Company
J. C. Lincoln, The Lincoln Electric Company
*F. T. Llewellyn, United States Steel Corp. of Delaware
H. H. Moss, The Linde Air Products Company
†G. A. Orrok, Consulting Engineer
J. W. Owens, Fairbanks-Morse & Company
C. Schenck, Bethlehem Steel Company
A. Vogel, General Electric Company
W. H. Weiskopf, Consulting Engineer
*Aubrey Weymouth, Post & McCord, Inc.
H. L. Whittemore, National Bureau of Standards
H. A. Woofter, Consulting Engineer

The following were also included in the Membership of the Committee as Originally Constituted:

‡J. H. Edwards, Chairman; American Bridge Company
‡D. H. Deyoe, General Electric Company
‡P. Gillespie, University of Toronto
Milton Male, Technical Assistant; U. S. Steel Corp. of Delaware
‡F. P. McKibben, Consulting Engineer
‡L. H. Miller, American Institute of Steel Construction
‡C. P. Turner, Bethlehem Steel Company
‡S. C. Weiskopf, Consulting Engineer

* Representative of American Society of Civil Engineers.
† Representative of American Society of Mechanical Engineers.
‡ Deceased.

EXHIBIT B

Structural Steel Investigations

Statement of Receipts and Disbursements
to March 10, 1939

Receipts

American Institute of Steel Construction	$10,000.00	
Manufacturing Companies identified with the Welding Industry	12,526.68	
		$22,526.68

Disbursements

Preparation and Testing of Specimens in First Program	$ 5,988.54	
Technical Assistant	3,293.67	
Stenographic Services, Office Supplies, Travel and Miscellaneous	1,142.77	
Printing Reports, Specifications, Forms	5,684.34	
Studying Literature, Translating, Magazines	389.71	
Lehigh Investigations	4,050.00	
Cornell Investigations	1,600.00	
	$22,149.03	
Balance on Hand	377.65	
		$22,526.68

EXHIBIT C

List of Published Reports of Structural Steel Welding Committee

Report on Pilot Tests Conducted for the Structural Steel Welding Committee of the American Bureau of Welding by the University of Toronto, 1927–1928—March 1929 Journal, pp. 9–27, by Peter Gillespie, C. A. Hughes, R. B. Jackson and J. F. Fox.

Survey of Existing Test Data by Structural Steel Welding Committee of American Bureau of Welding, Nov. 1927 Journal, pp. 8–14.

Bridge Welding—A Review of the Literature, by F. H. Frankland, October 1935 Supplement, pp. 1–16.

An Investigation of Welded Seat Angle Connections, by Inge Lyse and N. G. Schreiner, Feb. 1935 Supplement, pp. 1–15.

A Photoelastic Study of Bending in Welded Seat Angle Connections, by Inge Lyse and Douglas Stewart, February 1935 Supplement, pp. 16–20.

The Behavior of Fillet Welds When Subjected to

Bending Stresses, by Norman G. Schreiner, September 1935 Supplement, pp. 1–16.

Welded Beam-Column Connections, by Inge Lyse and Glenn J. Gibson, October 1936 Supplement, pp. 34–40.

An Investigation of Plug and Slot Welds, by Glenn J. Gibson, July 1937 Supplement, pp. 10–21.

Effect of Welded Top Angles on Beam-Column Connections, by Inge Lyse and Glenn J. Gibson, October 1937 Supplement, pp. 2–9.

Effect of Rigid Beam-Column Connections on Column Stresses, by Inge Lyse and Edward H. Mount, October 1938 Supplement, pp. 25–31.

The Committee has also financed a wide distribution of the following two papers:

Practical Design of Welded Steel Structures, by H. M. Priest, Aug. 1933 Journal.

Erecting Steel Buildings and Strengthening Steel Bridges by Welding, by F. P. McKibben, May and June 1933 Journals.

EXHIBIT D

Fritz Engineering Laboratory
Department of Civil Engineering
Lehigh University
Bethlehem, Pa.

March 6, 1939

Mr. William Spraragen, Secretary,
Structural Steel Welding Committee,
29 West 39th St., New York City.

Dear Mr. Spraragen:

In answer to your letter of March 2nd I am giving you the following summary of the work carried out at Lehigh University under the auspices of the Structural Steel Welding Committee.

The Structural Steel Welding Committee of the AMERICAN WELDING SOCIETY sponsored three Research Fellowships at the Fritz Engineering Laboratory of Lehigh University during the years 1933 to 1939. Each fellowship permitted the student to do half time work in welding research for two years while obtaining at the same time an M.S. degree. The holders of these fellowships have been Norman G. Schreiner, Glenn J. Gibson and Edward H. Mount. Their publications, either individual or under joint authorship with Inge Lyse, have represented a continuous series of correlated projects of proved practical value to welding engineers and to the industry. The service of these men in the welding industry has not stopped with the completion of their fellowships. Because of their specialized training both Mr. Schreiner and Mr. Gibson have found positions in the welding field and Mr. Mount has planned to do likewise.

The publications which have resulted directly from these Welding Fellowships are as follows:

1. Inge Lyse and Norman G. Schreiner, "An Investigation of Welded Seat Angle Connections," A. W. S. J., Supplement, pp. 1–15, vol. 14, no. 2, February 1935.
2. Inge Lyse and Douglas Stewart, "A Photoelastic Study of Bending in Welded Seat Angle Connections," A. W. S. J., Supplement, pp. 16–20, vol. 14, no. 2, February 1935.
3. Norman G. Schreiner, "The Behavior of Fillet Welds When Subjected to Bending Stresses," A. W. S. J., Supplement, pp. 1–16, vol. 14, no. 9, September 1935.
4. Inge Lyse and Glenn J. Gibson, "Welded Beam-Column Connections," A. W. S. J., Supplement, pp. 34–40, vol. 15, no. 10, October 1936.
5. Glenn J. Gibson, "An Investigation of Plug and Slot Welds," A. W. S. J., Supplement, pp. 10–21, vol. 16, no. 7, July 1937.
6. Inge Lyse and Glenn J. Gibson, "Effect of Welded Top Angles on Beam-Column Connections," A. W. S. J., Supplement, pp. 2–9, vol. 16, no. 10, October 1937.
7. Inge Lyse and Edward H. Mount, "Effect of Rigid Beam-Column Connections on Column Stresses," A. W. S. J., Supplement, pp. 25–31, vol. 17, no. 10, October 1938.

The preceding list of publication omits those by Professors Lyse, Jensen and others which were not directly connected with the welding research fellowships.

Current investigations now in progress in cooperation with the revised Structural Steel Welding Committee are expected to result in the following publications:

Bruce Johnston and Edward H. Mount, "Slope Deflection and Moment Distribution Methods Applied to the Analysis of Welded Building Frames with Semi-Rigid Connections," to be submitted to the Proceedings of the A.S.C.E. in the summer of 1939.

Howard Godfrey and Edward H. Mount, "Pilot Tests for a Covered Wire Electrode Program," to be submitted to the AMERICAN WELDING SOCIETY in the summer of 1939.

Bruce Johnston and Edward H. Mount, "The Design of Building Frames with Welded Semi-Rigid Connections," to be submitted to the AMERICAN WELDING SOCIETY in the summer of 1939.

The tests of the welded building frame now in progress are their first of their kind on a welded structure and should be a step toward more economical design of beams in buildings.

The significance of the completed tests to industry has been considerable. Welded building frames have already been constructed using the design method and joint proposed by Lyse and Gibson. The methods are presented in the latest Welding Handbook. The investigation of plug and slot welds has also found application in design. The value of all of these investigations will be furthered by the work of correlation now in progress.

Donations of material and financial assistance have been given by the Bethlehem Steel Company and the American Institute of Steel Construction, respectively. Close cooperation with the American Institute of Steel Construction Research Fellowship programs has been to the advantage of welding studies. The Battledeck Floor investigation, for example, has extended the application of welding.

Sincerely yours,
(Signed) BRUCE JOHNSTON
Assistant Director

EXHIBIT E

College of Engineering
Cornell University
Ithaca, New York

S. C. Hollister,
Dean of the College

April 4, 1939.

Mr. W. Spraragen,
Secretary, American Bureau of Welding,
29 West 39th Street,
New York City.

Dear Mr. Spraragen:

In accordance with your report of April 3rd, I am pleased to submit a progress report on the program of the

Investigation of Fatigue of Large Welded Connections being conducted at Cornell University.

The investigation was conducted in accordance with the outline marked Exhibit B, attached to the minutes of the Executive Structural Steel Welding Committee for December 1, 1936. As stated in that exhibit, the material for the fatigue machines was obtained from the Carnegie-Illinois Steel Corporation and the specimens were provided by the Babcock & Wilcox Company. Two scholarships paying $800 each year were provided by the American Bureau of Welding.

Work began on the project in February 1937. Parts 1, 2 and 3 of the outline, namely, photoelastic studies, the strain gage stress distribution survey and the investigation of residual stresses in the as-welded specimens were undertaken first. The scholars holding the American Bureau of Welding Research scholarships were engaged for the full term of their scholarships on these three projects. The photoelastic studies and the strain gage stress distribution surveys were completed by the scholars. Attempts to obtain quantitative surveys of the residual stresses, however, did not meet with success.

In addition to the strain gage measurements, the specimens so tested were polished in the region of maximum stresses, and loaded until Lueder's lines were developed. By this means it was possible to compute the stress raisers at the point of maximum stress. The stress raises thus determined compared reasonably well with those obtained by the photoelastic measurements.

While the first part of the investigation was proceeding, other graduate students were completing the construction of the fatigue machines. Preliminary trial of the machine developed fatigue difficulties in the bearing surfaces of the fulcrum and load points of the machine. Various bearing designs have been developed and tried out, but without complete satisfaction. We are now installing very heavy needle roller bearings at these three points of the machine, and operation will begin about May 1st.

Indications of the need of a careful study of fatigue of bearings and pin connections has developed through the experience had with fatigue machines. This will become the topic for a separate investigation following our investigation with the welded joints.

The University has furnished the necessary finances to complete this investigation, and it will be prosecuted with due diligence.

Respectfully submitted,
(Signed) S. C. HOLLISTER

EXHIBIT F

Proposed
Welding Research Committee

Structural Steel Research

As a result of previous thorough and unbiased investigations it is a fact that welding received a standing and a respect in the structural building field which it could not have otherwise attained, as well as a highly desirable start toward standardization of design.

Welding possesses certain advantages, not all of which are exhausted, and some of which are undiscovered. In other fields than structural, the advent of fusion welding has led to new uses for steel; in the structural field it promises to introduce economies that will increase the competitive power of steel.

The kind of research now contemplated differs from that which is being carried on by the steel mills in that the latter is aimed primarily at problems of weldability, whereas the former proposes to carry on further investigation into the quantitative results of welding the materials in question by the improved methods, as well as the manner in which these results can be applied to the design of structures.

New developments are constantly being made. Since the first series of tests were conducted, shielded arc electrodes have appeared, which give vastly improved properties in the welded joints. The exploitation of alloy steels has rapidly progressed. It is now proposed to continue the earlier investigations, and to secure equally authoritative design data on these newer techniques, steels and filler materials. It is expected that problems of detail will continually be uncovered, and that these investigations will be continuous, extending over a period of years. A few of the pressing problems needing solution follow:

(*a*) Determine unit design stresses for fillet and butt joints using the latest materials and techniques;

(*b*) Use of welding to impart rigidity to wind and earthquake bracing, hitherto unattainable in the desired degree in steel framing;

(*c*) Should the permissible stresses in fillet welds be constant, or should they decrease with increase in the weld size as required in Poland and Italy?

(*d*) In view of the known uneven distribution of stress in side fillet welds, should their estimated working strength be governed by their length as in the Indian, Italian and German (S) specifications?

(*e*) Can "side" and "end" fillets be considered to develop their full individual load-bearing capacities when acting together, or should some reduction be made to their added individual working strengths as in the Italian regulations and the Australian (D.R.) Code?

(*f*) Establish usable data in regard to slot and plug welds.

(*g*) General behavior of stiffeners in columns and girders.

The precise manner in which the problems will be investigated will depend upon the availability of funds. The nature of the problems will cause their investigation to be concentrated to a greater extent than was the original program.

Welding Research in Belgium

On March 3rd the Belgian Association for the Study, Testing and Application of Materials created a new committee on welding research. Forty specialists representing universities and industries promised their active collaboration with the work of the new Committee.

One of the principal objects of the Committee is to unite all welding research in Belgium. It will organize conferences and will encourage exchange of ideas. A list will be prepared of all welding research completed and in course in Belgium, and of foreign research on these topics. Later, subcommittees will investigate assigned welding problems.

The Chairman of the Committee is Prof. H. Rabozée, President of the Association.

TEST OF MODEL OF THE TACOMA Narrows Anchorage Bar

By BRUCE JOHNSTON* and H. J. GODFREY†

INTRODUCTION

THE design of anchorage bars for the Tacoma Narrows Suspension Bridge involved the consideration of a bar with a slotted hole in one end reinforced by welded pin plates as a substitute for the conventional forged eyebar. In order to check on this design a model bar was fabricated by the Bethlehem Steel Company with all dimensions $^5/_8$ of those of the prototype with the exception of the over-all length which was governed by the clearance in the testing machine. The following report presents the test procedure, general test results, and conclusions.

The investigation was made for the Bethlehem Steel Company by the Fritz Engineering Laboratory of Lehigh University. The Tacoma-Narrows Bridge is being constructed by the Washington Toll Bridge Authority, Lacey V. Murrow, Chief Engineer, Clark H. Eldridge, Bridge Engineer. The anchorage bar was designed under the supervision of Mr. Jonathan Jones, Chief Engineer, Fabricated Steel Construction, Bethlehem Steel Company. Mr. Edward H. Mount, Research Fellow at the Fritz Laboratory assisted in checking the theoretical analysis.

TEST SPECIMEN AND TEST PROCEDURE

The dimensions of the model anchorage bar are given in Fig. 1. The material used was structural steel, A. S. T. M. A7, tensile properties of which are presented in Table 1. The tensile specimens were made in both the transverse and longitudinal direction to rolling.

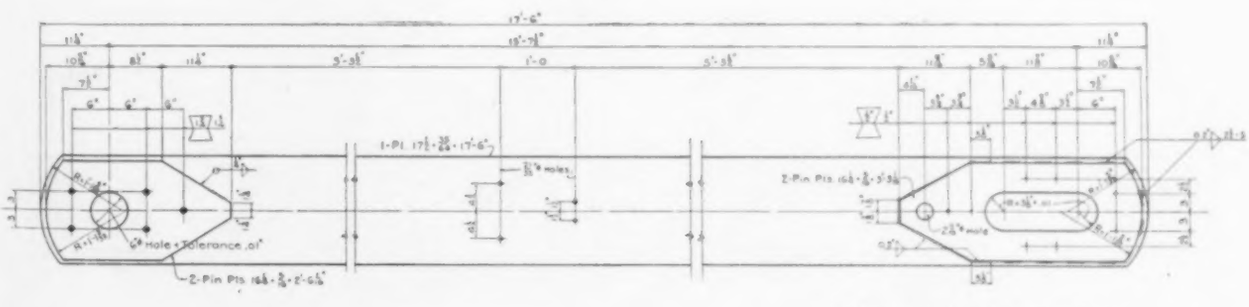
The prototype of this model bar was intended to be equivalent to a solid bar 12 by $1^7/_8$ inch or 22.5 sq. in. As it was desired to connect it to supporting frames by two 1-inch diameter bolts, the bar was designed 28 by $^7/_8$ inch which, with two holes at $1^1/_8$ inch deducted, gave 22.5 sq. in. net as desired.

The design required a 10-inch diameter pin, and the net area through the pin was built up by the addition of two pin plates, 26 by $^1/_2$ inch. The total net area through the slotted head was thus 18 by $^7/_8$ plus 16 by 1, or 31.75 sq. in., being 140 per cent of the body area, as would be required for riveted heads by the A. R. E. A. bridge specifications.

The distance back of pin was set at 13 inches for the $^7/_8$-inch plate and $11^1/_2$ inch for the two $^1/_2$-inch plates, making 22.9 sq. in. or slightly over 100 per cent of the body area as required for riveted heads by the specifications just mentioned. Both of these specification requirements were proved by the test to be quite conservative for the welded head design.

The pin plates' share of the load was designed to be transferred back to the full main plate, above the pin slot, by symmetrical fillet welds stressed not over 9600 lb. per sq. in., the value assigned by the AMERICAN WELDING SOCIETY'S Specifications for Welded Bridges for members in which the minimum stress is not less than 50 per cent of the maximum. This also was proved by the test to be a conservative figure.

*Assistant Director, Fritz Engineering Laboratory, Lehigh University, Bethlehem, Pennsylvania.
†Engineer of Tests, Fritz Engineering Laboratory, Lehigh University, Bethlehem, Pennsylvania.



Notes
Material Structural Steel for Bridges-A.S.T.M.-A7
All dimensions shown for the slotted end of the bar, including the width and thickness of plates, are ⅝ times the corresponding dimensions of the prototype

FIG. 1
TEST MODEL
OF 50 FT. ANCHORAGE BARS
FOR TACOMA NARROWS BRIDGE

In order to hold the pin plates and main plate together adjacent to the slotted hole (which was milled throughout, as is intended for the prototype bars) and adjacent to an auxiliary circular hole which has to do with cable strand adjustment, there were provided eight plug welds through each of the pin plates. These were welded to the main plate, filled with weld metal and ground flush.

The pin connection at the opposite end of the test bar was purposely made entirely too strong to be a possible point of weakness.

To simulate the two attachment holes in the body of the bar, two such pairs of holes were drilled in the main portion of the bar. One pair was spaced close about the longitudinal center line; as if on about $4^3/_4$-inch gage in the prototype; the other pair was spaced as if on about $14^1/_4$-inch gage in the prototype, or about half the bar width. The two pairs were drilled far enough apart so that one pair would not affect the other.

The anchorage bar was tested in the 800,000-lb. capacity Riehle testing machine. The Bethlehem Steel Company furnished the laboratory with a special testing rig which enabled Huggenberger tensometers to be attached to the surface of the test specimen at any location desired. Figures 2 and 3 show photographs of the testing rig and the end of test specimen with tensometers in position.

Strain observations included measurements on 29 equiangular‡ strain-rosettes and 43 single lines taken in duplicate on both sides of the specimen across various sections at the slotted end. Both half-inch and one-inch gage lengths were used, the former at locations where the stresses were changing most rapidly. Ten Huggenberger tensometers were available for strain measurement and since strains were to be measured at 248 different points it was necessary to repeat the loading operation 25 times. For each cycle, strain readings were first taken at loads of 20,000, 150,000 and 20,000 lb. These readings were a check on the correct functioning of the tensometers and were followed by readings at six or more increments between 20,000 and 150,000 lb. and finally by a third reading at 20,000 lb. The final determination of the measured strain was made by plotting the tensometer readings against the load increments. The foregoing method eliminates accidental error from the work of strain observation, this being a particularly important feature in interpreting the three directional equiangular strain-rosette. The range of loading used for strain readings did not cause the elastic limit of the material to be exceeded at any point.

After the strain readings within the elastic range were completed the anchorage bar was tested to failure. In

Table 1—Physical Properties of Material in Anchorage Bar

Specimen	Yield Point Psi	Tensile Ultimate Strength Psi	Modulus of Elasticity Psi	Per Cent Elongation in 2 In.
Main Bar:				
Longitudinal No. 1	38,000	64,900	30,200,000	45.0
Longitudinal No. 2	38,000	64,100		45.0
Longitudinal No. 3	37,800	64,100	29,800,000	47.0
Transverse No. 1	34,300	63,400	29,200,000	44.5
Transverse No. 2	35,800	63,000	29,100,000	44.5
Pin Plates:				
Longitudinal No. 1	42,000	70,200	30,250,000	30.0
Longitudinal No. 2	42,000	67,600		39.0
Longitudinal No. 3	41,000	68,400	29,600,000	39.0
Transverse No. 1	41,400	70,000	28,800,000	31.0
Transverse No. 2	41,100	69,500	30,200,000	32.0

‡Raymond D. Mindlin, "The Equiangular Strain-Rosette," *Civil Engineering*, **8**, No. 8, August 1938, p. 895.

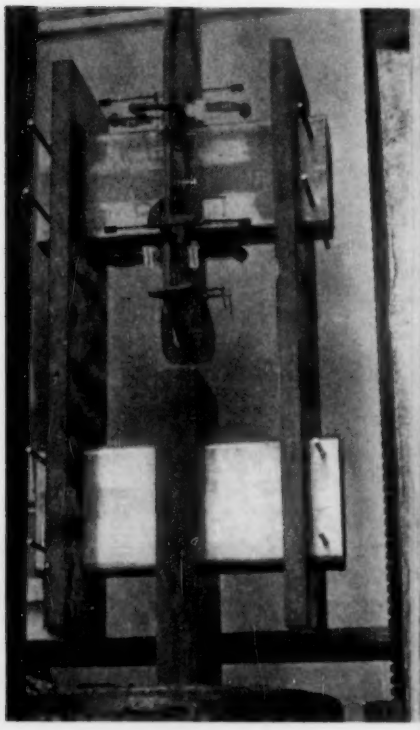


Fig. 2—Testing Rig

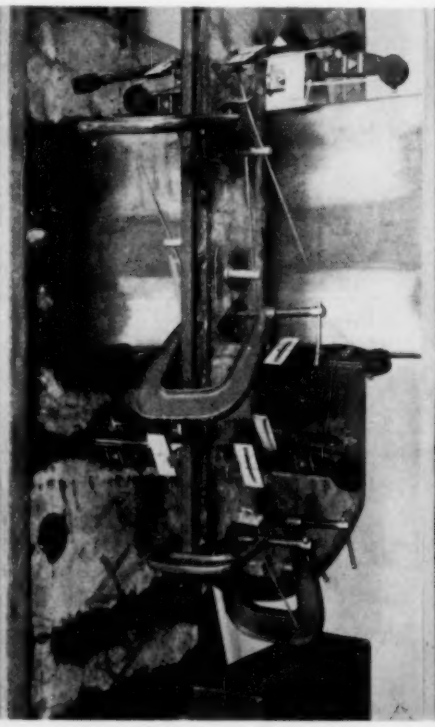


Fig. 3—Test Specimen with Tensometers in Position

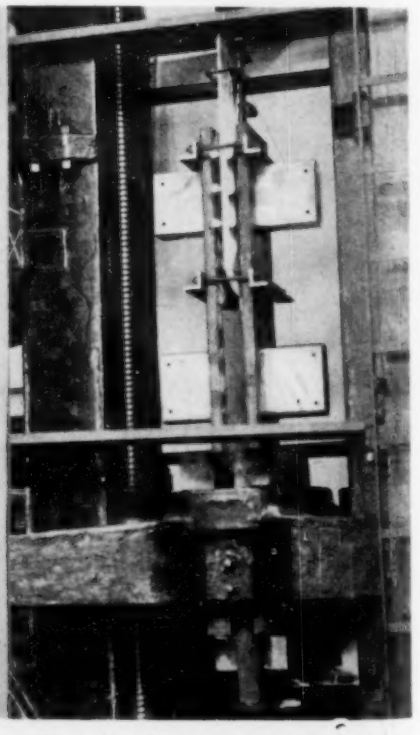
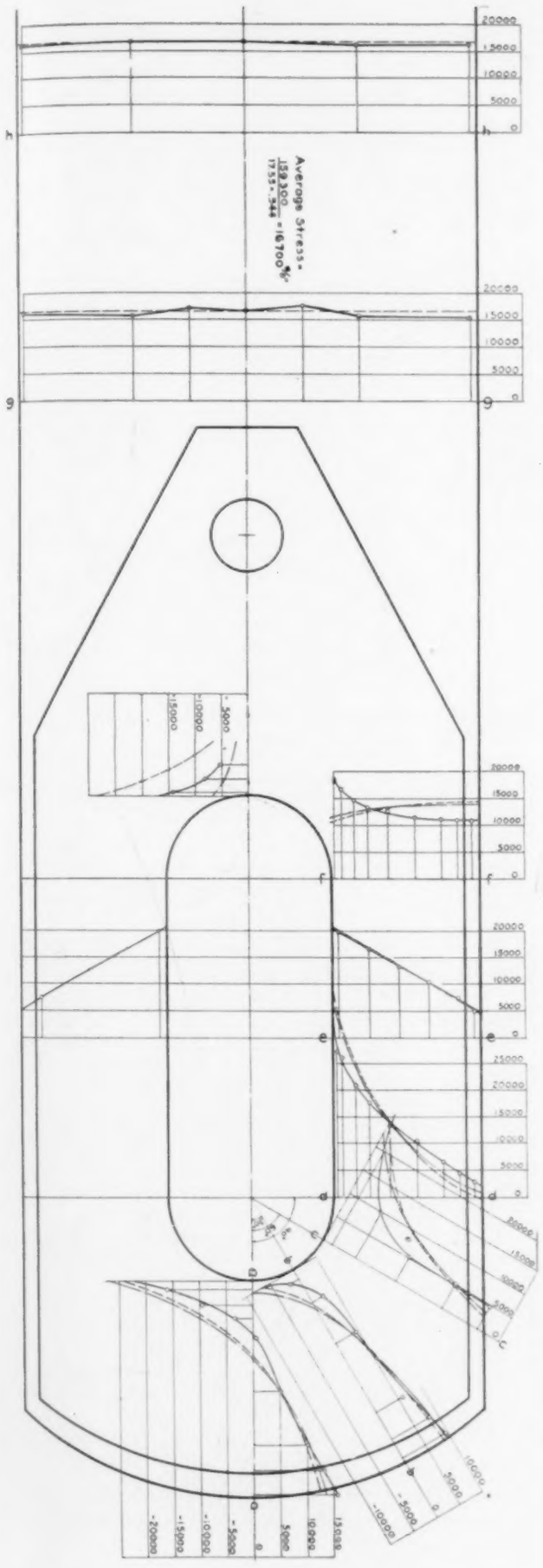


Fig. 4—Test Bar Supported Laterally by Rollers



Notes:
All stresses are given in pounds per sq. in., and are normal to Sections a-a, b-b, c-c, etc. as shown.
—o—o— Indicates actual unit stress as calculated from strain readings.
=—=—= Indicate calculated unit stresses from theoretical analyses on the basis of two different approximations as to the shape of the upper end of the test bar head as assumed for purposes of calculation (see appendix).

FIG. 5
MEASURED & CALCULATED STRESSES ON SLOTTED END OF TEST BAR

order to prevent failure by dishing, the bar was supported laterally in the vicinity of the pin by $1^1/_2$-inch diameter rollers clamped between the test bar and the side bars of the testing rig as shown in Fig. 4. The over-all elongation of the eyebar was measured by means of 0.001-inch Ames dials between the pin and a point in the straight section of the bar just above the pin plates. Strain measurements were also made in the straight section of the test bar by means of a 20-inch strain gage, both at the minimum section and at 20 inches below the minimum section.

TEST RESULTS

Stress Analysis.—The measured strains were used to determine the stress distribution at the end of the anchorage bar at loads within the elastic range of the material. The distribution of normal stress across six different sections through the slot and at two sections in the main bar close to the pin plates is shown in Fig. 5. These stresses are calculated on a basis of a working stress of 18,000 psi through the net section of the main bar which corresponded to a total load of 159,300 lb. The maximum measured tensile stress was 26,150 psi at a point $^3/_{16}$ inch from the inside edge on section *d-d* in Fig. 5. A maximum stress of about 33,000 psi at the inside edge of section *d-d* is indicated by extrapolating the line plotted through the points of measured stress. Figure 6 shows the measured radial or bearing stresses below the semi-circular bearing block. The maximum measured radial stress directly below the center of the bearing block on line *a-a* was 27,800 psi at a point $^3/_8$ inch below the edge of the pin hole. The maximum bearing pressure directly at the edge of the pin hole on line *a-a* is determined by extrapolation to be about 33,000 psi. It is concluded that the maximum tensile stresses and bearing pressures are within the elastic limit of the material and that there is a very good balance between the two.

It should be noted that in the areas of the plate close to the bearing block along lines *a-a*, *b-b*, *c-c* and *d-d* there was not a uniform relation between strain and load. This is due to a redistribution of bearing pressure which takes place entirely within the elastic range of the material. The pin hole at the bearing end of the slot was machined to a radius of 3.125 inch and the semi-circular bearing block had a radius of 3.109 inch. The pin plate closed in upon the bearing block in the elastic range and caused an outward shifting of bearing pressure. The stresses shown in Fig. 6 are based on actual strain increments in the load range of 20,000 to 150,000 and extended proportionately to the equivalent stresses in the working load range of 0 to 159,300 lb. This method of calculation gives very nearly the actual stress distribution at working load, but the distribution of bearing pressure will get progressively more uniform as the load

increases. The favorable redistribution of stress would be furthered by initial plastic yielding. In the present design with the stresses entirely within the elastic limit there is, however, no need to depend on plastic yielding for stress redistribution.

The results of a theoretical analysis of the tangential normal stresses are also shown on Fig. 5 for the same sections as previously noted. The analysis was made by means of strain-energy principles applied to the theory of bending of curved bars. Certain assumptions as to the geometrical shape of the end of the bar and the distribution of bearing pressure were made. An outline of the analysis and the resulting formulas for stress calculation are given in an appendix. In making an analysis of this type it is necessary to assume that the end of the bar around the slot is a closed link. The dimensions of the link at the sides and bearing end of the slot are assumed the same as the actual bar, but in front of the slot there is no definite criterion for the width of the link. Two analyses were made on the basis of the following two assumptions: (1) that the width of the link in front of slot was the same as at the bearing end, and (2) that the width of the link in front of the slot was $13^3/_4$ inch, or the same as that of the pin plates. The results of the analysis using assumption (1) are given by the short dashed lines on Fig. 5, and the results using assumption (2) are shown by the dot-and-dash lines on the same figure. The close agreement between maximum tangential stresses on the inner edge at the bearing end of the slot (where the actual stresses are maximum) indicates that the formulas which have been derived may be used in the study of other similar designs. The fact that the theoretical stresses in this location are not widely different for the two assumptions indicates that any reasonable assumption as to the shape of the front end of the link will result in computed stresses at the bearing end, not greatly different from the actual stresses.

Test to Failure.—Ultimate failure in the head of a bar of this type may be one of three types.**

1. Fracture through net section at the side of pin.
2. Fracture through the section below pin.
3. Dishing.

Calculations based on a previous paper showed that the strength with respect to failure by (1) or (2) was well over the strength based on the net section in the body of the bar. The load at which the head might dish was but a few per cent more than the strength through the net section. In the actual structure dishing is impossible because the end of the bar is buried in concrete. As a precautionary measure, therefore, the head was blocked laterally as previously described and as shown in Fig. 4.

Figure 7 shows the graph of load plotted against the deformation between the bearing block and an attachment to the main bar 36 inches above the bearing end of the slot. Figure 8 shows the record of load plotted against average longitudinal unit strain in a 20-inch gage length both across and adjacent to the minimum section near the center of the bar.

The following notes describe the progress of the test from initial load to failure in terms of the average tensile unit stress through the minimum section near the center of the bar:

Average Stress in psi	
28,200	Surface slip lines noted in whitewash at 45° to the direction of the bar and adjacent to the 21/32-in. ϕ holes in the center of the bar.
31,600	Surface slip lines in main bar adjacent to welds at top of pin plates. Local yielding under bearing block in head.

Bruce G. Johnston, "Pin Connected Plate Links," Proceedings of the American Society of Civil Engineers, **64, No. 3, pp. 461–483, March 1938.

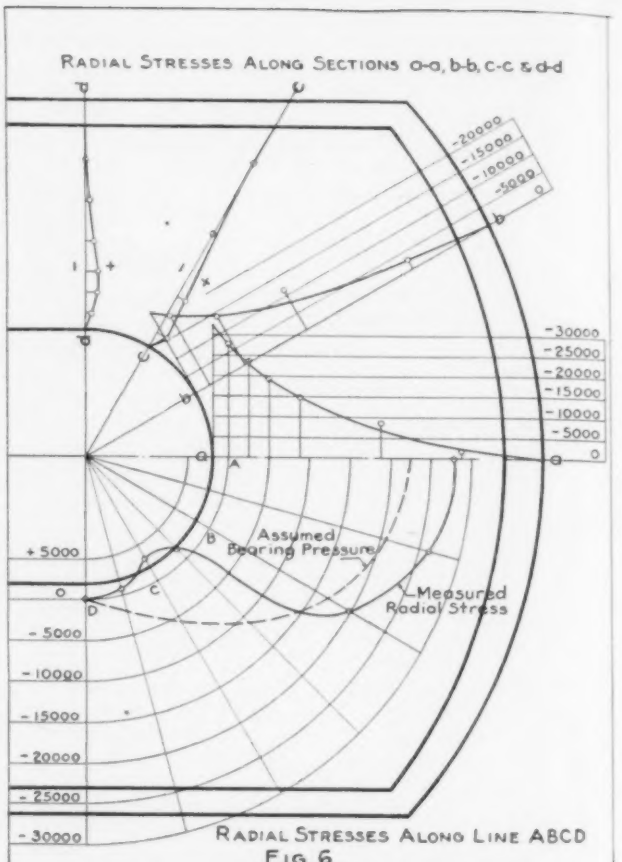


FIG. 6

RADIAL STRESSES BACK OF PIN

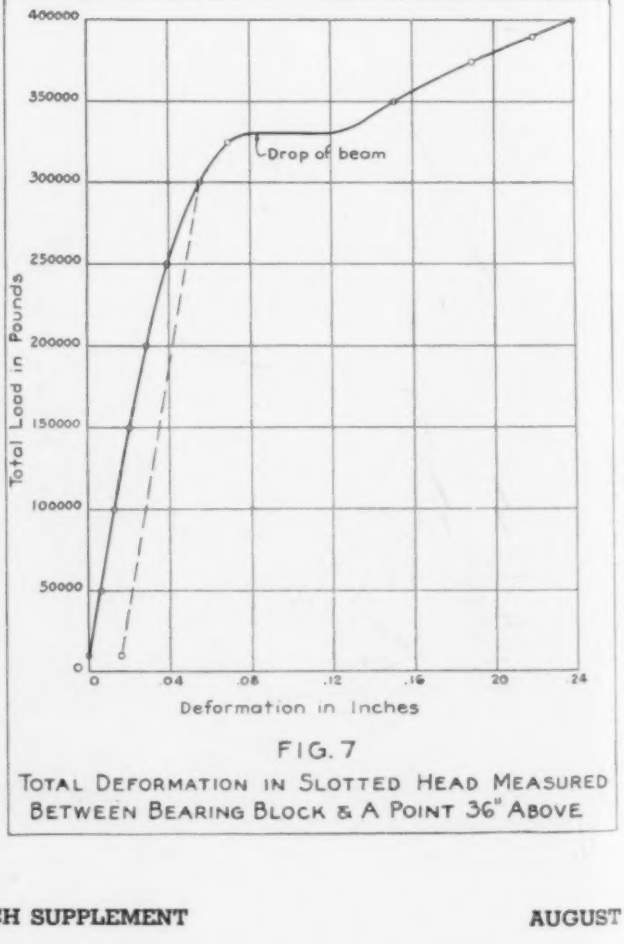


FIG. 7

TOTAL DEFORMATION IN SLOTTED HEAD MEASURED BETWEEN BEARING BLOCK & A POINT 36" ABOVE

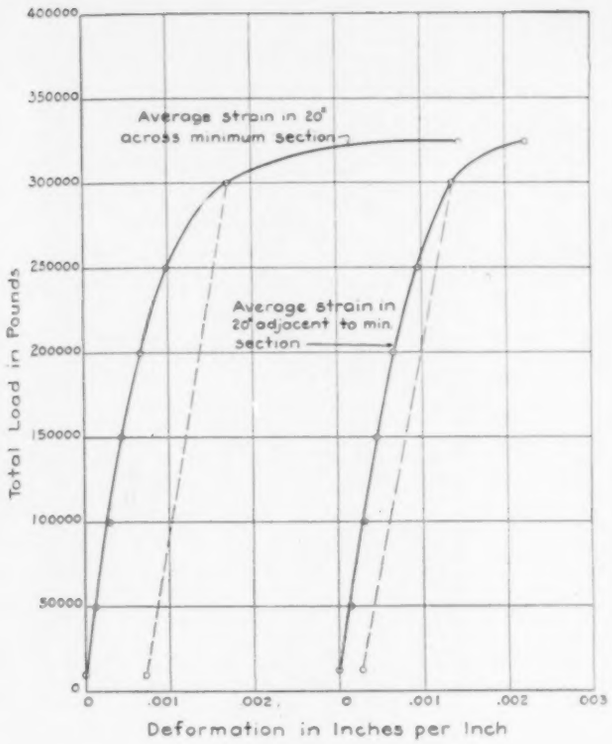


FIG. 8
AVERAGE STRAIN IN MAIN PLATE

37,000 General yielding of main bar as indicated on Figs. 7 and 8 and by drop of the beam on testing machine. This stress is close to the 38,000 psi yield strength of the longitudinal tensile coupons.

60,900 Fracture of main bar at minimum cross section through the lower pair of 21/32-inch ϕ holes near the center. This stress is 94.5 per cent that of the tensile unit strength of the longitudinal test coupons.

After fracture the length of the bar between bearing ends of slot and hole (15 feet $7^1/_2$ inches) had a permanent elongation of 10.1 per cent. The permanent elongation in the 20-inch gage length across the break was 12.5 per cent.

Figure 9 shows the appearance of the fracture and Fig. 10 shows the surface slip line pattern on the head of the bar around the slot after fracture. In Fig. 10 the surface slip lines should be differentiated from the general dark regions where the rollers had rubbed off some of the whitewash. The material in the head of the bar at the slot is seen to be above the yield point but there appears to be no immediate prospect of fracture in this region.

Prior to the failure, the entire bar between head reinforcements visibly showed a permanent uniform narrowing or lateral contraction. After failure, the general width was found to be reduced from $17^1/_2$ inches to about 17 inches and the thickness from 0.545 inch to 0.525 inch. It will be observed in Fig. 9 that there was a further necking on a section through the pair of closely-spaced small holes, where failure occurred; but little if any on a section through the widely-spaced small holes, where it would appear that appreciably more load would have been resisted.

It will be observed in Fig. 10 that the short stretches of main bar between intermittent fillet welds, showed cracking of the whitewash, but no further effect, and that the regions of the plug welds are hardly distinguishable from other areas. In some previous tests of riveted bar heads, the rivet holes have been definitely the seat of failure.††

SUMMARY AND CONCLUSIONS

1. At the working load the stresses in the slotted end of the bar were all below the yield-point stress of the material.
2. The general yielding of the bar took place at an average tensile stress through the minimum section of 37,000 psi and the maximum stress prior to fracture through the same section was 60,900 psi.
3. Calculated tangential stresses agreed well with measured stresses in regions of maximum stress behind the center of the slot.
4. The test and stress analysis showed that the slotted head of the bar was a well-balanced design and that it was stronger than the main body of the bar.
5. There was no indication of failure in any of the plug or fillet welds.
6. A head of this type could be designed on less conservative lines and still develop the full strength of the bar. The yield point in such a design would be exceeded in very localized regions but this would have little effect on the general behavior of the head.

APPENDIX I

The analysis of the slotted end of the bar was made by an application of the principle of least work to the strain energy of the curved and straight portions of a closed link.[1] An assumed link having a uniform width b is laid out as shown in Fig. 11. The actual variation in area and moment of inertia of radial cross sections at the ends is taken account of by assuming a parabolic variation for the inverse values of these factors. The resultant pressure between bearing block and end of slot is assumed to vary according to the cosine law, which is equivalent to the assumption that the projected components on a cross section at right angles to the longitudinal axis of the bar are uniform.

The following notation is in addition to that given on Figs. 11 and 12.

††The Quebec Bridge, **1**, p. 227, Report of the Canadian Government Board of Engineers, 1919.

[1] Timoshenko, "Strength of Materials," **2**, pp. 439–440.

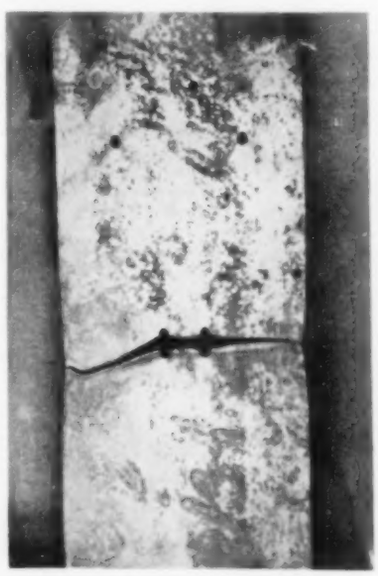


Fig. 9—Appearance of the Fracture

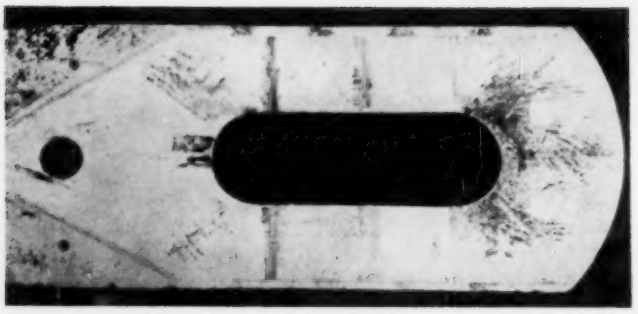


Fig. 10—Surface Slip Line. Pattern on the Head of the Bar Around the Slot After Fracture

M = moment at centroid of any cross section; positive moment causing compression on outside fibers.
N = resultant normal force acting at the centroid of any cross section, tensile force being considered positive.
P = half the total load on the bar.
ν = Poisson's Ratio = 0.3 for steel.
R = radius to centroid at curved ends.
A = area.
k = curved beam shape factor = 3/2 for rectangular cross section.

The steps in the analysis are as follows:

1. The redundant quantities M_o and N_o are determined by the theory of least work.
2. The moment and thrust may then be calculated at any section.
3. The tangential normal stress may then be calculated by the curved bar theory in the curved portions or by the usual theory in the straight parts.

Expressions for the strain energy in various parts of the link are written and differentiated with respect to the two redundants, M_o and N_o. Taking advantage of symmetry these are set equal to zero and the required integrations over the length of the link are performed. There result two simultaneous equations, the solution of which is shown on Fig. 13, Equation 1.

The constants K_{11}, K_{12}, etc., in Equation (1) are evaluated in terms of constants, C_{01}, C_{02}, etc., as given in Equation 2 of Fig. 13, and these in turn are given in terms of constants J_{01}, J_{02}, etc. as shown in Equation (3). Constants C_{s1}, C_{s2}, etc. are given in Equation (4), Fig. 13.

By changing the first subscript from 0 to 1, 2 and 3 successively in both the C and J terms there result expressions for the other 15 C constants.

Finally, the constants J_{01}, J_{02}, etc. are determined from the actual dimensions at the end of the eyebar head by Equation (5). Let h be the variable width of the eyebar head and ϕ_1 be the angle as shown on Fig. 11.

Letting $\phi = 0$; J_{01}, J_{11}, J_{21} and J_{31} are evaluated at once in each of the four equations.

The remaining two constants in each equation may be satisfactorily determined from the values of $\frac{1}{h}$ and $\frac{1}{h^3}$ corresponding to $\phi_1 = \frac{\pi}{6} = 30°$ and $\phi_1 = \frac{\pi}{2} = 90°$.

The determination of the J constants in this manner gave a good average agreement with the actual values for the various values of ϕ_1.

If the upper end of the link is assumed to have the same shape as the lower end, the number of C constants are reduced by one-half and the expressions for the K constants are simplified. If the link had a uniform cross section the values of $\frac{1}{h^3}$ and $\frac{1}{h}$ would be constant rather than variable and the computation would be further simplified.

After M_o and N_o are determined by the foregoing cal-

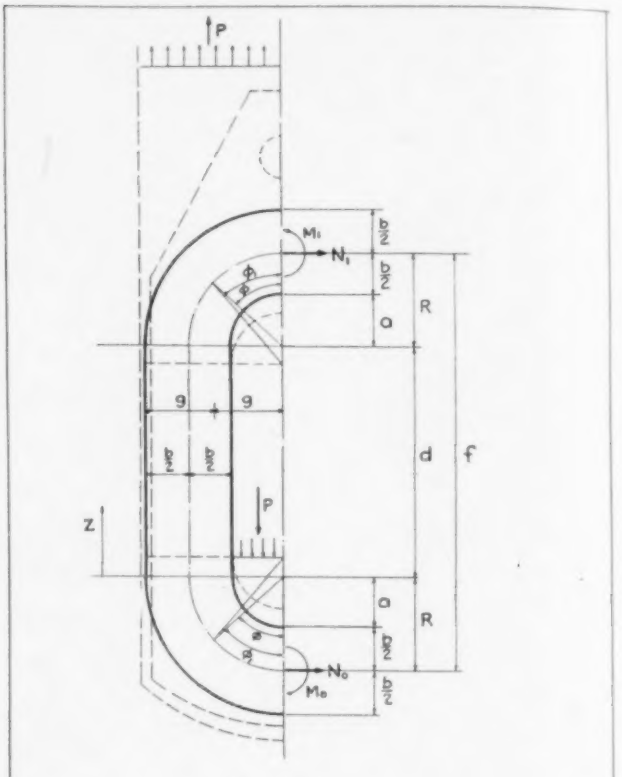


FIG. 11
NOMENCLATURE USED
IN THEORETICAL ANALYSIS

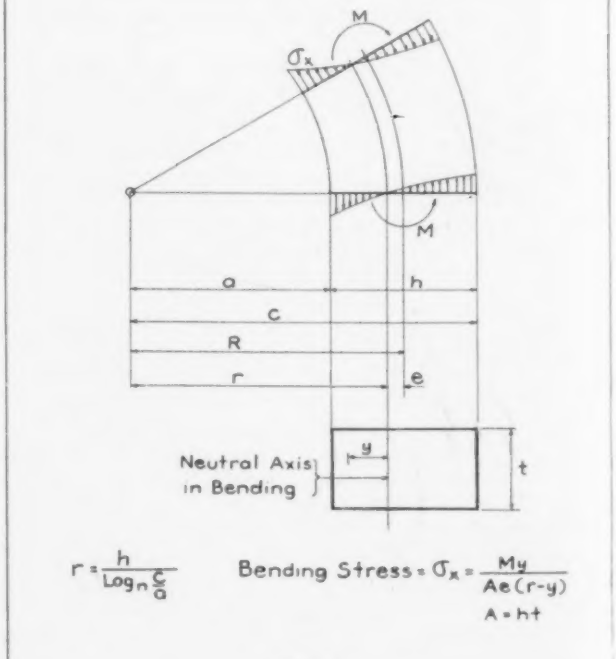


FIG. 12
NOMENCLATURE AND FORMULAS
FOR CALCULATING BENDING STRESS
IN A CURVED BEAM

$$M_o = \left[\frac{-K_{13}K_{22}+K_{12}K_{23}}{+K_{11}K_{22}-K_{12}K_{21}}\right]P \qquad (1)$$
$$N_o = \left[\frac{-K_{11}K_{23}+K_{21}K_{13}}{+K_{11}K_{22}-K_{12}K_{21}}\right]P$$

$$K_{11} = 12RC_{02} + C_{51} + 12RC_{22}$$
$$K_{12} = 12R^2C_{03} - 12R^2C_{02} - C_{13} - C_{52} - 12R(d+R)C_{22} - 12R^2C_{23} + C_{33}$$
$$K_{13} = 12RgC_{01} - C_{11} + C_{53} + 6RaC_{21} + 6RbC_{22} - C_{31} \qquad (2)$$
$$K_{21} = K_{12}$$
$$K_{22} = 12R^3C_{02} - 24R^3C_{03} + 12R^3C_{04} + 2RC_{13} - RC_{15} + 2K(1+\nu)RC_{11} + C_{55} + C_{57} + 12R(d+R)^2C_{22} + 24R^2(d+R)C_{23} + 12R^3C_{25} - 2(d+R)C_{33} - RC_{35} + 2K(1+\nu)RC_{31}$$
$$K_{23} = 12R^2gC_{04} - 12R^2gC_{01} + RC_{11} - gC_{14} - 2K(1+\nu)RC_{14} - C_{56} - 6R^2(d+R)C_{21} - 6R^3C_{24} - 6Rb(d+R)C_{22} - 6R^2bC_{23} + \frac{g}{2}C_{34} + \frac{b}{2}C_{33} + (d+R)C_{31} + 2K(1+\nu)RC_{34}$$

$$C_{01} = J_{01}\frac{\pi}{4} + J_{02}\left(\frac{\pi^2+4}{16}\right) + J_{03}\left(\frac{\pi^3+6\pi}{48}\right)$$
$$C_{02} = J_{01}\frac{\pi}{2} + J_{02}\frac{\pi^2}{8} + J_{03}\frac{\pi^3}{24} \qquad (3)$$
$$C_{03} = J_{01} + J_{02}\left(\frac{\pi}{2}-1\right) + J_{03}\left(\frac{\pi^2}{4}-2\right)$$
$$C_{04} = \frac{J_{01}}{3} + J_{02}\left(\frac{3\pi-4}{18}\right) + J_{03}\left(\frac{\pi^2}{12}-\frac{14}{27}\right)$$
$$C_{05} = \frac{J_{01}\pi}{4} + J_{02}\left(\frac{\pi^2-4}{16}\right) + J_{03}\left(\frac{\pi^3-6\pi}{48}\right)$$

$$C_{51} = \frac{12d}{b^3} \quad C_{57} = \frac{2K(1+\nu)d}{b}$$
$$C_{52} = \frac{12Rd+6d^2}{b^3} \qquad (4)$$
$$C_{53} = \frac{12gd}{b^3}$$
$$C_{56} = \frac{12R^2d+12Rd^2+4d^3}{b^3}$$
$$C_{56} = \frac{12Rgd+6gd^2}{b^3}$$

At back or bearing end	At upper end
$\frac{1}{h^3} = J_{01} + J_{02}\phi_1 + J_{03}\phi_1^2$	$\frac{1}{h^3} = J_{21} + J_{22}\phi_1 + J_{23}\phi_1^2$
$\frac{1}{h} = J_{11} + J_{12}\phi_1 + J_{13}\phi_1^2$	$\frac{1}{h} = J_{31} + J_{32}\phi_1 + J_{33}\phi_1^2$

(5)

FIG. 13 — EXPRESSIONS FOR M_o AND N_o

culations the following equations may be used to evaluate the moment and thrust on any section.

In the lower curved portion:

$$N = P\sin^2\phi + N_o\cos\phi$$
$$M = Pg\sin^2\phi + M_o - N_oR + N_oR\cos\phi \qquad (6)$$

In the straight portion:

$$N = P$$
$$M = M_o - N_o(Z+R) + Pg \qquad (7)$$

The moment and thrust may also be calculated in the upper section but the results cannot be expected to agree closely with actual stresses because of the difference between the actual bar and assumed link in this section. These stresses, however, will not be as great as those in the lower section.

The procedure for calculating the stress is as follows: For any desired point on the centroid of the actual bar the moment and thrust are calculated for the corresponding point in the assumed link which is shown by the heavy lines in Fig. 11. The total stress at any point will then be the sum of the average direct stress and the bending stress. In calculating the stresses the dimensions of the actual link are used.

$$\sigma = \frac{N}{A} + \frac{My}{Ae(r-y)} \qquad (8)$$

The eccentricity e as shown in Fig. 12 locates the neutral axis of bending and is determined for rectangular sections by the formula (3)

$$e = R - r \qquad (9)$$

$$\text{where } r = \frac{h}{\log_n \frac{c}{a}} \qquad (10)$$

The maximum tangential stress will usually be on the inside of the link at the tangent between the lower curved portion and the straight portion where $\phi = 90° = \frac{\pi}{2}$. This point is actually on the straight portion of the assumed link, but in analyzing the actual link the curved beam theory should be applied. The stress in the center region of the straight part of the link is given by:

$$\sigma = \frac{N}{A} + \frac{My}{I} \qquad (11)$$

In computing the stresses in the Tacoma Narrows anchorage bar by the preceding method an equivalent rectangular cross section was assumed which had the same total thickness and area as the two pin plates and center plate at every section.[3] The following results were obtained for the design load of 2P = 159,300 lb.

1. Assuming the dimensions of the link to be the same at the top as the bottom:

$$M_o = -2.284P = -181{,}900 \text{ in.-lb.}$$
$$N_o = +0.1088P = +8665 \text{ lb.}$$

2. Assuming the top of the link to have a width equal to the over-all width of the pin plate at that point:

$$M_o = -2.483P = -197{,}800 \text{ in.-lb.}$$
$$N_o = +0.0960P = +7646 \text{ lb.}$$

The stresses computed on the basis of these widely different assumptions as to upper dimensions of the link are both in reasonably good agreement with actual measured stresses in the lower half of the link. In the upper half the agreement is not good but the stresses here are not critical. The results have been given on Fig. 5 for both assumptions.

Correction

In a recent communication Dr. Rosenthal pointed out two misstatements that occured in the translation of the paper "Elastic Behavior and Strength of Side Fillet Welds" by D. Rosenthal and P. Levray, AMERICAN WELDING SOCIETY JOURNAL, **18** (4) Suppl., 140–149, April 1939.

1. Our Welding Research Commission is not a part of the Belgian National Foundation for Scientific Research, as it would appear from the reference given on page 140, but is only sponsored by this body.
2. The remark "fractured through the measuring holes" in the Table 1, page 141, applies only to two of the 24 specimens tested in the Series A and B. These two specimens have been discarded in calculating the mean value of the breaking stress in lb./in.^{2} All other specimens fractured through the fillet welds.

FIRST PROGRESS REPORT

Joint Investigation of Continuous Welded Rail*

By HERBERT F. MOORE,† HOWARD R. THOMAS,‡ and RALPH E. CRAMER**

I. INTRODUCTION

Forward.—In putting out this Progress Report especial emphasis is laid on the fact that it is a progress report and not a final report. It has been necessary to report tentative values of fatigue strength, drop test values and bend test values for continuous welded rail on the basis of very few tests. This necessity in the case of fatigue tests is one faced by all investigators of fatigue properties of full-sized members. It is most strongly urged that these results be regarded as tentative, and if they are so regarded, it is believed that the publication of them at this time will be of value.

1. *Beginning of Investigation*

In September 1937 arrangements were completed for a cooperative investigation of continuous welded rail. The investigation is under the joint auspices of the Association of American Railroads and the Engineering Experiment Station, University of Illinois. The Association of American Railroads through the American Railway Engineering Association Committee on Rail appointed the following Advisory Committee for the investigation:

J. C. Patterson, Chief Engineer, Maintenance of Way, Erie Railroad Company, *Chairman.*

G. M. Magee, Research Engineer, Engineering Division, Association of American Railroads.

C. E. Morgan, Superintendent Work Equipment and Track Welding, Chicago, Milwaukee, St. Paul & Pacific Railroad.

G. R. Smiley, Chief Engineer, Louisville and Nashville Railroad.

H. S. Clarke, Engineer Maintenance, Delaware & Hudson Railroad Corp.

Mr. Clarke has died since the beginning of the investigation, and the members of the test party of the investigation wish to record here their appreciation of his help in the early testing work of the investigation, and their deep sorrow at his loss.

Mr. P. O. Ferris, Engineer of Maintenance of Way, Delaware and Hudson Railroad Corporation has been appointed in Mr. Clarke's place.

At the University of Illinois the work was placed under the general direction of Professor H. F. Moore. Close contact is maintained with Professor A. N. Talbot and the Special Committee on Stresses in Railroad Track. One-quarter of the time of Professor H. R. Thomas, Engineer of Tests and of Professor R. E. Cramer, Metallurgist, is given to this investigation. The assistance of Messrs. S. W. Lyon, J. L. Bisesi, E. C. Bast and N. J. Alleman is gratefully acknowledged.

Acknowledgment is also made of the cooperation of the following representatives of welding companies in preparing specimens and offering constructive criticisms of test methods and form of presentation of results:

Lem Adams, Chief Engineer, The Oxweld Railroad Service Company.

C. A. Daley, Maintenance of Way Engineer, Air Reduction Sales Co.

J. H. Deppeler, Chief Engineer, Metal & Thermit Corporation.

H. C. Drake, Director of Research, Sperry Products, Incorporated.

2. *General Outline of Work*

Eventually a considerable amount of field study of welded joints will have to be done, and some is in progress already. The general lines of laboratory investigation in progress are:

(1) Etch tests of the metal in specimens of welded rail joints made by various processes, to see whether any "flakes," laps, holes, burns, inclusions, cracks or any other type of defect, have formed during the process of welding and subsequent cooling, and, if so, where these defects are located in the joint.

(2) Hardness surveys of the metal in the weld, in the adjacent rail and in the transition zone between weld metal and rail metal.

(3) Survey of typical crystalline structure in weld, rail and transition zone by means of the metallographic microscope.

(4) Chemical analyses of steel in rail, in weld and in the transition zone.

(5) Mechanical tests of specimens cut from weld metal, rail metal and transition zone. These tests include tension tests, impact tests and fatigue tests.

(6) Rolling-load fatigue tests of full-size specimens of welded rail to determine: (*a*) whether internal fissures can be developed in welded joints, (*b*) to determine the fatigue strength in flexure of welded joints, and (*c*) to determine likelihood of damage to the web of the rail near the weld.

(7) Drop tests and bend tests of full-size rail joints.

In planning tests and considering test results two bases of comparison must always be kept in mind: (1) a comparison between welded joint and ordinary joint-bar joint as to strength and toughness and wearing qualities, and (2) a comparison between welded joint and rail. In comparing the strength and other properties of welded joints with those of ordinary joint-bar joints close contact is maintained with the work of Professor A. N. Talbot's Committee on Stresses in Railroad Track.

* Reprinted from the Proceedings of the American Railway Engineering Association for 1939, pages 687–713.

† Research Professor of Engineering Materials, University of Illinois. In Charge, Joint Investigation of Continuous Welded Rail.

‡ Special Research Professor of Engineering Materials, University of Illinois. Engineer of Tests, Joint Investigation of Continuous Welded Rail.

** Special Research Assistant Professor of Engineering Materials, University of Illinois. Metallurgist, Joint Investigation of Continuous Welded Rail.

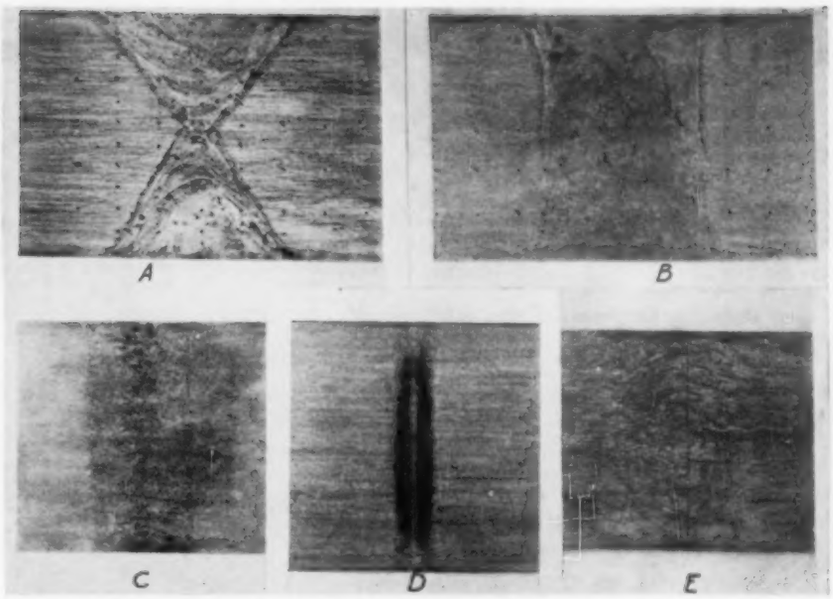


Fig. 1—Horizontal Etched Sections of Welds 1/16 Inch Below the Tread. Etched with Hot Hydrochloric Acid. Approx. 0.4 Natural Size

A. Fusion gas weld, Specimen W8, Process *A*
B. Fusion gas weld, Specimen W101, Process *B*
C. Thermit weld, Specimen W252, Process *C*
D. Electric flash pressure weld, Specimen W303, Process *D*
E. Gas-heated pressure weld, Specimen W406, Process *E*

Note the regularly spaced dots on *A* and *B* are the marks of the Rockwell "C" diamond cone point used in the hardness surveys.

3. *Types of Welded Joint Studied*

Up to the present time 104 test rails welded by five different processes have been supplied.

Process A.—Twelve 112-lb. rails and twelve 131-lb. rails. This process is a gas welding process. The rails were first beveled with a torch and the joint welded by two welders, one on each side of the weld. A special welding rod was used. The base was welded first, starting at the web; next the web was welded from base to head, then the head was welded. When completed the top surface was peened by hammering. It took about one and one-half hours to complete a weld. After the head had cooled to black heat the entire rail was heated with a large acetylene torch to a normalizing temperature (about 1600° F.) for a distance of about $3^1/_2$ inches on each side of the weld. Then a strip of sheet metal was placed around the weld and filled with ground magnesia insulating material, covering a distance of about 8 inches on each side of the weld. This material allowed the heated head of the rail to cool to about 200° F. in about 3 hours.

Process B.—Twelve 112-lb. rails and twelve 131-lb. rails. This was also a gas welding process. The rail ends were beveled with a cutting torch. The surface of the bevel was ground clear of oxide. One welder made each joint. First the web of the rail was welded, starting from the junction of web and base and welding up to the head. The second step was to weld the base on both sides from the web out. The head was welded last. The weld metal was deposited in layers across the extent of the head and peened several times with the hammer during welding. The weld took about 3 hours to complete. After the welding was complete, a special furnace was put around the weld and the weld was heated to about 1200° F.—a stress-relieving treatment. The holes in the furnaces used for burners were then covered and the rail allowed to cool in the furnace. It required approximately 3 hours for the rail to cool to 300° F.

Process C.—Twelve 112-lb. and twelve 131-lb. rails. This was the Thermit process using the intense heat generated by the transformation of aluminum to aluminum oxide. The welding of the rails by this process consists of three main steps: (1) The preparation of the rails for welding—including cutting of the gap, end-facing and aligning; (2) the actual welding of the rail—which includes preheating, pouring of the molten products of the reaction into the molds, and pressing together the rails in order to effect the pressure weld; and (3) the finishing operation—knocking off the catch basins and steel nubs on the sides of the head, post-heating to relieve strain, grinding and finishing the gage and tread surfaces.

The extensive details of this process and further details of the other processes are not given here in this summary but are on file at the office of the investigation at the University of Illinois.

Process D.—Electric Flash Pressure Butt Welds. Twelve 131-lb. rails. The detailed description of this process involving, as it does, very elaborate equipment and the welding together of as much as 1400 feet of rail before placing it in the track will not be given here. A quite complete description may be found in *Railway Age*, Vol. 103, No. 10, page 300. (September 4, 1937).

Process E.—Gas-heated Pressure Butt Welds. Twelve 112-lb. rails. The rail ends to be joined were ground square. A special oil pressure cylinder was used to force the rail ends together under longitudinal pressure. While pressure was being applied the metal on both sides of the joint was heated by a large number of small oxyacetylene flames, completely surrounding the rail joint. These flames were on a movable frame which oscillated back and forth along the rail through about $1^1/_2$ inches on each side of the abutting rail ends. As the rail steel softened under the heat of the gas flames, the steady pressure of the oil-pressure cylinder forced the rail ends together about $^7/_8$ inch, forming an upset bead of deformed metal at the joint. After the flames had been shut off, and the rails had cooled below the critical temperature of the rail steel, they were reheated in another oxyacetylene heating rig slightly above the critical temperature to refine the grain of the joint metal and relieve internal stresses in the steel.

II. ETCH TESTS, METALLOGRAPHIC TESTS AND HARDNESS SURVEYS

By R. E. Cramer assisted by E. C. Bast

4. *Etch Tests of Metal at Welded Joint*

Figure 1 shows typical etch test results for test joints welded by the five processes studied. The etch tests shown in Fig. 1 were on horizontal slices from rail heads, approximately $^5/_{16}$ inch below the tread. The slices

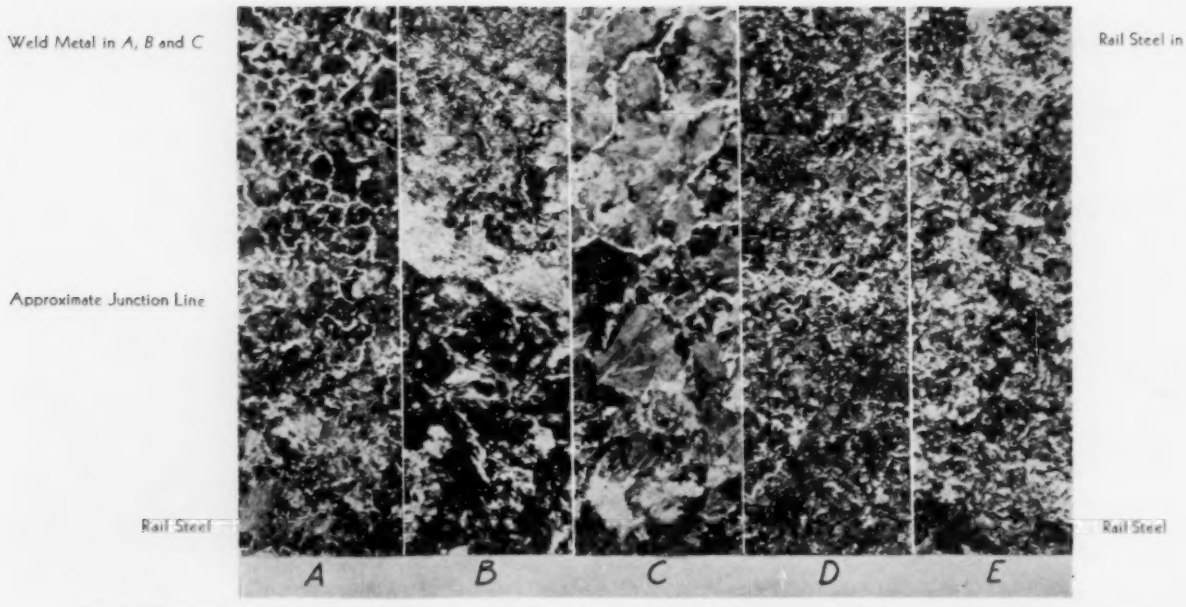


Fig. 2—Micrographs of Crystalline Structure at Junction of Weld Metal and Rail Steel. Magnification 44 ×. Etch 2 Per Cent Nital

A. Fusion gas weld, Process *A*
B. Fusion gas weld, Process *B*
C. Thermit weld, Process *C*
D. Electric flash pressure weld, Process *D*
E Gas-heated pressure weld, Process *E*

were rough polished and then etched in a 50 per cent solution of hot hydrochloric acid for 20 to 40 minutes. In Fig. 1 (*a*) and (*b*) the regularly spaced dots are the marks of the Rockwell "C" diamond cone, used in the hardness surveys.

Evaluating etch test results is very difficult. Evidences of inclusions, laps, bending of the "fiber" of the metal and some crack-like markings are shown. How much damage these do is rather uncertain, although actual cracks may do very considerable structural damage by acting as nuclei for the spread of fissures, or other fatigue cracks. Obviously the macro-structure of the steel in and near the weld has been changed from that in the rail itself. The macro-structure of the joint metal is further discussed after the results of tests of joints to fracture under repeated wheel loads have been presented.

5. *Micrographs of Steel in Welded Joints*

Figure 2 shows micrographs of the metal at the weld and on each side of it for the five types of welded joints tested. It will be noted that the specimens for Processes *A*, *B* and *C* show more ferrite and larger grain sizes than do the specimens for Processes *D* and *E*. Figure 3 shows a method for locating the junction line of butt-welded rails by examining the decarburized surface of the rails. In this metal the ferrite and pearlite grains of the two rails do not "match" and thus the junction line is made quite distinct.

6. *Hardness Surveys of Welded Joints*

The Rockwell "C" hardness test was selected as the most suitable for hardness surveys of areas of rail steel, weld metal, and the fairly well-defined "heat-affected area" adjacent to the weld metal. This test measures the net penetration of a diamond cone point under a load of 150 kg. after an initial load of 10 kg. has been applied. Hardness tests were made over the area of the weld region cut by various planes, horizontal and vertical. Indentations were made $^1/_2$ inch apart along the rail, and, usually, $^1/_4$ inch apart across the rail. The results on a horizontal plane $^3/_{16}$ inch below the tread of the rail are given in Table 1. These results give some idea of the relative hardness of rail, heat-affected zone and weld metal, and also some idea of the "scatter" of hardness values. With the small indentation made by the Rockwell "C" diamond brale (cone point) more localized spots of hard or soft metal are detected than with the relatively large indentation of a Brinell ball. It is rather doubtful whether the low Rockwell numbers reported are as significant as an index of general hardness of the metal as Brinell readings, but Rockwell readings are more sensitive to localized variation of hardness in the metal. It will be noted that for fusion gas weld *B* the weld metal was distinctly harder than the rail steel. This is probably due to the welding rod used. In general, it would seem to be of advantage to have the weld about as hard as the rail steel, to even up the wear along the track.

Table 1—Hardness Surveys of Welded Rail Joints

Rockwell "C" Numbers along horizontal section $^3/_{16}$ inch below tread of rail

Type of Joint	Weight of Rail Lb. per Yd.	Rail Min.	Rail Av.	Rail Max.	Rockwell "C" Numbers Heat-Affected Zone Min.	Heat-Affected Zone Av.	Heat-Affected Zone Max.	Weld Metal or Junction Line Min.	Weld Metal or Junction Line Av.	Weld Metal or Junction Line Max.
Fusion weld *A* (gas)	112	21	22	24	13	21	25	13	22	30
Fusion weld *B* (gas)	131	22	23	24	17	26	29	28	31	37
Thermit weld *C*	112	21	23	24	19	24	26	16	22	26
" " *C*	131	20	22	24	23	27	29	10	25	28
Electric flash butt weld *D*	131	21	23	24	27	28	30	27	30	32
Gas-heated pressure weld *E*	112	21	23	24	16	20	24	17	25	28

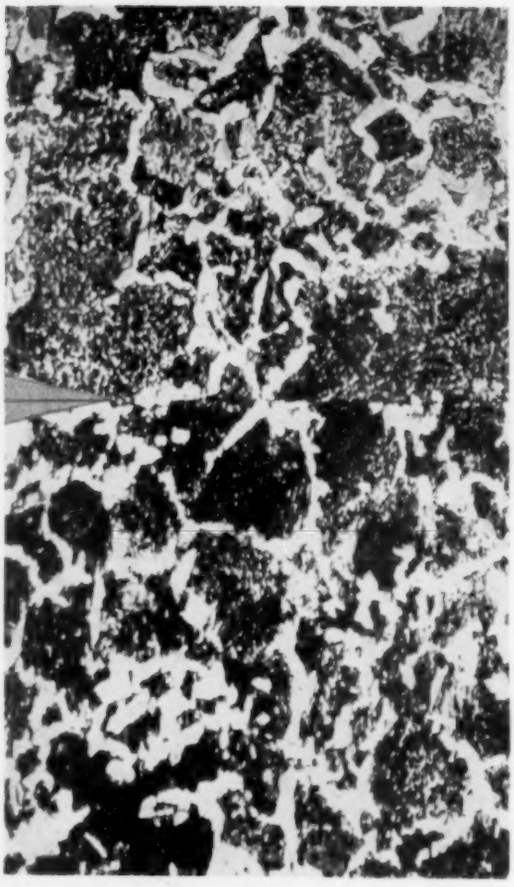


Fig. 3—Micrograph Showing Junction Line Outlined by Break in Crystalline Structure of Steel in Butt Weld, Process E. Magnification 400×. Etch 2 Per Cent Nital

One method of locating the junction line of butt-welded rails is to examine the surface of the rails, especially if it is decarburized as was the case in the joint shown above. In this decarburized metal the pattern of the ferrite network in one rail does not match that of the other, thus making the junction line quite distinct.

7. *Chemical Analyses of Rail Steel and of Welding Rods*

The following analyses were reported for rail steel and welding rods.

Rail steel for joints made by Processes *A*, *B*, *C* and *D*, reported by Robert W. Hunt Company:

131-lb. rail, Carbon 0.71%; Manganese 0.81%; Phosphorus 0.023%; Sulphur 0.037%; Silicon 0.15%.

112-lb. rail, Carbon 0.70%; Manganese 0.76%; Phosphorus 0.020%; Sulphur 0.024%; Silicon 0.19%.

Rail steel for joints made by Process *E*, reported by firm making the test joints:

112-lb. rail, Carbon 0.73%; Manganese 0.80%; Phosphorus 0.020%; Sulphur 0.022%; Silicon 0.20%.

Welding rods used in Process *A* and rods used in Process *B*, analyses reported by firms making the test joints:

Welding rod for Process *A*. Carbon 0.45–0.50%; Manganese 1.00%; Silicon 0.45%; Chromium 1.00%.

Welding rod for Process *B*. Carbon 0.20–0.40%; Manganese 0.85–1.15%; Phosphorus 0.035%; Sulphur 0.04%; Silicon 0.15%; Chromium 0.90–1.25%; Vanadium 0.10–0.30%.

Approximate analysis of weld metal for Process *C*. Carbon 0.41%; Manganese 0.74%; Phosphorus 0.036%; Sulphur 0.058%; Silicon 0.044%, reported in *Railway Engineering Maintenance*, June 1936, pp. 352–356.

III. MECHANICAL TESTS OF SPECIMENS FROM WELDED RAIL JOINTS

By S. W. Lyon

8. *Strength, Ductility and Toughness Tests of Specimens from Welds*

Specimens for the usual mechanical tests were cut from weld metal, from heat-affected zone metal and from unaffected rail steel for each type of joint furnished for testing. Tension tests, impact-tension tests and fatigue tests were made. The specimens used are shown in Fig. 4. Tension tests were made in an Olsen screw-power testing machine using Robertson shackles to hold the specimens; impact-tension tests were made in a Charpy impact machine, fitted with a Lyon tension attachment; and fatigue tests were made in small rotating-cantilever machines which subjected the specimen to a complete reversal of flexural stress during a cycle of stress (one revolution of the machine).

Figures 5, 6 and 7 show graphically typical results for tension, tension-impact and fatigue tests. As will be noted from these figures, specimens were cut from various portions of the welded joint and from the rail steel which is unaffected by the welding treatment. There have been made approximately 550 tension tests, 520 tension-impact tests and 1050 fatigue tests, a total of approximately 2120 specimens tested. Table 2 summarizes the results of these tests. Table 2 is divided into two parts, in one of which (*a*) the actual test values are tabulated, and in the other of which (*b*) the values are tabulated in terms of percentage of the figures obtained from the rail steel.

9. *Conclusions from the Tests of Specimens*

These tests of specimens cut from welded joints give an indication of the variation of strength of material in different parts of the joint. They do not give any indication of the effect of imperfections such as laps, blow-holes, small cracks, inclusions and sharp changes of outline upon the fatigue strength of a joint as a whole. For judging this effect, the rolling-load tests, which are discussed in the next section, seem distinctly more significant.

However, the tests of these specimens do indicate for all the welded joints a rather wide variation in the quality of material in the joint as welded. None of the material in the welded joint reaches 100 per cent of the values for the rail steel with the exception of the impact-tension values of steel from the head of the rail joints welded by Process *D*, and reduction of area values for the web of the rail joints welded by Process *E*. The tensile strength for head, web and base of joints welded by Process *D* very nearly reached the tensile strength of the rail steel.

IV. TESTS OF WELDED JOINTS UNDER REPEATED WHEEL LOAD

By H. F. Moore, H. R. Thomas and N. J. Alleman

10. *Rolling-Load Tests of Welded Joints*

The testing machine used for rolling-load tests of welded rail joints is similar to those used in the Investiga-

Fig. 4—Types of Specimens for Mechanical Tests

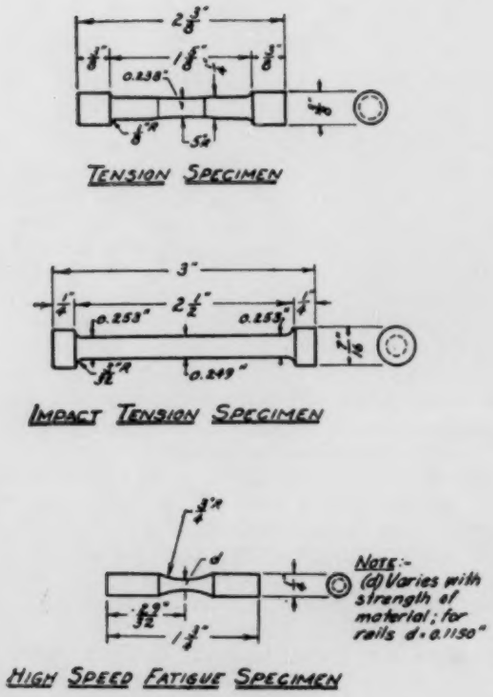

TENSION SPECIMEN
IMPACT TENSION SPECIMEN
HIGH SPEED FATIGUE SPECIMEN
Note:- (d) Varies with strength of material; for rails d = 0.1150"

Fig. 5—Typical Tension Test Results for Specimens from Welded Rails

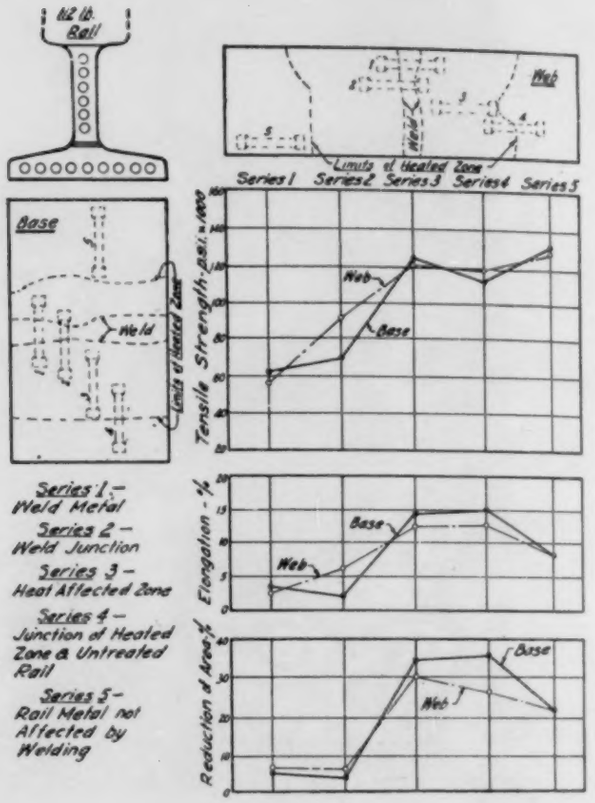

112 lb. Rail
Base
Weld
Limits of Heated Zone
Series 1 – Weld Metal
Series 2 – Weld Junction
Series 3 – Heat Affected Zone
Series 4 – Junction of Heated Zone & Untreated Rail
Series 5 – Rail Metal not Affected by Welding
Series 1 Series 2 Series 3 Series 4 Series 5
Tensile Strength-psi. x 1000
Elongation - %
Reduction of Area %
Web
Base

Fig. 6—Typical Impact-Tension Test Results for Specimens from Welded Rails

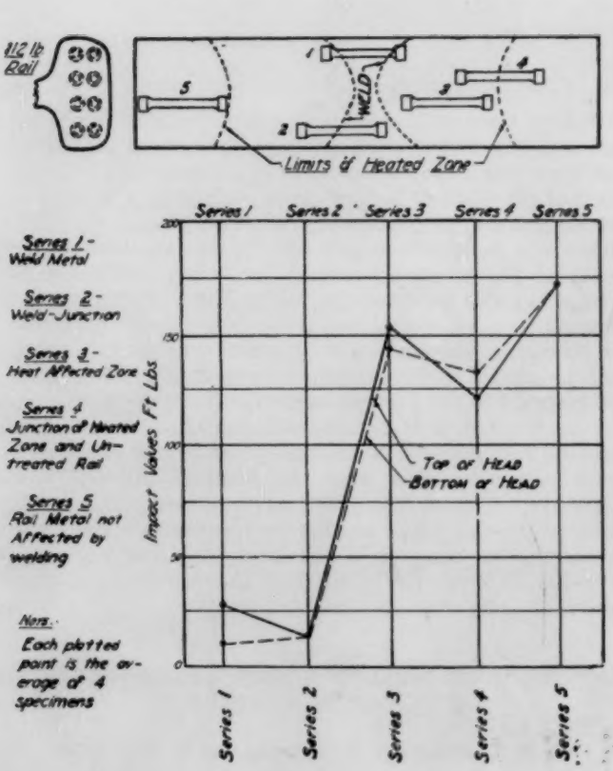

112 lb Rail
Limits of Heated Zone
Series 1 – Weld Metal
Series 2 – Weld-Junction
Series 3 – Heat Affected Zone
Series 4 – Junction of Heated Zone and Untreated Rail
Series 5 – Rail Metal not Affected by welding
Note: Each plotted point is the average of 4 specimens
Impact Values Ft Lbs
Top or Head
Bottom or Head
Series 1
Series 2
Series 3
Series 4
Series 5

Fig. 7—Typical Fatigue Test Results for Specimens from Welded Rails

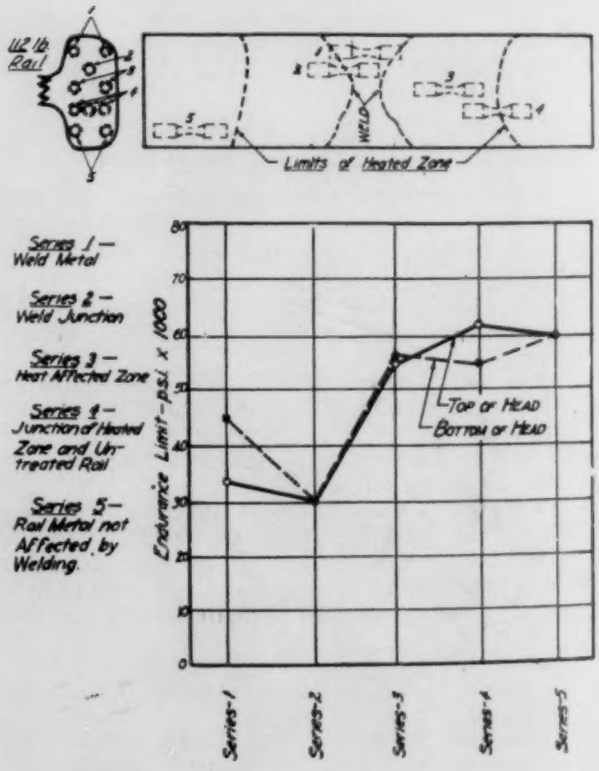


Table 2—Test Results for Specimens Cut from Welded Rail Joints

The values given for rail steel are the average values for the specimens cut from a location in the joint specimen outside the heat-affected zone of metal. The values given for weld metal are the lowest values observed along the length of the weld and in its immediate vicinity. Most of these values are for weld metal, but a few are for the metal at the junction of weld and rail steel

Part (*a*) of this table gives the results in terms of the units commonly used in reporting test results, lb. per sq. in., per cent and ft.-lb.

Part (*b*) gives the values in terms of percentage of the corresponding values obtained for rail steel

(*a*)

Description of Process	Weight of Rail Lb. per Yd.	Tensile Strength 1000 Lb. per Sq. In.			Reduction of Area Per Cent			Impact Tension Ft.-Lb.			Endurance Limit 1000 Lb. per Sq. In.		
		Head	Web	Base	Head	Web	Base	Head	Web	Base	Head	Web	Base
Rail	112	131	127	133	20	21	22	170	167	167	60	59	62
	131	130	132	134	19	17	21	126	141	147	62	60	60
Gas Fusion *A*	112	97	57	62	6	7	4	14	6	24	30	35	33
Gas Fusion *B*	131	105	72	85	2	3	2	5	11	8	55	50	44
Thermit *C*	112	113	106	122	4	12	8	28	50	9	29	44	45
	131	113	101	100	4	8	2	37	52	15	34	46	44
Electric Flash *D*	131	126	131	133	4	6	14	160	85	112	54	44	54
Gas Pressure *E*	112 Rail*	135	136	139	18	19	21	134	145	141	61	62	62
	112 Weld	122	122	124	16	21	17	92	107	108	48	45	54

(*b*)

Description of Process	Weight of Rail Lb. per Yd.	Tensile Strength			Reduction of Area			Impact Tension Values			Endurance Limit		
		Head	Web	Base	Head	Web	Base	Head	Web	Base	Head	Web	Base
Rail	112	100	100	100	100	100	100	100	100	100	100	100	100
	131	100	100	100	100	100	100	100	100	100	100	100	100
Gas Fusion *A*	112	74	45	47	30	33	18	8	4	14	50	59	53
Gas Fusion *B*	131	81	55	63	11	18	10	4	8	5	89	83	73
Thermit *C*	112	86	84	92	20	57	36	16	30	5	48	75	73
	131	87	77	75	21	47	10	29	37	10	55	77	73
Electric Flash *D*	131	97	99	99	21	35	67	127	60	76	87	73	90
Gas Pressure *E**	112	90	90	89	89	110	81	69	74	77	79	73	87

*Rail for Process *E* not the same as for other processes. Percentage strength for Process *E* welds computed on basis of rail used for Process *E* joints.

tion of Fissures in Railroad Rails except that the stroke of the machine is 12 inches instead of 7 inches. Figure 8 (*a*) shows this machine with a welded specimen after fracture. (See *Bull.* 376 Am. Ry. Eng. Assn. June 1935.)

The specimen to be tested is placed as shown in Fig. 8 (*b*). The moment arm at the weld is 10 inches and at the weld the bending moment (inch-pounds) is ten times the wheel load.

The following routine has been used in the test: A specimen was put in the machine under a load of 75,000 lb. If the specimen broke before 2,000,000 cycles had been reached a second specimen was put in under a lower wheel load, usually a load of 60,000 lb. If this one broke, a specimen was put in at a still lower load, and the test continued to fracture or to 2,000,000 cycles of load.

The selection of 2,000,000 cycles as the limiting number of cycles for the rolling-load test was based on the following line of reasoning. During the passage of 300,000,000 tons of traffic, a location at any point in the rail would be subjected to approximately 30,000,000 wheel loads, allowing 5 tons as the average wheel load and remembering that the total tonnage is divided between the two rails. From the reports of the Investigation of Fissures in Railroad Rails it may be seen that, judged by records of about one-half a million wheel loads at four characteristic locations, about one wheel load out of one thousand has a magnitude of 40,000 lb. or greater. In rolling-load tests of rail, 40,000 lb. was the lowest wheel load which developed a fissure. Recent experiments at the University of Illinois by Dr. R. N. Arnold* indicate that, owing to inertia effects of the rails, this ratio of one in one thousand might be increased considerably, possibly to a point where one wheel load out of 300 may be expected to reach the value of 40,000 lb. or greater. This would mean that when 300,000,000 tons of traffic had passed over a location—a fair length of service for rail—about 100,000 wheel loads of 40,000 lb. or greater might be developed. Now in fatigue tests in the laboratory it is desirable to make tests under a much greater number of cycles of stress than will probably occur in practice. The reason for this is that above the fatigue limit a small increase in stress causes a great shortening of life. It is well then to make tests to ten times the number of cycles of stress expected in service. In these rolling-load tests the limiting number of cycles chosen was 2,000,000—twenty times the expected number of cycles of the load of 40,000, lb. which may be expected after 300,000,000 tons of traffic has passed over the track.

This maximum number of cycles, 2,000,000 is the same as that used by Professor W. M. Wilson in his studies in fatigue in structural members. The identity of these two figures for number of cycles is an interesting coincidence, nothing more.

11. *Stresses Developed in Rolling-Load Tests*

Failure of a rail or a welded rail joint under repeated wheel loads may arise from several causes. Failure may take place as the result of repeated flexural stresses and

* *Proc. (British) Inst. Mech. Engr's.*, **137**, p. 217 (1937).

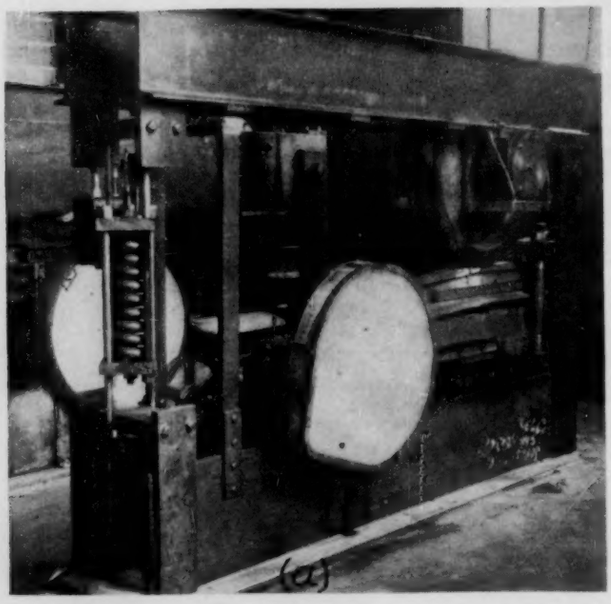
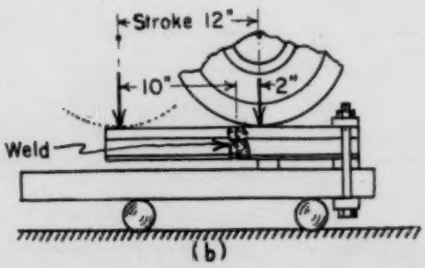


Fig. 8—"Rolling-Load" Testing Machine for Tests Under Repeated Wheel Load

(a) General view of machine. Stroke 12 inches, speed 60 r. p. m. Note fractured specimen



(b) Wheel positions on specimen at ends of stroke. During a stroke the bending moment at the weld ranges from zero to ten times the wheel load

in this case it is usually the tensile stress that causes failure. Failure may take place due to repeated shearing stress in the web of the rail and this depends upon wheel load rather than bending moment. Failure may take place by the development of internal fissures starting at minute imperfections and caused by the heavy stresses directly under the wheel load. All three of these causes of failure have occurred in the tests of welded joints so far carried out and Fig. 10 shows all three types of fracture.

The shearing stresses in the web and the flexural stresses in the head were computed by the common formulas of Mechanics of Materials. Their distribution is shown in Fig. 9 for a wheel load of 75,000 lb. on a 131-lb. rail and for a 112-lb. rail.

The specimen is so placed in the rolling-load machine that the head of the specimen is subjected directly to wheel load, and, during a cycle of load, to tensile stress. This is probably a somewhat more severe condition than is met with in rails in track where the compressive stresses are larger than the tensile stresses, although as the train moves over the track there occurs a *partial* reversal of stress in the rail. The maximum flexural stress in the rolling-load test occurs when the wheel is 10 inches from the center line of the welded joint. The maximum wheel load at the weld occurs when the wheel is directly over the joint and the flexural stress is therefore zero. The shearing stresses in the web near the weld remain approximately constant throughout 10 inches of the 12-inch stroke. This arrangement of stress was chosen, not with the idea of reproducing conditions in service precisely, but with the idea of producing measurable stresses of the same kind which occur in practice and studying their effect.

12. *Results of Rolling-Load Tests to Date*

Table 3 gives the results of rolling-load tests so far run. It will be noted that the two gas weld joints failed to withstand 2,000,000 cycles of load of even 40,000 pounds. The Thermit 112-lb. specimen under 40,000-lb. load did withstand 2,000,000 cycles, but the specimens under 50,000 lb., 60,000 lb. and 76,000 lb. did not. The test of the Thermit 112-lb. welded joint under 75,000-lb. wheel load is of interest in that it withstood more cycles of load than did the joint under 60,000 lb. This may be due to the fact that it had previously been subjected to 2,000,000 cycles of a 40,000-lb. load without fracture. This seems to indicate that welded joints may be strengthened by being subjected to many cycles of stress below the fatigue limit of the original material. This phenomenon of strengthening of metals by repeated understress has been known for several years, and repeated understress is just what rail joints receive in service—together with a rather rare overstress.

Three electric flash-welded joints have so far been put into the machine and all tested under 75,000-lb. wheel load. In the first one the excess metal was ground off over about as much of the perimeter of cross-section as is common in practice. The shape of metal left is shown at *A* in Fig. 9. This left a sharp re-entrant corner across the line of vertical shearing stress—a "stress raiser." The second specimen had the metal ground off as shown at *B* in Fig. 9, in an attempt to avoid this stress raiser. The first specimen failed by a crack starting at point *A*, and this crack is shown in Fig. 11 (*b*).

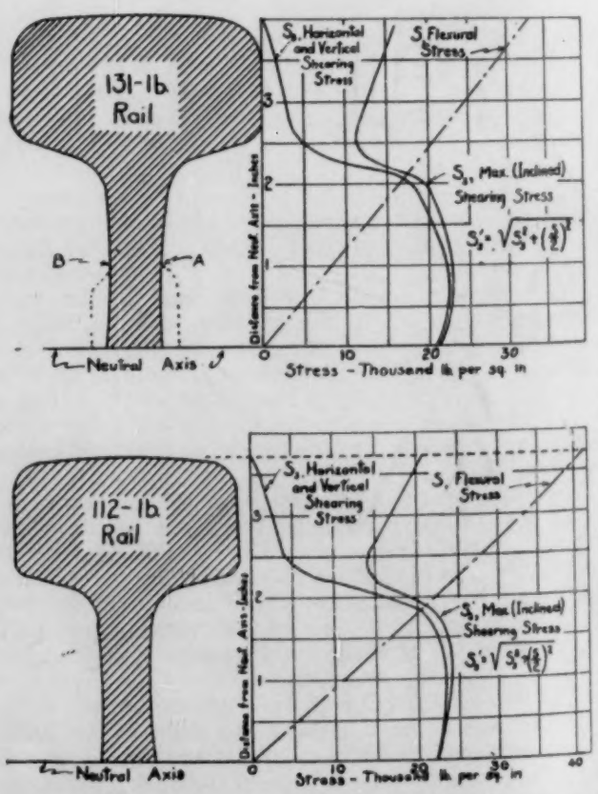


Fig. 9—Shearing and Flexural Stresses in Test Rail Joint Under 75,000-Lb. Wheel Load in Rolling-Load Testing Machine

Table 3—Results of Rolling-Load Fatigue Tests of Specimens of Welded Rail

Welding Process	Specimen No.	Weight of Rail Lb. per Yd.	Wheel Load Lb.	Maximum Flexural Stress* (S) Lb. per Sq. In.	Maximum Shearing Stress* (S_s) Lb. per Sq. In.	Cycles for Fracture	Manner of Failure and Remarks
Rail	NW-406	112	75,000	52,500	29,200	511,200	Fatigue crack corner of head, edge of cold-rolled path
Without	NW-410	112	65,000	43,300	25,400	1,650,900	Fatigue crack corner of head, edge of cold-rolled path
Weld	NW-408	112	60,000	40,000	23,500	2,038,800	Did NOT fracture
Gas Weld	W-17	112	75,000	41,600	24,400	67,900	Transverse fissure in head
Fusion	W-13	112	60,000	33,300	19,500	107,300	Transverse fissure in head
Process *A*	W-2	112	40,000	22,200	13,000	1,261,300	Compound fissure in head
Gas Weld	W-109	131	75,000	33,100	23,000	59,500	Horizontal fissure and failure at junction head and web
Fusion	W-110	131	60,000	26,500	18,400	190,800	Transverse fissure in head
Process *B*	W-106	131	40,000	17,700	12,300	532,800	Failure started in weld at lap at junction head and web
Thermit	W-256	112	75,000	41,600	24,400	307,400	Transverse fissure in head, previously tested for 2,000,000 cycles of 40,000-lb. wheel load without fracture
Weld	W-261	112	60,000	33,300	19,500	275,000	Fatigue crack corner of head, edge of cold-rolled path
	W-257	112	50,000	27,800	16,200	1,004,100	Fissure near tread, apparently burnt metal
	W-256	112	40,000	22,200	13,000	2,002,900	Did NOT fracture
Process *C*	W-205	131	75,000	33,100	23,000	582,900	Transverse fissure in head
	W-211	131	60,000	26,500	18,400	1,484,700	Transverse fissure in head and also failure at junction head and web
	W-208	131	55,000	24,300	16,900	1,378,100	Failed in web at "collar" of weld
Electric	W-308	131	75,000	33,100	23,000	1,769,400	Failed in web where metal had been ground off so as to form a re-entrant angle. See *a* Fig. 10 and (*b*) Fig. 12
Flash Pressure	W-305	131	75,000	33,100	23,000	942,300	Fatigue failure in web outside of weld, starting at a stamped letter on web. See Fig. 12 (*a*)
Process *D*	W-309	131	75,000	33,100	23,000	2,001,600	Did NOT fracture
Gas	W-403	112	75,000	41,600	24,400	650,600	Fatigue crack at corner of head, edge of cold-rolled path, also crack started at stamped letter on web
Pressure Weld	W-402	112	60,000	33,300	19,500	1,269,900	Fatigue crack at side of head
Process *E*	W-401	112	55,000	30,600	17,900	2,046,500	Did NOT fracture

*For specimens with welds Fig. 10 shows variation from tread of rail to neutral axis of: (1) flexural stress, (2) horizontal and vertical shearing stress and (3) maximum shearing stress (inclined). For specimens of rail without weld the values of S for specimens with weld should be multiplied by 1.2, since the maximum moment arm to the center of the weld is 10 inches while the maximum moment arm for specimen without weld is 12 inches.

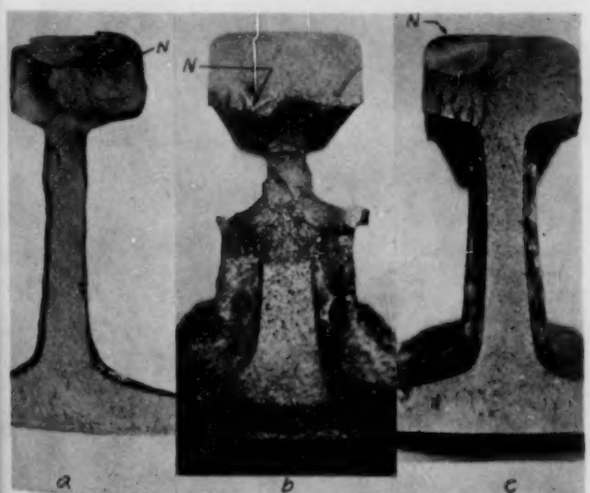


Fig. 10—Typical Fractures of Welded Joint Specimens in Rolling-Load Test

(*a*) Transverse fissure in head. Fissure started at *N*, and fracture occurred after 1,216,300 cycles of a 40,000-lb. wheel load
(*b*) Fissure started at *N* near junction of web and head, just outside the weld. Fracture occurred after 1,378,100 cycles of a 55,000-lb. wheel load
(*c*) "Fatigue" failure by spreading crack starting at *N* near corner of tread of rail. Crack started in coarse transverse grinding marks left on head of rail. Failure occurred after 650,600 cycles of a 75,000-lb. wheel load

The second specimen failed after 650,600 cycles of a 75,000-lb. wheel load. The failure was not through the weld, not in the heat-affected metal, but it started from a stamped letter on the web of the rail (see Fig. 11 (*a*)).

The third specimen, which had no stamped letter on the web of the test specimen and in which the excess weld metal had been ground off so as to avoid a sharp-re-entrant corner, ran out for 2,000,000 cycles of a 75,000-lb. wheel load without failure.

Three specimens of welded joints in 112-lb. rail have been tested in the rolling-load machine in which specimens the weld was a pressure butt weld with the heat furnished by gas (Process *E*). In these welds the temperature used was less than in the fusion processes *A* and *B*. Under 75,000-lb wheel load 650,600 cycles were required to produce fracture. The fracture started at the edge of the path cold rolled on the tread by the wheel, but at the same time another spreading fracture had started at a stamped letter on the web. The second specimen fractured after 1,266,000 cycles of a 60,000-lb. wheel load. The fracture started in the fillet between head and web. The third specimen withstood without fracture 2,046,500 cycles of a 55,000-lb. wheel load.

13. *Endurance Limits for Welded Joint Specimens*

As has been previously noted, it seems suitable to

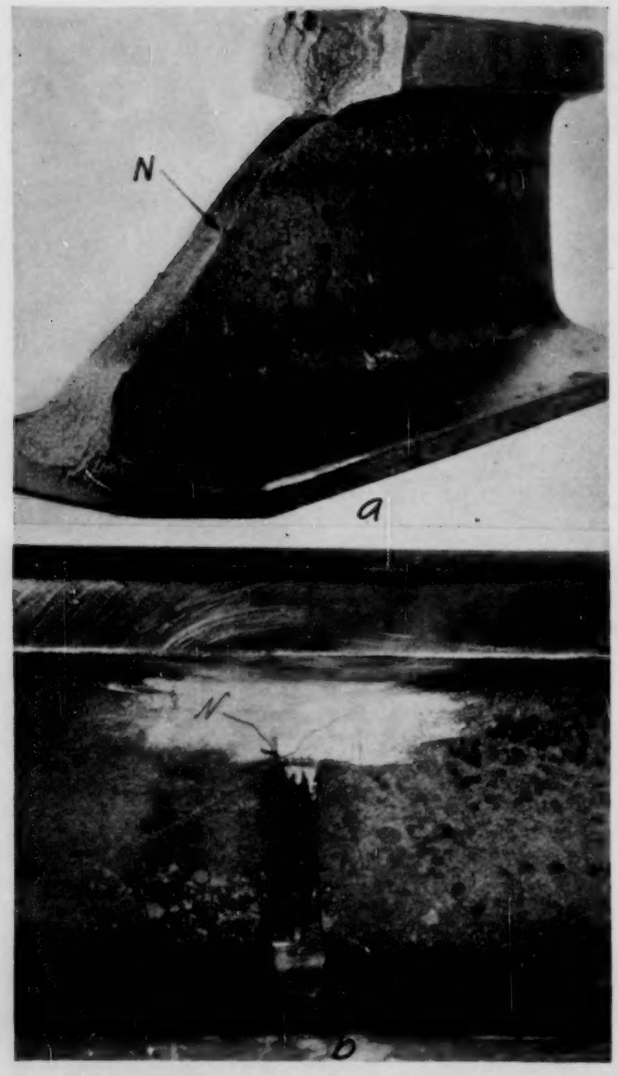


Fig. 11—Welded Joint Fractures Starting at "Stress Raisers" in the Web

(a) Fracture started at *N* on a line tangent to the curved part of the letter *C* which was stamped on the web of the rail. Fracture was outside the weld zone

(b) Fracture started at *N* where there was a sharp re-entrant angle (shown at *A* in upper half of Fig. 9) left after grinding off excess weld metal. After this rail failed the excess metal was ground off as shown at *B* in Fig. 9, avoiding a sharp re-entrant angle

determine endurance limits of welded rail joints for 2,000,000 cycles of wheel load. Figure 12 shows how this limit was determined for specimens of 112-lb. rail without welds, for 112-lb. rail with Thermit welds, and for specimens of 131-lb. rail gas-welded by Process *B*.

The endurance limits for the other types of weld were determined in a similar manner. The results of three or more tests of each type of joint were plotted on load-cycle diagrams, in which wheel load was plotted as ordinates and number of cycles of load were plotted (to a logarithmic scale) as abscissas. For each type of joint the straight line which seemed best to fit the three or more test results was drawn, remembering that for a test in which 2,000,000 cycles of stress were withstood without fracture the plotted point cannot be *above* the load-cycle line. If none of the tests showed a "life" of 2,000,000 cycles the straight line for the three tests was extended until it intersected the ordinate for 2,000,000 cycles. This is shown in Fig. 12 for the tests of 131-lb. rail welded by Process *B*. Where one of the specimens ran to 2,000,000 cycles or over without fracture the straight line was drawn through the points for the two specimens which did fail. This is shown in Fig. 12 for the 112-lb. rail without weld and 112-lb. rail specimens welded by Process *C*. These endurance limits are distinctly more uncertain than the endurance limits of small polished specimens tested at high speeds, and determined from six or more specimens. However, the differences which they show are believed to be significant. Table 4 shows the tentative endurance limits determined in this manner.

Table 4—Tentative Endurance Limits of Welded Joints for 2,000,000 Cycles of Wheel Load

Wheel load at endurance limit is estimated as shown in Fig. 13

Type of Weld	Weight of Rail Lb. per Yd.	At Endurance Limit: Wheel Load Lb.	Maximum Flexural Stress Lb. per Sq. In.	Maximum Shearing Stress Lb. per Sq. In.	Approximate Ratio of Fatigue Strength of Weld to That of Rail
Rail Without Weld	112	63,000	42,000	25,600	1.00
Rail Without Weld	131	79,000*	42,000*	24,200	1.00
Pressure Butt Weld (electric) Process *D*	131	more than 75,000	more than 33,100	more than 19,500	more than 0.79
Pressure Butt Weld (gas) Process *E*	112	55,000	30,600	17,900	0.73†
Thermit Weld Process *C*	112	46,000	25,400	15,100	0.61†
	131	52,000	23,000	16,000	0.66‡
Fusion Weld (gas) Process *A*	112	32,000	17,800	10,400	0.51**
Fusion Weld (gas) Process *B*	131	22,000	9,700	6,700	0.28**

*Estimated on assumption that 131-lb. rail will develop as high flexural stress at endurance limit as 112-lb. rail. Maximum load which can be applied by machine 75,000 lb.

†Based on flexural stress at endurance limit. Failures by fatigue crack from surface.

‡Based on shearing stress at endurance limit. Fatigue failures were in web.

**Based on wheel load. Failures started from internal fissures.

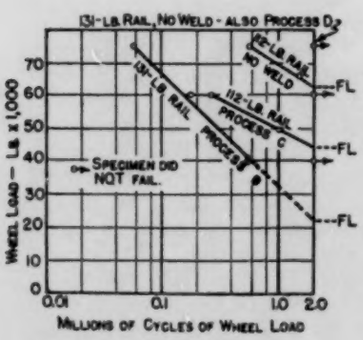


Fig. 12—Typical Load-Cycle Diagrams for Estimating Endurance Limit for 2,000,000 Cycles of Wheel Load

FL denotes Endurance Limit

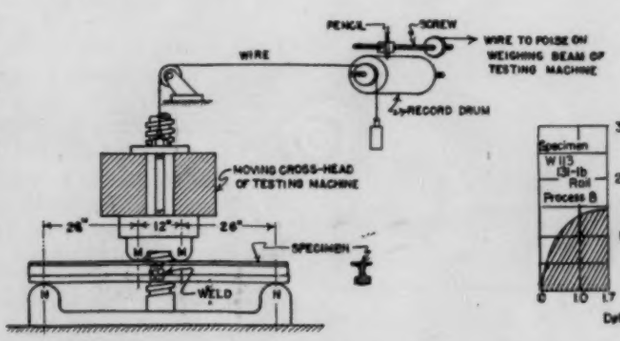


Fig. 13—Bend-Test Rig with Specimen

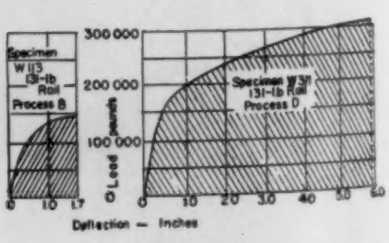


Fig. 14—Typical Load-Deflection Diagrams for Bend Tests

Table 5—Results of Drop Tests of Welded Rail Specimens

Standard M. C. B. testing machine and recommended A. R. E. A. testing procedure used. Specimens tested head-up

Specimen No.	Weight of Rail Lb. per Yd.	Welding Process	Height of Drop of 1-Ton Tup. Ft.	Number of Blows for Fracture	Elongation in Each Inch of Gage Length on Tension Side. In.
W 3	112	*A*	20	1	.....
W 23	131	*A*	22	1	0-1-3*-2-0-0
W 124	112	*B*	20	1	0-0-1*-0-0-0
W 102	131	*B*	22	1	0-1-3*-0-0-0
W 259	112	*C*	20	1	.....
W 210	131	*C*	22	1	.....
W 304	131	*D*	22	2	3-3-7*-3-4-3
W 4	112	*E*	20	1	3-4-2*-0-4-4

*Measurement across fracture.

In determining the probable endurance limit for 131-lb. rail without weld it is evidently more than 39,600 lb. per sq. in. maximum flexural stress, since in the tests of the Process *D* welds the rail itself adjacent to the weld and directly over the supporting block was subjected to 1.2 times the stress in the center of the weld, which was 33,100 lb. per sq. in. and failure occurred neither in the weld nor in the rail. It was then assumed that the 131-lb. rail did develop a maximum flexural stress as high as 112-lb. rail, that is, 42,000 lb. per sq. in. and the wheel load and maximum shearing stress accompanying this flexural stress were computed. The right-hand column in Table 4 gives the approximate ratios of the fatigue strengths of the welds to that of the rail which were developed in these tests.

Again it is noted that welded rails should *not* be judged by the results of rolling-load tests alone. Lack of homogeneity shown by etch tests and microscopic examination, variation of strength, ductility and toughness qualities as shown by the tests of specimens cut from the joints—these factors must be given consideration. It must also be remembered that all the welded joints tested in the rolling-load machine were made with the knowledge that they were test joints, and this might make them either better or poorer than the average joint welded under service conditions. It is inevitable that joints made by the firm regularly doing rail welding and known to be used for tests of their product would be made with somewhat more care and closer inspection than is the case in routine welding. This is in no sense a reflection on the honesty of the producers, it is merely the inevitable increase of carefulness which is present when a man knows he, or the product of his firm, is under examination. Now this very increase of carefulness may defeat its own object. Conditions accompanying the making of test joints are somewhat different from those accompanying the making of service welds, and the very additional care used, and the slowing down of the welding process which often happens may tend to make the test joints poorer than the average joint turned out.

In all consideration of welds the occasional occurrence of a poor weld must be recognized. There is at present no reliable non-destructive test for poor welds, and there are very few, if any, data on the probable frequency of occurrence of poor welds in rail joints. These considerations, together with considerations of the relative seriousness of failure of joint bars and of welded joints in rails must be factors in deciding whether or not to use welded rail joints in track.

V. BEND TESTS AND DROP TESTS OF WELDED JOINTS

By N. J. Alleman and H. F. Moore

14. *A Comparison of Bend Tests and Droop Tests for Rails*

The standard drop test for rails consists of striking the rail, which is mounted over a span of 3 or 4 feet on a spring-supported anvil, with a falling weight, or "tup." The tup weighs 2000 lb. and falls from a distance of from 18 to 22 feet depending on the weight per yard of the rail. It strikes the test specimen at mid-span. This is a "pass or fail" test and gives very little information about the strength of the rail. Force is not measured at all and, if repeated blows are used until the rail breaks, only the energy required to fracture the rail and to compress the springs is indicated, and that only in "steps" of about 40,000 foot-pounds. Moreover, the distribution of energy between rail and springs is variable for different sections of rail, and for rail steels of different elastic strength.

In a cross-bending test of a rail, if a load-deflection diagram is plotted up to fracture the area under that diagram is a measure of the energy required to fracture the rail. A number of investigations* have shown that up to a certain critical speed,—a speed higher than that acquired by a weight falling 22 feet—the energy absorbed is about the same for a given specimen of a given steel whether the load be applied slowly or rapidly. Moreover, in a bend test strength values are determined as well as energy values.

* For these references and a fuller discussion of the relative advantages of the bend test, see the Fifth Progress Report of the Rails Investigation. *Bull.* 411 Am. Ry. Eng. Assn.

Table 6—Results of Bend Tests of Welded Rail Specimens

Specimens tested head-down

Specimen No.	Weight of Rail Lb. per Yd.	Welding Process	Maximum Load Before Fracture Lb.	Energy for Fracture Ft.-Lb.	Deflection at Fracture In.	Elongation in Each Inch of Gage Length on Tension Side In.	Remarks
Rail†	112	None	225,000	75,000	4.0	.....	
Rail†	131	None	300,000	100,000	5.0	.....	
W 4	112	*A*	124,200	4,200	0.71	0-0-0-2*-0-0	Lap side of head
W 22	131	*A*	168,500	7,360	0.53	0-0-0-1*-0-0	Lap side of head
W 113	112	*B*	140,500	6,900	0.92	0-0-0-0-0-0	Lap web and base
W 105	131	*B*	79,400	867	0.21	0-0-0-0-0-0	Lap at junction, head and web
W 253	112	*C*	193,900	23,370	1.33	1-1-1-1*-1-2	
W 203	131	*C*	231,500	22,200	1.65	1-1-1-2*-1-1	
W 311	131	*D*	315,100	121,500	5.98	7-6-10*-9-7-7	
W 404	112	*E*	217,600	42,600	3.16	.....	
W 405	112	*E*	239,200	72,100	4.70	3*-3-4-6-6-6	

*Measurement across fracture.

†Values for rails without welds taken from records of bend tests made by the Rails Investigation. Minimum values for crack-free rails recorded here.

15. *Drop Test Results*

Both drop tests and bend tests have been made on specimens of welded rail. The drop tests were the standard drop tests for rails. The rails were tested "head up" (base in tension), and test results for rails welded by Processes *A*, *B*, *C* and *D* are given in Table 5. It will be noted that only the joints welded by Process *D* passed the requirement of one blow without fracture.

16. *Bend Test Apparatus and Test Results*

Figure 13 shows in diagram the testing apparatus and the specimens used for the bend tests of welded joints. It is believed that this figure is self explanatory. Figure 14 shows (reduced) two typical load-deflection diagrams. The ordinates of these diagrams measure strength values, the abscissas measure ductility values, and the area under the entire diagram measures energy absorption up to fracture which is here designated as "toughness."

Table 6 gives the results of the bend tests so far performed on welded rail joints. All specimens were tested "head down" (head in tension). The specimens for Process *D* welds gave results, both for bend tests and for drop tests; which would be regarded as satisfactory for rail without welds.

Efficient Utilization of Aircraft Tubing*

By J. MÜLLER†

CHROMIUM-MOLYBDENUM aircraft tubing is used for several structural parts of airplanes, for example, nacelles. Unless the chromium-molybdenum tubing can be used in the heat-treated condition, it has no advantage over plain-carbon steel tubing. However, the heat-affected zone in the vicinity of welded joints in heat-treated chromium-molybdenum steel tubing is soft and reduces the strength of the tubing nearly to that of plain-carbon steel. Since heat treatment of the welded structure is not feasible, a method has been patented in Germany whereby each section of tubing is welded to a short but thicker reinforcing end tube before heat treatment. The welding of the reinforced section after heat treatment localizes the soft, heat-affected zone in the thick reinforced end tube. The welded, tubular assemblage thus utilizes the full strength of the thin, heat-treated tubing.

The method of reinforcing the vicinity of welded joints is shown in Fig 1. The short, thick-walled end section is welded to the tubing before heat treatment. The wall thickness of the end section is such that the end section may be designed on the basis of annealed steel without reducing the strength of the structure. Under static tensile and simple compressive stress (fatigue tests were not made) it is obvious that the reinforced tubing meets requirements. The column action of the reinforced tubing could not be predicted but was found by test to be satisfactory.

*Abstract of "Erhöhung der spezifischen Belastbarkeit bei Zug, Druck, und Knickung von eingeschweissten Stahlrohr-Fachwerkstreben im Flugzeugbau durch Massnahmen werkstofftechnischer und konstruktiver Art" published in Luftfahrtforschung, 16 (1) 14–17, Jan. 10, 1939. The author reports tests made at Focke-Wulf Aircraft Co., Bremen, Germany.

†Focke-Wulf-Flugzeugbau G. m. b. H., Bremen-Flughafen, Germany.

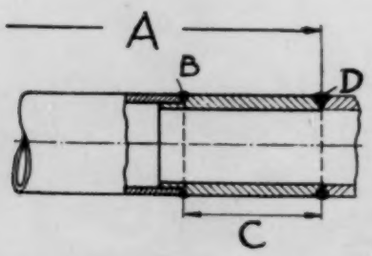


Fig. 1—Reinforced End of Heat-Treated Chromium-Molybdenum Steel Aircraft Tubing

A—Heat-treated strut
B—Heat-treated weld
C—Minimum length = heat-affected zone
D—Joint to structure welded after heat treatment

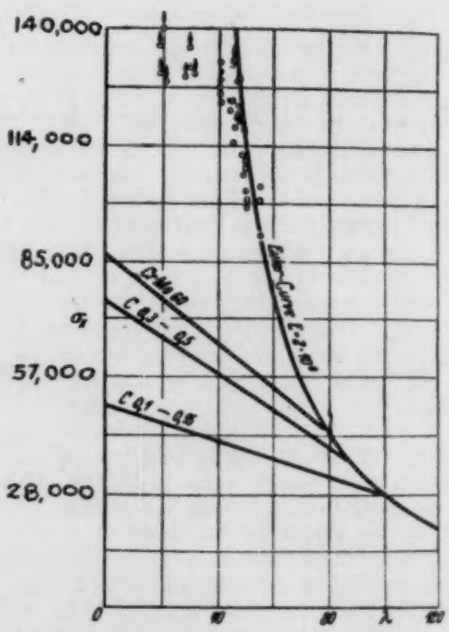


Fig. 2—Column Tests on Welded Aircraft Tubing

Curve 1. Euler Curve, $E = 2 \times 10^6$
Curve 2. Unreinforced, heat-treated chromium-molybdenum steel tubing with as-welded butt welds 0.39 inch from each end
Curve 3. Plain carbon steel tubing, 0.3–0.5 C. Tests by A. Rechtlich, 1930
Curve 4. Plain carbon steel tubing, 0.10–0.15 C. Tests by A. Rechtlich, 1930
Circles—Results for chromium-molybdenum steel tubing welded by the patented method. Arrow signifies specimen reached the capacity of the testing machine without failure
σ_K = Buckling stress, lb./in.²
$\lambda = l/r$.

The results of column tests on plain-carbon and chromium-molybdenum aircraft tubing, Fig. 2, show the superiority of the reinforced tubing over unreinforced. The testing machine was fitted with knife-edge compression plates so that the degree of fixity of the ends of the columns was zero. Dial gages were used to measure buckling. The results for unreinforced tubes were obtained on tubes 0.55–1.6 inches diameter, 0.030–0.12 inch wall. The reinforced tubes were 1.1 x 0.039 or 1.4 x 0.030 inches. The dimensions of the thick end sections were not stated but the welding was performed with chromium-molybdenum steel rod. The tensile strength of the heat-treated tubing was 150,000–170,000 lb./in.² The results show that the column strength of welded tubing made by the new method is superior to that of unreinforced tubing up to l/r ratios of 75, which are those most commonly used in aircraft.

FATIGUE STRENGTH OF Oxyacetylene Welded Boiler Plate*

By K. H. BUSSMANN†

AN INVESTIGATION was made of the effect of hot peening, stress annealing and normalizing on the pulsating tension fatigue strength (10 million cycles criterion) of low-carbon boiler plate (composition not stated) 0.59 and 1.18 inches thick. The results, Table 1, show that:

1. Aside from machining the weld, hot peening is the most effective in raising the fatigue strength;
2. Normalizing at 920° C. may or may not benefit the fatigue strength;
3. Stress annealing at 650° C. was not beneficial under the test conditions that prevailed.

Five specimens were used for each determination of fatigue strength. The welded specimens 1.18 inches thick and all unwelded specimens were reduced in breadth, Fig. 1; the welded specimens 0.59 inch thick were not reduced in breadth. Mohr and Federhaff or Losenhausen pulsators (375 or 500 cycles/min.) were used. Tests were not continued beyond 4 million cycles maximum; the fatigue strength at 10 million cycles was determined by obvious extrapolation of *S*-log *N* curves.

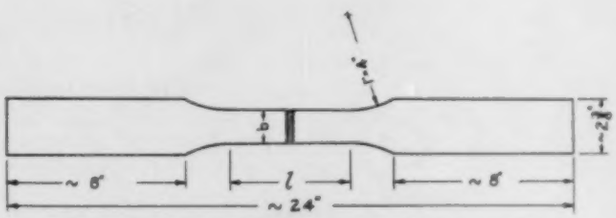


Fig. 1—Dimensions of the Specimens with Reduced Section
b = 1.37 inches *l* = about 5 inches

The welds were of good quality. Consequently, there was little scatter of experimental results from the *S*-log *N* curves.

No explanation was offered for the low fatigue strength of stress annealed welds. The higher fatigue strength of the thicker stress annealed welds may have been related to more favorable cooling rate or to a slightly different stress annealing temperature from the thinner welds.

The low fatigue strength of the normalized welds 0.59 inch thick was attributed to irregularities in the specimens, which caused considerable scatter of the experimental points about the *S*-long *N* curves. The high fatigue strength of the machined and normalized welds substantiates the probability of irregularities in the unmachined specimens.

Hot peening raised the pulsating tension fatigue strength of unmachined welds about 25%.

---

* Abstract of "Der Einfluss verschiedenartiger Nachbehandlung auf die Dauerzugfestigkeit Gas-schmelzgeschweisster Kesselbleche," published in *Wissenschaftliche Abhandlungen der Deutschen Materialprüfungsanstalten*, Folge 1, H. 2, 59–64 (1939).

† The author is a member of the staff of the German National Materials Testing Bureau, Berlin-Dahlem.

Table 1—Results of Pulsating Tension Fatigue Tests on Oxyacetylene Welded Boiler Plate

Type of Weld	Thickness of Plate, Inch	Treatment After Welding	Fatigue Strength,‡ *S*, Lb./In.²	Notch Sensitivity Factor *B*†	Difference from As-Welded,* %
Unwelded	0.59	None	±16,400	...	..
V weld	0.59	None	± 7,800	2.1	..
V weld, reverse welded	0.59	None	± 9,500	about 1.7	..
Unwelded	1.18	None	±15,400	...	..
X weld with high-grade rod	1.18	None	±10,200	1.5	..
V weld, reverse welded	0.59	Weld was hot peened, not machined	±11,700	1.4	+22
X weld with high-grade rod	1.18	Weld was hot peened, not machined	±13,100	about 1.2	+28
V weld, reverse welded	0.59	Heated ½ hr. at 650° C., cooled in furnace, not machined	± 7,800	2.1	−18
X weld with high-grade rod	1.18	Heated ½ hr. at 650° C., cooled in furnace, not machined	±10,000	about 1.5	− 2
V weld, reverse welded	0.59	Normalized ½ hr. at 920° C., not machined	± 7,800	2.1	−18
X weld with high-grade rod	1.18	Normalized ½ hr. at 920° C., not machined	±10,900	1.4	+ 7
V weld, reverse welded	0.59	Normalized ½ hr. at 920° C., ground flush in direction of axis	±13,500	about 1.2	+42
X weld, with high-grade rod	1.18	Normalized ½ hr. at 920° C., ground flush in direction of axis	±11,900	about 1.3	+17

* By as-welded is meant reverse welded or X welded.

† $B = \frac{S_{unwelded}}{S_{welded}}$

‡ $S = \pm \frac{S_o - S_u}{2}$ where S_o = upper stress, S_u = lower stress (2800 lb./in.² in all tests).

THE EFFECTS OF CARBON ON THE Welding of Plain Carbon Steels

A Review of the Literature to July 1, 1937

By W. SPRARAGEN* and G. E. CLAUSSEN†

This report is prepared under the auspices of

THE LITERATURE DIVISION OF THE ENGINEERING FOUNDATION
WELDING RESEARCH COMMITTEE

TABLE OF CONTENTS

	Page
Summary	272-s
Introduction	273-s
Mechanical Properties	273-s
Microstructure	280-s
Carbon Limit	281-s
Recovery	281-s
Oxyacetylene Welding	281-s
Bare Electrodes	282-s
Coated Electrodes	283-s
Atomic Hydrogen Welding	283-s
Carbon Arc Welding	283-s
Fusion Welding Procedure	283-s
Resistance Welding	285-s
Forge Welding	286-s
Oxygen Cutting	287-s
Acknowledgments	287-s
Bibliography	287-s
Suggested Research Problems	288-s

NOTE: The plain carbon steels which are the subject of the review are exclusively wrought, not cast, products. The literature on welding cast steels was summarized in a review on Welding Cast Steels (AMERICAN WELDING SOCIETY JOURNAL, **16** (4) Suppl., 2–11 (1937)).

Summary

MECHANICAL PROPERTIES

Hardness

Test results establish carbon content as a powerful factor in increasing the hardness of the heat-affected zone as welding speed increases. Adopting 100 Vickers units as the allowable maximum increase in hardness, the allowable welding speed decreased, and the allowable minimum heat generated by the arc correspondingly increased as the carbon content was raised to 0.53%. The maximum hardness was found in a zone not over 0.10 inch wide. Stress relief (1/2 hr. at 620° C., furnace cool) reduced the maximum Vickers hardness of S. A. E. 1035, 18 inches/min., to 200 from 250. Hardness surveys of single beads deposited on a steel rail by organic coated mild steel, all-position electrodes indicated that the hardness of the heat-affected zone decreased as the preheating temperature was raised. In a series of tests on plain carbon steels (0.08 to 0.70 C), the hardness of the bead welded specimens rose with increase in carbon content, but was lower for specimens with three beads than for single-bead specimens.

Tensile tests of the heat-affected zone in a series of plain carbon steels indicated that the strength of the metal in the heat-affected zone was higher than unwelded base metal, but the ductility was lower when the carbon content was 0.50% or greater (steels from 0.08 to 0.70 C). Of course, the tensile strength increased with the carbon content. Depending on the electrodes used, one investigator found that for plates from 1/4 to 1/2 inch thick satisfactory butt welds can be made in plain carbon steel up to about 0.56 C. Fillet welds made with heavy covered electrodes cracked above 0.38 C, the cracking not being observed with electrodes that generated less heat.

Impact

In a series of tests on plain carbon steels the notch-impact value of the base metal decreased rapidly as the carbon content increased. The heat-affected zone had higher notch-impact value than base metal. Three layers were better than one. Annealing had a beneficial effect on the notch-impact value of high carbon arc welds (0.51 to 0.83 C).

MICROSTRUCTURE

As the carbon content of the base metal is increased, there is an increasing tendency to form martensite in the heat-affected zone, since carbon reduces the critical cooling rate of the steel.

CARBON LIMIT

In low-alloy steels 0.15 C is considered the upper limit for fool-proof steel. The German Railways restrict the carbon content of steel for welded bridges to 0.20% max. The Bridge Specification of the AMERICAN WELDING SOCIETY limits the carbon content to 0.25 max. with Mn 1.0 max.

Bend tests by one investigator on a series of plain carbon steels led him to conclude that steels with 0.06–0.12 C were very good for welding, steels with 0.12–0.30 C

* Secretary, Welding Research Committee.
† Research Assistant, Welding Research Committee.

were good, and steels with 0.30–0.70 C could be welded if occasion demanded.

A survey of specifications for steel used in fusion welded pressure vessels showed that the carbon ranges from 0.05–0.15% in France, and 0.15–0.35% in the United States (A. S. M. E. Power Boiler Construction Code). The U. S. Navy Bureau of Engineering has issued a proposed specification for fusion welding of drums of power boilers in which the composition of the base metal is limited to not over 0.30 C, 0.30–0.60 Mn.

RECOVERY

Oxyacetylene Welding

The recovery of carbon in oxyacetylene welding depends primarily on flame adjustment, the "neutral" mixture containing more acetylene with high-carbon rods than with low carbon. The addition of 1.5 to 2.5 Si to the rod reduces carbon loss to nearly zero. With high-carbon rods the recovery is about 60% in oxyacetylene welding.

Bare Electrodes

In general the recovery of carbon from bare plain carbon steel electrodes is about 25% at 0.1–0.2 C, rising to about 50% at 0.5–1.0 C.

Aluminum-deoxidized wire aids in carbon recovery, as does making the electrode positive, and using short arcs, and larger diameter electrodes.

Coated Electrodes

The recovery of carbon from the rod with coated electrodes is usually higher than with bare electrodes. However, the nature of the coating is of controlling importance.

Atomic Hydrogen Welding

In order to produce the same carbon content in weld as base metal, atomic hydrogen filler rods for high-carbon steels should contain 20 to 30% more carbon than base metal.

FUSION WELDING PROCEDURE

In arc welding plain carbon steels with 0.40–0.60 C preheating (180–260° C.) is recommended as is also stress relieving, or heat treating (790° C.). Steels with 0.60–0.80 C should be preheated to at least 260° C. and heat treated at 730–790° C. after welding. Puddling is advantageous. Preheating is also recommended in the use of the atomic hydrogen process for steels containing 0.65 C. The repair of cracked shoe dies (0.9 C steel) by oxyacetylene welding involves veeing the crack and using a high-carbon steel rod $^1/_8$ inch diameter and neutral flame. The flame should be as small as possible. The welded die is heat treated as if it were unwelded.

RESISTANCE WELDING

Flash welds in plain carbon steels have practically the same tensile strength as the base metal up to 0.70 C. The ratio of endurance limit to tensile strength for welded specimens decreased from 0.5 for the low-carbon (0.08 C) specimens to 0.3 for the high-carbon (0.86 C) specimens. However, the ratio of endurance limit of the welded to the unwelded specimens for this range of carbon content was about 0.75%. The effect of heat treatment on flash and resistance butt welds in a series of plain carbon steels was found to be beneficial in raising notch-impact values. Furnace annealing raised the strength (based on area of sheared spot) of spot welds in 0.83 C steel to 75,000 lb./in.^{2}

FORGE WELDING

Carbon content is an important factor in forge and water gas welding. In Sweden, for example, the carbon content of steel for water gas welded boiler drums is limited to 0.12–0.15%.

OXYGEN CUTTING

The preheating temperature for steel containing over 0.30 C should be 260–320° C. (500–600° F.) but not usually above 600° C. (1100° F.). Preheating prevents checking or cracking and saves gases. If the preheating temperature is too high the cut is difficult to complete. An increase in carbon content decreases the oxygen cutting speed. The oxygen pressure at the regulator in lance cutting should be 25 lb./in.^{2} higher for low-carbon steel than for medium-carbon steel up to 0.5 C.

Effects of Carbon on the Welding of Plain Carbon Steels

INTRODUCTION

THE effect of carbon content on the welding of plain carbon steels is a subject that has attracted the attention of many investigators, whose conclusions are summarized in the present review. Most of the investigators have used a wide selection of steels, and, considered as a whole, the experimental conditions have been sufficiently varied to offer the engineer a fairly sound basis for gaging the expediency of adopting special measures in welding plain carbon steels. The effect of carbon content on the welding of alloy steels has been dealt with in the series of reviews on alloying elements. A thorough discussion of the effect of carbon on iron is given in the Alloys of Iron and Carbon (2 vols., McGraw-Hill Book Co.). A brief statement of the effects of carbon from the welding standpoint is given in Welding Handbook, pp. 426, 444–450 and 464–478 (1938 Edn.).

MECHANICAL PROPERTIES

Available information on the mechanical properties of welds in unalloyed steel is of three types:

1. Tests of the hardness and notch-impact value of the heat-affected zone created by a bead of weld metal (composition immaterial) deposited under known conditions on a series of plain carbon steels having a wide range of carbon contents;
2. Tests of welds in a series of plain carbon steels

having a wide range of carbon contents made with low-carbon steel filler rods;

3. Individual determinations of different degrees of completeness on one or two plain carbon steels.

At present, Type 1 information appears to be least complicated by extraneous factors. Tests of the second type are confused by the unknown extent of dilution of low carbon or alloy weld metal by higher carbon base metal. In the present review little space can be allotted to information of Type 3, except for high carbon steels. The wealth of information on the properties of welds in plain low-carbon steels has been reserved for reviews involving the separate properties, since summary of the results shows that in the tests carbon content was but one of many determining factors, such as process, thickness and cleanliness of base metal.

Heat effect in arc welding plain carbon steels has been studied most extensively by Theisinger,[1] who deposited mild steel bare electrodes (composition not stated) $^5/_{32}$ inch diameter (375 amp. A.C., 23 volts) down the center of the length of hot rolled strips $^1/_2 \times 2 \times 9$ inches. The steels met S. A. E. specifications, but only the carbon content (0.17, 0.25, 0.33, 0.44 and 0.53 C) was determined. The bead was deposited at speeds of 4, 6, 8, 12 and 18 inches per minute. The hardness was explored by means of a Vickers machine at a section 2 to 3 inches from the crater, an average of 80 determinations being made on each specimen. In the fusion zone the impressions were spaced 0.02 inch apart; elsewhere the spacing was 0.05 or 0.10 inch. In general the maximum hardness was found near the base of the bead in the heat-affected base metal. Oxides and nitrides were not found in the microstructure of the heat-affected zone.

The results, Fig. 1, establish carbon content as a powerful factor in increasing the hardness of the heat-affected zone as welding speed increases. Adopting 100 Vickers units as the allowable maximum increase in hardness, Theisinger found, Table 1, that the allowable welding speed decreased and the allowable minimum heat generated by the arc correspondingly increased as the carbon content was raised to 0.53%. Regardless of composition, the most uniform distribution of hardness between weld metal, heat-affected zone, and base metal was attained at a speed of 8 inches/min. (64,700 Joules/inch). The maximum hardness was found in a zone not over 0.10 inch wide.

The microstructure (150 ×) at the higher welding speeds, according to Theisinger, was sorbitic in the specimens up to 0.33 C. The heat-affected zone of the 0.44 C steel was sorbitic at 6 inches/min., sorbito-troostitic at 8, mainly troostite at 12, and troostite with patches of martensite at 18 inches/min. With 0.53 C the structure was mainly sorbite at 4 inches/min., troostite at 12 and troostite with large patches of martensite at 18 inches/min. Stress relief ($^1/_2$ hr. at 620° C., furnace cool) reduced the maximum Vickers hardness of S. A. E. 1035, 18 inches/min., to 200 from 250.

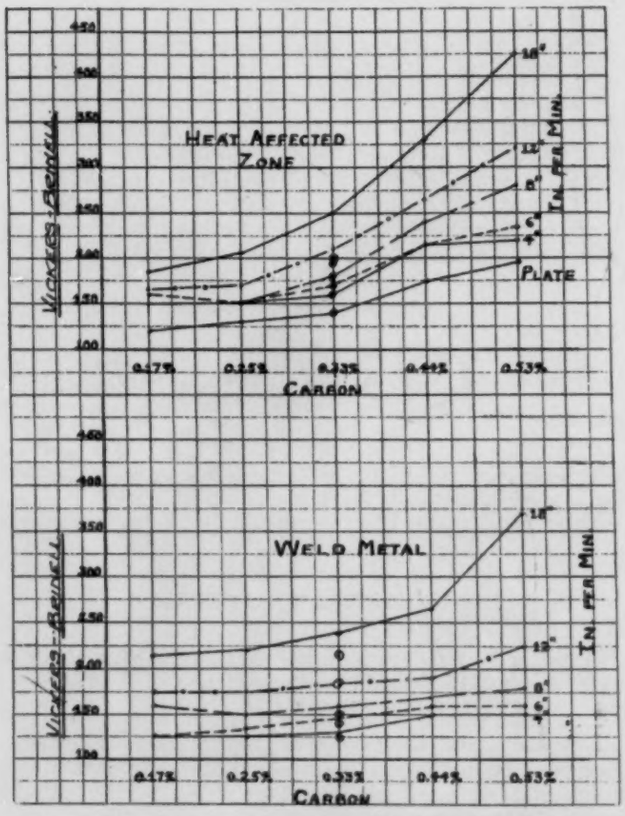


Fig. 1—Hardness of Carbon Steel Plates $^1/_2$ Inch Thick with Single Bead Deposited on Surface by Automatic Welding. Theisinger[1]

Table 1—Maximum Welding Speeds and Corresponding Minimum Energy of Arc with a Criterion of 100 Vickers Units Allowable Increase in Hardness Under Given Experimental Conditions. Theisinger[1]

Grade of Steel	Maximum Allowable Speed of Travel of Arc, Inches/Min.	Minimum Arc Energy,* Joules/Inch
S. A. E. 1015 (0.17 C)	>18	<28,750
S. A. E. 1025 (0.25 C)	>18	<28,750
S. A. E. 1035 (0.33 C)	>16$^1/_2$	31,360
S. A. E. 1045 (0.44 C)	13	39,800
S. A. E. 1050 (0.53 C)	9	57,500

* Arc Energy (Joules/Inch) = $\frac{\text{Amps.} \times \text{Volts}}{\text{Welding Speed (Inch/Sec.)}}$

Using the procedure described above, except that the specimen was 1 inch instead of 2 inches wide, Theisinger found that the maximum Vickers hardness in the heat-affected zone of 0.90 C steel was 302 at 8 inches/min. but only 322 at 12 inches/min. The weld metal was 162–166 Vickers number. Troostite and sorbite were observed in the heat-affected zone, but the entire specimen was red hot after welding.

Table 2—Maximum Vickers Hardnesses in Manual Arc and Oxyacetylene Beads on S. A. E. 1045. Theisinger[1]

	Bare Electrode 170 Amp., 18 Volts	Covered Electrode 140 Amp., 26 Volts	Oxy-acetylene Weld
Weld metal	195	167	148
Heat-affected zone	282	258	215

Hardness explorations were also made of beads (no details) deposited by manual arc and oxyacetylene torch on S. A. E. 1045, Table 2. Comparison with Fig. 1 shows that the manual arc and oxyacetylene processes create softer heat-affected zones than the automatic arc process. The width of the zone of maximum hardness around the oxyacetylene bead was 0.25 inch, the grain size being exceptionally large.

Oxyacetylene flame cutting created maximum Vickers hardnesses of 170 and 275, respectively, in S. A. E. 1015 and 1045. The specimens consisted of plates $^1/_2 \times 2 \times 9$

inches from which a sliver $^1/_4 \times 9$ inches was flame cut.

Theisinger's pioneering research on the influence of carbon content on the maximum hardness in the heat-affected zone exhibited, as well, the importance of the rate of cooling after welding. For example, a maximum Vickers hardness of just short of 400 was found in the unfused heat-affected zone of a spot weld made in 47 cycles, 150 kva., 3000 lb pressure, in 0.22 C steel (175 Vickers number) $^1/_4$ inch thick. Much higher than any hardness developed beneath a bead of arc weld metal, this result emphasizes the danger of interpreting heat effect without consideration of all experimental conditions, such as dimensions of specimen, support for specimen, initial temperature of base metal, and its composition, hardening and grain size characteristics. The thickness of the specimen is vital, at least up to 1 inch, Table 3, in 0.43 C steel (175 Vickers hardness).

Table 3—Effect of Plate Thickness on the Maximum Vickers Hardness Near a Bead of Automatic Bare Electrode Arc Weld Metal Deposited on 0.43 C Steel at 8 Inches/Min. Theisinger[1]

Plate Thickness, Inch	Maximum Vickers Hardness in Heat-Affected Zone
$^1/_2$	255
1	300
$1^1/_2$	305
2	310

Hardness surveys of single beads deposited on a steel rail by organic coated, mild steel, all-position electrodes were made by Armstrong,[2] Table 4. The coarse grained, steel rail (0.52 C, 0.60 Mn) was machined to $6 \times 2^7/_{16} \times 1^1/_8$ inches, the bead being deposited down the length of the $6 \times 2^7/_{16}$ inch surface. The bead was deposited on the preheated specimen immediately upon withdrawal from the preheating furnace. Cracks occurred only if the base metal was below 260° C.

The hardness of the heat-affected zone decreased as the preheating temperature was raised. When the rail was welded cold, the heat-affected zone was composed mainly of martensite (500 ×), with some troostite according to Armstrong. As the preheating temperature was raised, the proportion of troostite increased. Ferrite appeared at the grain boundaries at 260° C., martensite disappeared at 370° C., and at 675° C. the structure contained ferrite, pearlite and troostite. Sorbite was never found. Structures observed in specimens of the unwelded rail quenched in water from 870° C. and tempered at the preheating temperatures were different from those observed in the heat-affected zone of the welded rail. Although the hardness was reduced by using a $^5/_{16}$-inch electrode instead of $^3/_{16}$-inch, the heat-affected zone contained martensite.

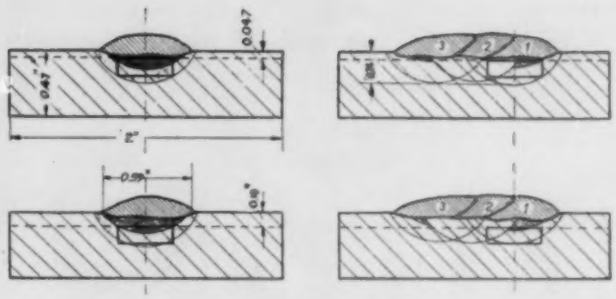


Fig. 2—Location of Specimens in Heat-Affected Zone. Czyrski[3]

Discussing Armstrong's research, L. C. Bibber stated that Vickers hardness explorations of double layer fillet welds in plain carbon steels (thickness not stated) containing 0.31 C (150 Vickers) and 0.45 C (160 Vickers) created maximum hardnesses of 217 and 517 Vickers, respectively. Martensite was found in the heat-affected zone of the higher carbon steel.

Tensile and notch-impact tests of the heat-affected zone in a series of plain carbon steels, Table 5, were made by Czyrski.[3] The tensile specimens were machined from bars 0.47 inch thick, 2.0 inches wide, 8 inches long on the surface of which 1 or 3 beads, 6 inches long, were deposited by means of a covered electrode, 0.16 inch diameter (no details). The tensile specimens were 0.12 × 0.37 inch, and were cut from two parts of the heat-affected zone, Fig. 2. The strength of the metal in the heat-affected zone was higher than unwelded base metal, but the ductility was lower when the carbon content was 0.50% or greater, with the exception of the specimens cut 0.098 inch below the surface of base metal carrying a triple bead.

The notch-impact specimens were of the standard German sharp notch type (DVMS) made from butt welds the plate on the notched side of which was unscarfed (see Fig. 9, review of literature on Impact Tests of Welded Joints—II, AMERICAN WELDING SOCIETY JOURNAL, 17 (9) Suppl., Sept. 1938). Table 6 shows that the heat-affected zone had higher notch-impact value than base metal, particularly with three layers.

The hardness of the bead welded specimens, Table 7, rises with increase in carbon content but is lower for the specimens with three beads than for the single bead specimens. Steel 5 (0.42 C) did not appear to have any martensite (50 ×) in the heat-affected zone.

The maximum Vickers hardness of the heat-affected zone of specimens $1.2 \times 4 \times 12$ inches with beads deposited on the surface, Fig. 3, was determined for a steel containing 0.54 C, 0.75 Mn, 0.46 Si, 0.015 P, 0.020 S, 0.15 Cu. Multiple layers usually decreased the hardness

Table 4—Mean Maximum and Minimum Vickers Hardness Numbers of Welded Rail. Armstrong[2]

Diameter of Electrode, Inch	Temperature of Base Metal, ° C.	Depth of Apparent Affected Zone, Inch	Penetration of Bead, Inch	Vickers Hardness Number*		
				Bead	Heat-Affected Zone	Unaffected Base Metal
$^1/_8$	Approx. 35	0.0625	0.0781	235–256	470–511	181–208
$^5/_{16}$	Approx. 35	0.0781	0.0625	215–235	462–557	184–202
$^5/_{16}$	Approx. 35	0.0625	0.0781	199–208	385–426	177–187
$^5/_{32}$	100	0.0781	0.0625	243–246	365–487	182–191
$^5/_{32}$	200	0.0625	0.0938	189–193	294–395	179–185
$^5/_{32}$	260	0.0939	0.0625	175–181	230–309	183–186
$^5/_{32}$	370	0.1094	0.0781	173–184	245–272	181–191
$^5/_{32}$	540	0.1719	0.0625	146–162	197–229	181–186
$^5/_{32}$	675	0.2656	0.1094	147–154	171–215	174–191

* The first number in each column is the mean minimum, the second is the mean maximum of 4 to 7 determinations.

Table 5—Tensile Tests of Specimens Cut from the Heat-Affected Zone (Fig. 2) of Plain Carbon Steels. Czyrski[3]

	Composition of Base Metal						Base Metal			Single Bead					
										Depth Below Surface = 0.047 Inch			Depth Below Surface = 0.098 Inch		
Steel	C	Mn	Si	P	S	Cu	Yield Strength, Lb./In.²	Tensile Strength, Lb./In.²	Elongation, % in 0.39 Inch	Y. S.	T. S.	El.	Y. S.	T. S.	El.
1	0.08	0.40	..	0.017	0.025	0.110	40,000	51,300	25.5	44,600	51,700	26.9	46,400	52,700	27.1
2	0.15	0.59	0.22	0.031	0.028	0.150	46,500	67,800	21.7	53,700	71,500	19.7	53,300	71,000	21.4
3	0.20	0.60	0.27	0.024	0.013	0.139	50,600	71,500	20.7	56,700	74,500	18.5	60,400	74,700	20.5
4	0.30	0.63	0.26	0.032	0.027	0.142	52,100	79,500	16.0	70,000	95,000	13.7	65,700	90,500	15.6
5	0.42	0.71	0.23	0.024	0.026	0.167	62,700	101,000	13.0	79,500	116,000	9.6	78,000	114,000	11.3
6	0.50	0.80	0.21	0.021	0.030	0.138	69,000	108,000	12.1	90,500	124,000	2.2	89,500	123,000	8.4
7	0.60	0.53	0.36	0.028	0.030	0.150	86,500	129,000	10.3	124,000	155,000	1.7	119,000	148,000	5.0
8	0.70	0.72	0.29	0.021	0.025	0.145	100,000	134,000	12.0	123,000	164,000	2.2	117,000	151,000	4.3

Triple Bead					
Depth Below Surface = 0.047 Inch			Depth Below Surface = 0.098 Inch		
Y. S.	T. S.	El.	Y. S.	T. S.	El.
46,500	53,300	25.6	40,600	52,000	26.2
54,300	71,100	24.0	51,000	68,400	25.6
60,700	76,500	22.3	61,100	76,200	21.9
66,800	90,500	15.5	64,400	85,500	16.7
74,000	105,000	13.0	74,600	104,000	14.5
87,000	116,000	8.9	87,500	116,000	13.3
103,000	145,000	7.8	96,000	138,000	10.2
106,000	147,000	8.5	97,500	142,000	13.3

Table 6—DVMS Notch-Impact Value (Mkg./Cm.²) of the Heat-Affected Zone of Welded Plain Carbon Steels. See Table 5. Czyrski[3]

Steel	1	2	3	4	5	6	7	8
Base metal	16.3	10.7	8.7	5.5	2.0	1.72	1.28	0.98
Single layer weld	14.5	12.0	10.0	7.5	3.0	2.14	1.57	1.13
Three layer weld	20.2	14.2	11.1	10.0	4.1	3.28	2.98	2.53

of the heat-affected zone. However, in specimen 7, high hardness was found under the last bead S, perhaps because the adjacent beads of weld metal accentuated the quenching action. The heat-affected zone appeared completely martensitic (500 ×). With higher carbon steels, Table 8, tested in a similar way, a large bead slowly deposited created a much softer heat-affected zone than small, superposed beads. Evidently, the heat from the small bead does not completely penetrate prior beads to draw the hardened base metal. The broad bead, on the other hand, carries so much heat that the cooling rate beneath it is relatively slow.

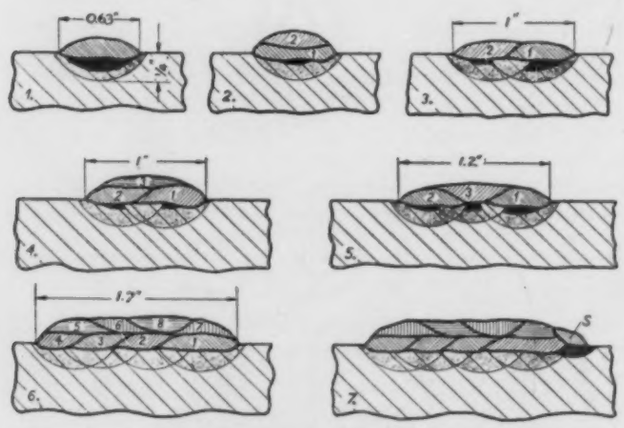


Fig. 3—Specimens of Welded Steel Containing 0.54 C Used for Hardness Determinations. Czyrski[3]

Specimen	Maximum Vickers Hardness
1	514
2	336
3	403
4	315
5	332
6	299
7	590 (under last bead S)
Unaffected base metal	206

Table 7—Maximum and Minimum Vickers Hardness in the Heat-Affected Zone of Arc-Welded Plain Carbon Steels (See Fig. 2). Czyrski[3]

	Steel (see Table 5)							
	1	2	3	4	5	6	7	8
Base metal	102	142	157	161	189	208	259	289
Maximum hardness, single bead	104	153	167	199	291	329	391	438
Minimum hardness, single bead	100	145	156	170	248	258	347	389
Maximum hardness, triple bead	103	151	165	184	240	248	340	385
Minimum hardness, triple bead	98	143	156	162	208	232	287	303

Table 8—Maximum Vickers Hardness in Heat-Affected Zone of Arc-Welded High-Carbon Steels. Czyrski[3]

Steel	10	11	12	13
Base metal	259	272	291	304
Small bead	671	678	652	650
Specimen 6, Fig. 3, 8 layers	572	558	558	560
Broad bead	348	349	363	403

Steel	C	Mn	Si	P	S	Cu
10	0.69	0.72	0.29	0.030	0.029	0.157
11	0.84	0.49	0.41	0.012	0.027	0.163
12	1.01	0.25	0.34	0.015	0.027	0.217
13	1.21	0.40	0.28	0.019	0.019	0.153

The hardness of plain carbon steel deposits, Table 9, depends on the carbon content, as Zeyen[4] showed. Allowing each bead to cool thoroughly before depositing a succeeding bead slightly raised the hardness. Micrographic examination revealed a coarser structure in the oxyacetylene than in the arc deposited higher carbon steel. Hibbard's[5] A. E. R. A. drop tests on cones of weld metal 1 inch high deposited at 150–160 amp. under extraordinarily hot conditions showed, Table 10, that the

Table 9—Carbon Recovery and Hardness of Metal Deposited by Plain Carbon Steel Rods. Zeyen[4]

Method of Welding	Number of Superposed Beads	C	Recovery of Carbon %	Si	Mn	Brinell Hardness*
Rod, 0.20 inch diameter	...	0.65	..	0.09	0.70	...
Oxyacetylene	1	0.35	54	0.02	0.48	170
Oxyacetylene	3	0.42	65	0.02	0.50	174 (191)
Bare electrode, d.c.	1	0.15	23	0.02	0.45	161
Bare electrode, d.c.	3	0.20	31	0.03	0.49	170 (176)
Coated electrode, d.c.	1	0.19	29	0.02	0.49	160
Coated electrode, d.c.	3	0.24	37	0.06	0.66	174 (187)
Coated electrode, a.c.	1	0.20	31	0.05	0.51	164
Coated electrode, a.c.	3	0.24	37	0.06	0.59	177 (187)
Rod, 0.20 inch diameter	...	1.09	..	0.19	0.39	...
Oxyacetylene	1	0.58	53	0.09	0.29	249
Oxyacetylene	3	0.64	59	0.04	0.31	255 (267)
Bare electrode, d.c.	1	0.36	33	0.07	0.25	207
Bare electrode, d.c.	3	0.49	45	0.04	0.27	255 (264)
Coated electrode, d.c.	1	0.44	40	0.07	0.40	219
Coated electrode, d.c.	3	0.58	53	0.11	0.49	249 (258)
Coated electrode, a.c.	1	0.43	39	0.06	0.36	219
Coated electrode, a.c.	3	0.52	48	0.15	0.47	255 (285)

* The values in parentheses were determined on deposits each bead of which was allowed to cool thoroughly before the next bead was deposited.

deposits were relatively soft as welded but rapidly hardened to 300 Brinell under impact. Csilléry and Péter[6] report the following properties for a bare electrode deposit containing 0.60 C, 0.49 Mn, 0.18 Si: tensile strength = 72,500 lb./in.^{2}, elongation = 4% in 2 diam., reduction of area = 2%, notch-impact value = 4.1 mkg./cm.^{2} Annealing (no details) had a beneficial effect on the notch-impact value of high carbon arc welds in Pomp's[7] tests, Table 11. Rapatz[8] also found that annealing was advisable for butt welds in steel containing 1.0 C, the elongation of which was 1.9% (no details) as welded, but 12.2% after forging and annealing. The maximum Brinell hardness found by Roberts[9] in the vicinity of a single bead deposited by a heavy covered electrode on a steel containing 0.52 C, 0.71 Mn, 0.22 Si, 4 × 2 × 0.30 inch was 277. The hardness was reduced to 255 by heating to 590–650° C., base metal being 228 Brinell.

Table 11—Effect of Annealing on the Notch-Impact Value of High-Carbon Steel Weld Metal. Pomp[7]

Composition of Deposit C	Mn	Si	Notch-Impact Value, Mkg./Cm.^{2} As-Welded	Annealed
0.51	0.82	0.19	1.8	10.1
0.83	0.60	0.29	1.5	3.8

In addition to the tests on material of homogeneous composition, a number of tests have been made on heterogeneous specimens. For example, Schmitt[10] found that oxyacetylene and arc welds made with low-carbon electrodes in a series of plain carbon steels up to 1.25 C had a tensile strength of 45,000 lb./in.^{2} regardless of the composition of base metal. Nevertheless, penetration was satisfactory in all specimens and there was considerable pick up of carbon by the weld metal from the higher carbon steels. No explanation was forthcoming. On the other hand, Goodger[11] determined the hot and cold bend values and tensile strength of unmachined *V*, *X* and lap joints in boiler plate 1/2 inch thick made with a series of electrodes containing 0.08 to 1.20 C and coated with white asbestos and sodium salts. The experimental procedure (composition of base metal not known) was so sketchily described that no conclusions could be drawn. The weld metals contained a trace to 0.35 C. Single bead fillet welds were made by Wright[12] with covered electrodes (no details) in steel 1/2 inch thick containing 0.57 C, 0.76 Mn, 0.16 Si, 0.034 S, 0.015 P. Base metal was 219–232 Brinell; the maximum Vickers hardness in the heat-affected zone was 357. Tensile, bend and impact specimens fractured 1/2 to 4 1/2 inches from the weld.

Using a variety of electrodes as well as oxyacetylene welding, Zeyen[13] determined the tensile, bend, notch impact and reversed bend fatigue values and Firth hardness of butt welds in a series of plain carbon steels, Table 12. The results of oxyacetylene welding with a rod containing 1 Cr, 0.2 Mo and of welding with covered austenitic electrodes are summarized in the reviews of literature on Welding Chromium-Molybdenum Steels (American Welding Society Journal, **17** (7) Suppl., 16, July 1938), and Welding Chromium and Chromium-Nickel Corrosion and Heat Resisting Steels (American Welding Society Journal, **18** (3) Suppl., 92(1939)) respectively. The fatigue tests were summarized in the review of literature on Fatigue Tests of Welded Joints (American Welding Society Journal **16,** (1) Suppl., 17–18 (1937)), and impact tests were mentioned in the reviews of literature on Impact Tests of Welded Joints. The results are averages of 3 to 5 tests. The tensile specimens had no reduced section. The fillet weld tensile strength was determined on standard German cruciform specimens. Many details of the experiments are lacking.

The results led Zeyen to the conclusions that satisfactory butt welds can be made in plain carbon steel up to about 0.56 C. Fillet welds made with the heavy covered

Table 10—Hardness of Plain Carbon Steel Weld Deposits. Hibbard[5]

Specimen	C	Recovery of Carbon, %	Mn	Si	P	S	Brinell Hardness As-Welded	Brinell Hardness After 5 Blows	% Compression by 5 Blows
Bare electrode	0.96	..	0.45	0.07	0.014	0.033			
Ditto, deposit	0.48	50	0.35	0.13	0.005	0.032	203	295	37.7
Coated electrode	0.90	..	0.40	0.08	0.008	0.031			
Ditto, deposit	0.40	44	0.31	0.10	0.013	0.029	179	302	37.2

Table 12—Mechanical Properties of Welded Plain Carbon Steels. Zeyen[13]

Property	Electrode	Specimen	Steel 1 0.24 Inch	Steel 1 0.47 Inch	Steel 2 0.24 Inch	Steel 2 0.47 Inch	Steel 3 0.24 Inch	Steel 3 0.47 Inch
Tensile strength, lb./in.²	Unwelded		57,000	57,000	68,000	68,000	81,000	84,000
Tensile strength, lb./in.²	1	Unmachined	75,000	78,000	85,000	87,000	101,000	91,000
Tensile strength, lb./in.²	1	Machined flush	73,000	73,000	80,000	75,000	85,000	84,000
Tensile strength, lb./in.²	2	Unmachined	60,000	75,000	71,000	88,000	87,000	91,000
Tensile strength, lb./in.²	2	Machined flush	58,000	73,000	70,000	78,000	87,000	83,000
Tensile strength, lb./in.²	3	Unmachined	77,000	77,000	84,000	87,000	88,000	90,000
Tensile strength, lb./in.²	3	Machined flush	61,000	76,000	70,000	81,000	83,000	88,000
Bend angle, degrees	Unwelded		>180	>180	>180	>180	>180	>180
Bend angle, degrees	1	Unmachined	>180	180	>180	180	161	96
Bend angle, degrees	1	Machined flush	>180	>180	>180	>180	180	91
Bend angle, degrees	2	Unmachined	>180	180	96	80	80	57
Bend angle, degrees	2	Machined flush	>180	>180	180	72	92	60
Bend angle, degrees	3	Unmachined	140	85	88	82	69	55
Bend angle, degrees	3	Machined flush	85	87	71	80	53	48
Fillet weld tensile strength, lb./in.²	1	Unmachined	...	51,000	...	52,000	....	56,000
Fillet weld tensile strength, lb./in.²	2	Unmachined	...	>43,000	...	>51,000	....	55,000
Fillet weld tensile strength, lb./in.²	3	Unmachined	...	>48,000	...	51,000	....	53,000
DVMR notch-impact value, mkg./cm.²	Unwelded		...	17.0	...	8.3	....	7.1
DVMR notch-impact value, mkg./cm.²	1	Notch at root of weld	...	12.4	...	10.4	....	8.9
DVMR notch-impact value, mkg./cm.²	2	Notch at root of weld	...	7.4	...	5.6	....	6.0
DVMR notch-impact value, mkg./cm.²	3	Notch at root of weld	...	6.0	...	5.9	....	5.0
Firth hardness	Unwelded		...	91	...	130	....	152
			A	B	A	B	A	B
Firth hardness	1	Heat-affected zone	133	130	149	144	190	168
Firth hardness	2	Heat-affected zone	133	131	150	138	184	162
Firth hardness	3	Heat-affected zone	148	131	164	156	187	170

Property	Electrode	Specimen	Steel 4 0.24 Inch	Steel 4 0.47 Inch	Steel 5 0.24 Inch	Steel 5 0.47 Inch	Steel 6 0.24 Inch	Steel 6 0.47 Inch	Steel 7 0.24 Inch	Steel 7 0.47 Inch
Tensile strength, lb./in.²	Unwelded		92,000	92,000	98,000	95,000	105,000	108,000	121,000	112,000
Tensile strength, lb./in.²	1	Unmachined	101,000	91,000	122,000	97,000	124,000	93,000	111,000	95,000
Tensile strength, lb./in.²	1	Machined flush	88,000	83,000	91,000	88,000	101,000	77,000	71,000	78,000
Tensile strength, lb./in.²	2	Unmachined	94,000	94,000	115,000	97,000	112,000	98,000	101,000	103,000
Tensile strength, lb./in.²	2	Machined flush	93,000	91,000	114,000	93,000	110,000	95,000	98,000	97,000
Tensile strength, lb./in.²	3	Unmachined	105,000	97,000	111,000	101,000	115,000	105,000	118,000	101,000
Tensile strength, lb./in.²	3	Machined flush	90,000	91,000	93,000	91,000	95,000	90,000	88,000	94,000
Bend angle, degrees	Unwelded		>180	>180	>180	>180	94	105	65	90
Bend angle, degrees	1	Unmachined	122	74	106	67	86	39	64	13
Bend angle, degrees	1	Machined flush	95	66	76	60	69	17	51	5
Bend angle, degrees	2	Unmachined	78	52	58	26	53	24	34	15
Bend angle, degrees	2	Machined flush	72	52	52	32	59	31	49	25
Bend angle, degrees	3	Unmachined	55	48	52	42	46	23	41	21
Bend angle, degrees	3	Machined flush	52	45	42	40	38	35	35	25
Fillet weld tensile strength, lb./in.²	1	Unmachined	....	61,000	....	cracked welds	....	cracked welds	....	cracked welds
Fillet weld tensile strength, lb./in.²	2	Unmachined	....	59,000	....	60,000	....	55,000	....	56,000
Fillet weld tensile strength, lb./in.²	3	Unmachined	....	55,000	....	58,000	....	54,000	....	67,000
DVMR notch-impact value, mkg./cm.²	Unwelded		....	7.3	....	5.6	....	3.5	....	5.3
DVMR notch-impact value, mkg./cm.²	1	Notch at root of weld	....	8.0	....	7.9	....	6.6	....	5.6
DVMR notch-impact value, mkg./cm.²	2	Notch at root of weld	....	5.9	....	5.1	....	5.6	....	5.8
DVMR notch-impact value, mkg./cm.²	3	Notch at root of weld	....	5.1	....	3.3	....	3.3	....	3.4
Firth hardness	Unwelded		....	165	....	180	....	203	....	210
			A	B	A	B	A	B	A	B
Firth hardness	1	Heat-affected zone	209	186	226	190	305	245	355	305
Firth hardness	2	Heat-affected zone	191	180	229	215	282	249	298	269
Firth hardness	3	Heat-affected zone	224	211	260	244	278	266	292	272

Steel	C	Si	Mn	P	S
1	0.11	0.12	0.47	0.016	0.018
2	0.17	0.31	0.55	0.026	0.029
3	0.30	0.23	0.56	0.040	0.032
4	0.38	0.29	0.64	0.011	0.018
5	0.56	0.32	0.63	0.011	0.014
6	0.60	0.33	0.63	0.025	0.020
7	0.68	0.35	0.69	0.028	0.025

A = Maximum hardness in zone 0.04 inch below surface of plate.
B = Maximum hardness in zone 0.24 inch below surface of plate; Firth hardness is approximately the same as Brinell.
Electrode 1—heavy covered electrode for low-alloy steels. All-weld-metal contained 0.10 C, 0.10 Si, 0.50 Mn; tensile strength = 72,000 lb./in.², notch-impact value (DVMR) = 12.9 mkg./cm.²
Electrode 2—light covered, contained 1.5% Mn (no details). Electrode 3—cored electrode of alloy steel (no details).

electrode cracked above 0.38 C, the cracking not being observed with electrodes that generated less heat. It was emphasized that the results apply only to plate 1/4 to 1/2 inch thick. Sheet and heavy plate require special precautions.

Similar tests have been made by Aysslinger[14] on three steels containing 0.29–0.56 C, 0.39 and 0.79 inch thick, Table 13, welded with a bare and 4 different coated electrodes.

Since the welds made with bare electrodes were weak and brittle, and since the thin, medium and heavy covered electrodes yielded about the same results, only the results from one of the medium covered electrodes are given in Table 13. The size of electrode, type of coating, and number of layers were not stated. The butt welds were V type with reverse run. The only difficulty in butt welding—fine cracks in the first layer of Steel 3—was overcome by prebending (no details). In fillet welding, cracks occurred with the medium and heavy covered electrodes, but not with bare or thin covered electrodes. It was believed that a thick slag hindered the cooling of the weld in the region of low yield strength. Since cracking occurred only in the root layer of a two-layer fillet weld, the bare electrode was used for the first layer of all fillet welds. The bare electrode deposited metal containing 0.04 C, 0.22 Mn. All tests were made in triplicate, the maximum and minimum values or averages being quoted in Table 13.

The results showed that, although adequate tensile strength may be secured in welded higher carbon steels, the bend ductility is likely to be unsatisfactory. Furthermore, fillet welds should be made with thin covered electrodes, otherwise the welds may be weak, particularly in thick plate. The microstructure revealed no martensite.

Tension, bend, notch impact and hardness tests were made by Stursberg[15] on oxyacetylene butt welds in 0.35 C steel, Table 13, using four plain carbon steel rods containing 0.05 to 0.58 C. The base metal was 0.24 inch thick and had tensile strength = 83,000 lb./in.^{2}, bend elong. = 36.7% in 0.59 inch, notch-impact value = 8.2 mkg./cm.^{2} The welds were made in a jig to prevent distortion. The gap was 0.10–0.12 inch and right hand welding was used. After the weld had been made, the joint was turned over and lightly reverse welded. The rods were 0.16 inch diameter. To restrict fracture to the weld, the tensile specimens were made with a reduced section 0.59 inch long, 0.55 inch wide, machined to 0.16 inch thick. The bend specimens were machined to 0.20 inch thick and the plunger diameter was 0.59 inch. The notch-impact specimens were only 0.24 inch thick; otherwise they conformed to DVMR type with notch in the

Table 13—Mechanical Properties of Arc-Welded Higher Carbon Steels. Aysslinger[14]

	Plates 0.39 Inch Thick			Plates 0.79 Inch Thick		
	Steel 1	Steel 2	Steel 3	Steel 1	Steel 2	Steel 3
Tensile strength, lb./in.^{2}, parallel section, unmachined	87,500–89,000	(85,000)	96,500–98,000	82,500 (84,000)	(84,000–85,500)	85,500–87,000
Tensile strength, lb./in.^{2}, parallel section, machined	82,500 (94,000)	79,500–85,500	87,000–88,500	81,500–85,500	78,500–79,500	94,000
Tensile strength, lb./in.^{2}, reduced section, unmachined	95,500–99,000	96,500–97,000	102,500–105,500	91,000–94,000	91,000–92,500	87,000–88,000
Tensile strength, lb./in.^{2}, reduced section, machined	82,500–87,500	85,500	84,500–86,000	79,500–85,500	84,000–85,500	84,000–87,000
Bend angle degrees, unmachined	56–74	83–99	34–40	62–100	38–81	36–39
Bend angle, degrees, machined	82–115	54–74	82–98	120–180	67–80	22–42
Bend elongation, % in 0.79 inch, machined	25	20	25	50	38	18
Bend elongation, % in 1.58 inches, machined	13	13	23	33	28	15
DVMR notch-impact value, mkg./cm.^{2}	6.0–6.7	6.7–8.3	7.5–9.6	5.7–8.2	4.5–6.5	6.2–7.0
Fillet weld tensile strength, lb./in.^{2}	69,000–71,000	59,500–64,000	62,000–71,000	49,000–53,000	51,000–55,500	57,000–59,000

NOTE: Values in parentheses denote fracture in base metal. All other fractures occurred in weld metal. Parallel-section specimens were 12 inches long, 1.18 inches wide. Reduced-section specimens were similar but the width at the weld was reduced to 0.79 inch with a radius of 2 inches. The diameter of the rollers for the bend test was 2 inches for plate 0.39 inch thick and 4 inches for plate 0.79 inch thick. The distance between rollers and thickness of ram were 6d and 3d, respectively. The specimens were 1.18 inches wide. The root of the weld was in tension. The notch of the DVMR impact specimen was in the middle of the weld. The cross-shaped end fillets consisted of a thin layer of bare electrode 0.12 inch diameter followed by the covered electrode.

Composition and Mechanical Properties of Unwelded Plates

Steel No.	Thickness, Inch	Chemical Composition, % C	Si	Mn	P	S	Tensile Strength, Lb./In.^{2}	Bend Angle, Degrees	Transverse DVMR Notch-Impact Value Mkg./Cm.^{2}
1	0.39	0.30	0.28	0.90	0.042	0.027	85,500	180	6.2
2	0.39	0.39	0.11	0.61	0.025	0.030	84,000	180	4.8
3	0.39	0.54	0.27	0.69	0.021	0.030	108,000	180	3.3
1	0.79	0.29	0.27	0.90	0.041	0.029	86,000	180	6.0
2	0.79	0.37	0.10	0.60	0.026	0.033	84,000	180	4.5
3	0.79	0.56	0.27	0.69	0.019	0.030	112,500	180	3.2

Composition of Welds, %

Steel No.	Plate Thickness, Inch	Composition of Welds, % C	Si	Mn	P	S
1	0.39	0.11	0.10	0.40	0.033	0.023
2	0.39	0.14	0.05	0.30	0.024	0.030
3	0.39	0.15	0.09	0.28	0.026	0.028
1	0.79	0.09	0.02	0.30	0.031	0.024
2	0.79	0.11	0.07	0.31	0.030	0.026
3	0.79	0.11	0.11	0.31	0.032	0.025
All-Weld-Metal		0.06	0.08	0.23	0.034	0.027

Table 14—Mechanical Properties of Oxyacetylene Welds as a Function of Carbon Content of Rod and Mixture Ratio. Stursberg[15]

Property	Rod No.	Mixture Ratio 0.8	0.9	0.95	1.0	1.1	1.2	1.4	1.6
Carbon content of weld metal, %	1	1.80	0.52	....	0.15	....	....	....	0.05
Carbon content of weld metal, %	2	2.30	0.76	....	0.25	....	....	....	0.15
Carbon content of weld metal, %	4	2.50	0.90	....	0.28	....	....	....	0.25
Tensile strength, lb./in.²	1	47,000	77,000	73,000	64,000	61,000	52,000	46,000	40,000
Tensile strength, lb./in.²	2	41,000	89,000	87,000	78,000	70,000	65,000	56,000	48,000
Tensile strength, lb./in.²	3	55,000	98,000	92,000	85,000	80,000	78,000	64,000	63,000
Tensile strength, lb./in.²	4	40,000	95,000	93,000	84,000	77,000	72,000	63,000	57,000
Bend elongation % in 0.59 inch	1	0	19	30	27	21	14	8	6
Bend elongation % in 0.59 inch	2	0	15	22	28	30	22	10	8
Bend elongation % in 0.59 inch	3	0	13	17	26	22	18	10	6
Bend elongation % in 0.59 inch	4	2	12	15	34	29	16	6	4
Notch-impact value, mkg./cm.²	1	0	6.0	8.2	9.3	8.4	6.0	3.4	2.2
Notch-impact value, mkg./cm.²	2	0	2.5	3.8	7.9	8.2	7.9	3.8	1.9
Notch-impact value, mkg./cm.²	3	0	1.9	2.9	7.3	6.4	5.3	4.7	3.8
Notch-impact value, mkg./cm.²	4	0	1.2	1.9	6.2	6.2	5.1	3.1	2.5
Brinell hardness of weld	1 to 4	360	230	200	155	....	....	....	125

Rod	C	Mn	Si	P	S
1	0.05	0.26	0.03	0.006	0.026
2	0.28	0.40	0.07	0.015	0.038
3	0.37	0.64	0.12	0.019	0.029
4	0.58	0.73	0.15	0.017	0.022
Base metal	0.35	0.65	0.18	0.033	0.034

Mixture Ratio = ratio of oxygen to acetylene by volume.

side of the weld. In other words, the mechanical properties of the weld metal, not the base metal, were determined.

Table 14 shows that the maximum tensile strength, which occurred at a mixture ratio of 0.9 does not correspond with maximum ductility, toughness or carbon content. Maximum bend ductility and toughness occurred at ratios of 0.95 to 1.1, corresponding approximately with the condition of equality in hardness between weld metal and base metal, which was 175 to 185 Brinell. Equality in hardness was achieved, as plots of the results showed, when there was 2% excess acetylene in the flame for all rods. The maximum or minimum hardness always was found in the center of the weld. The highest ratio of tensile strength (kg./mm.²) to Brinell hardness was 0.35 and occurred at a mixture ratio of 1.0. The ratio was 0.09 and 0.26 at mixture ratios of 0.8 and 1.6, respectively.

A 75° double V butt weld (step back welded in steps 3 inches long) in steel containing 0.82 C, 0.41 Mn, 0.040 S 1/4 inch thick made by Nestor[16] with a covered mild steel electrode (1/4 inch diameter, 300-amp., weld metal contained 0.22 C) had a maximum Rockwell B hardness of 114 in the heat-affected zone. Unaffected base metal was 97 B Rockwell. The effect of heat treatment, Table 15, was to reduce the hardness with accompanying reduction in strength. The heat-affected zone of the as-welded specimen was troostitic.

MICROSTRUCTURE

The microstructure of welded plain carbon steels as a function of carbon content is discussed in the preceding section (Theisinger, Armstrong, Czyrski, Zeyen). As the carbon content of the base metal is increased there is an increasing tendency to form martensite in the heat-affected zone, since carbon reduces the critical cooling rate of the steel. For example, Rapatz[18] found martensite in the heat-affected zone of 0.51 C steel welded with covered, cored and austenitic electrodes, but not in oxyacetylene welds. However, the Rail Joints Committee,[19] examining welds made by several processes in 0.75 C steel rail, concluded that decarburization, blow-holes, slag inclusions and grain growth were troublesome rather than martensite. Cook's[20] examination of oil well casing containing 0.40–0.45 C dealt with the refined and overheated zones in oxyacetylene and arc welds but no mention was made of martensite. Widmannstätten structure, regarded by Belaiew[21] as one of the causes of brittleness in welded joints was found to occur more readily as the carbon content of base metal was increased from 0.10 to 0.30%. Lavroff[22] examined the structure of a large number of arc welds in high-carbon steel and concluded that good welds from the microstructural viewpoint can be produced in base metal containing 0.9 C but not 1.1 C with electrodes containing 0.7 C. The mechanical properties were not determined. Gerbeaux[96] found that whereas there was no grain coarsening at the scarves of bronze-welded joints in steel containing 0.75 C, there was grain coarsening in both arc and gas welds.

Often the microstructure of a weld in mild steel reveals a zone of what appears to be high-carbon steel at the fusion line. According to Brophy,[23] who attributed brittleness to the zone, the steep temperature gradient

Table 15—Effect of Heat Treatment on Covered Electrode Welds in 0.82 C Steel. Nestor[16]

Heat Treatment	Tensile Strength,* Lb./In.²	Elongation, % in 1 Inch	Rockwell B Hardness Metal	Affected Zone
As-welded	97,000–104,000	31	97	114
Furnace cooled from 790–815° C.	77,000–79,000	39	80	89
Heated to 790–815° C., cooled below lower critical, reheated to just below Ac_1, cooled in furnace	.....	..	80	88
Heated just below Ac_1 (725° C.) cooled in furnace	68,000–81,000	33	84	95

* Specimen was reduced section, 2 1/2 inches between shoulders, 1 1/2 inches fillet radius, about 0.35 inch wide, machined to 0.20 inch thick (no details of fracture).

during welding creates a carbon solubility differential, the carbon in base metal migrating from the region of low temperature (base metal) to the region of high temperature (fusion line). Hodge[98] and Rolfe[99] observed the line of pearlite at the fusion line in as-welded mild steel, and Moses[100] refers to pearlite accumulations at the fusion zone as a "characteristic" of arc-welded mild steel. Atkins[101] found the pearlite band in arc-welded mild steel only after annealing. Busch[102] likewise observed the line of pearlite in a weld (0.08 C, 0.06 Si, 0.3 Mn) in boiler plate (0.16 C, 0.24 Si, 0.40 Mn, $1^1/_4$ inch thick) only after normalizing. Busch believed that the band of pearlite arose from incomplete mixture of higher-carbon, molten base metal with lower-carbon weld metal. Pearlite segregations back of annealed oxygen-cut surfaces in mild steel were attributed by Wilkinson and O'Neill[103] to oxygen pick-up from the cutting oxygen. Schuster[104] found that the band of pearlite was especially likely to occur in normalized arc welds in mild steel if the arc was unstable and high-oxygen weld metal was to be expected. Like Wilkinson and O'Neill, Schuster applied the well-known explanation of Thompson and Willows whereby banding is attributed to oxygen segregation. Schuster believed that the oxygen entered the mushy metal at the fusion line, and partially decarburized it. The balance of the carbon was pushed into a pearlite band by the oxygen-rich steel. Hoyt[104] found the pearlite band in welds made with bare electrodes but not in welds made with cellulose covered electrodes. He offered no explanation, but stated that the welds made with bare electrodes, had a high oxygen content, and were abnormal.

CARBON LIMIT

Several investigations have been made for the purpose of determining the possible or permissible carbon content of plain carbon steel for welding. The criterion of welding was more or less explicitly stated by the investigators. Hatfield's[24] general statement that carbon is the most important element in welding special steels is amply supported by the investigations of Warner and Theisinger. The committee on Low Alloy Steels[25] adopted 96 B Rockwell as the maximum hardness that may be attained under ordinary welding conditions (not precisely defined) by a "foolproof" steel. Foolproof steels never contained over 0.15 C in the Committee's tabulation. Kinzel[26] states there is no trouble in welding low-alloy steel containing less than 0.14 C, and Diegel[27] considered 0.15 C as the limit for oxyacetylene welding. Both used ductility as their criterion. The Metropolitan Railway in Paris[28] limited the steel used for welded bridge reinforcement to 0.10 C max. The German Railways, according to Bierett[29] restrict the carbon content of steel for welded bridges to 0.20% max. Theisinger[1] adopted 100 Vickers hardness units as the maximum allowable increase in hardness of the heat-affected zone over base metal and arrived at Table 1 for the experimental conditions prevailing in his tests: The conclusion drawn by Czyrski[3] from his tests (see section on Mechanical Properties) was that good arc welds can be produced in plain carbon steels containing not over 0.45 C. Rapatz[18] stated that the hardness of welds in plate 0.39 inch thick increases so rapidly as the carbon content is increased that there is no likelihood of obtaining a ductile weld in 0.5 C steel.

Bend tests by Münter[30] on a series of plain carbon steels welded by the Arcogen process led him to conclude that steels with 0.06–0.12 C were very good for welding, steels with 0.12–0.30 C were good and steels with 0.30–0.70 C could be welded if occasion demanded. In 1930 and 1934 Sommer[31] stated as a result of experiment that the limit for carbon content in gas and arc welding plain carbon steel was 0.35%. Hayes[32] observed that cracks occurred in the arc welding of strips to worn railheads if the strip contained 0.75–0.90 C but not if the strip contained 0.35–0.50 C. Applying the hypothesis (not well supported) that elements forming interstitial solid solutions with iron predispose the metal to welding cracks, Meunier and Rosenthal[33] deduced that cracking increased as the carbon content was raised, which was in accord with their experience. Metallographic examination of a broken cast steel ship's propeller (0.26 C, 0.42 Si, 0.47 Mn, 2.78 Ni, 0.031 P, 0.014 S) by Heyn[34] revealed that at the point of fracture a low grade patch weld had been made, the weld metal containing 0.90 C.

The permissible carbon content for welding increases as the thickness increases, according to Egloff.[35] He placed the maximum carbon content at 0.25% for plates $^3/_4$ inch thick or less, and at 0.30% for thicker plates. Similar variations of carbon content with thickness appear in a number of specifications for steel suitable for welding.

A survey of specifications for steel used in fusion welded pressure vessels made by Dorey[36] showed that the carbon ranges from 0.15–0.25% in Great Britain, 0.10–0.30% in Germany, 0.16–0.20% in Sweden, 0.05–0.15% in France and 0.15–0.35 in the United States (A. S. M. E. Power Boiler Construction Code). A. S. T. M. specifications also include carbon contents up to 0.35% for some purposes. For example, A. S. T. M.-A105-36 for High Temperature Pipe Flanges limits the carbon content to 0.35% max. in flanges to be welded. A. S. T. M.-A150-33T limits the carbon content to 0.35% max. (0.50–0.90 Mn, 0.25 Si max.) in steel plates for fusion welded pressure vessels. The U. S. Navy Bureau of Engineering[37] has issued a proposed specification for fusion welding of drums of power boilers in which the composition of the base metal is limited to not over 0.30 C, 0.30–0.60 Mn. The tentative specifications of the A. R. E. A.[38] (1933) for high-carbon steel electrodes were 0.85–1.10 C, 0.30–0.60 Mn, not over 0.02 Si.

RECOVERY

Oxyacetylene Welding

The recovery of carbon in oxyacetylene welding depends primarily on flame adjustment as Keel[39] (see Table 18, reviews of literature on Welding Manganese Steels, American Welding Society Journal, 17 (8) Suppl. 9, Aug. 1938) and Stursberg,[16] Table 13, have shown. Keel attributed loss of carbon to the action of CO and H_2 rather than to O_2. More recently Keel[90] believed that a part of the loss of carbon might be attributed to evaporation. Stursberg found that the "neutral" flame depends on the carbon content of the rod, the "neutral" mixture containing more acetylene with high-carbon rods than with low carbon. Tichodeev[40] found that even oxidizing oxyacetylene flames cause pick up of carbon by the deposit, a rod containing 0.07 C, 0.22 Si, 0.42 Mn depositing drops in an oxidizing flame containing 0.11 C, 0.05 Si, 0.16 Mn. Since the composition of the oxidizing flame was not determined, the results must be interpreted with caution. Even with flames of known composition, the size of the flame and the position of the rod are important factors that must affect recovery of carbon, although nothing is known quantitatively about these factors at present.

In oxyacetylene welding commercial steels Portevin and Leroy[41] found that 0.1 C was lost in melting down

the rod, regardless of carbon content (0.05–1.2%). Slightly more carbon was lost in making a weld in 0.39-inch plate of the same composition as the rod, this additional loss increasing with the carbon content of the rod, because high-carbon rods reduce the speed of welding. The addition of 1.5 to 2.5 Si to the rod reduced carbon loss to nearly zero. On the other hand, 1.0 to 2.0 Mn had no effect. Zeyen's[4] results, Table 9, point to a carbon recovery of 60% in oxyacetylene welding with high-carbon steel rods. Mercier[42] found 70% recovery in tests of a number of low-alloy steels of probably low or medium carbon content. Without stating experimental details, Picard[43] reported carbon contents of 0.40–0.60% in deposits with an oxygen-acetylene ratio of 1.15–1.21, and carbon contents of 0.92–1.42% with a ratio of 0.87–0.93.

Bare Electrodes

Determinations of carbon recovery from bare electrodes are scattered through the sections on Recovery in most of the reviews of literature on welding alloy steels. Additional information is summarized in Tables 16 to 19 (see Tables 9 and 10 of the present review and Tables 10 to 13 of the review of literature on Welding Manganese Steels, AMERICAN WELDING SOCIETY JOURNAL, 17 (8) Suppl., Aug. 1938).

In general, the recovery of carbon from bare plain carbon steel electrodes is about 25% at 0.1–0.2 C, rising to about 50% at 0.5–1.0 C, as shown by Zeyen,[4] Hare[44] (Table 16), Hibbard[8] (Table 10), and Pilarczyk.[45] The results of Tichodeev,[40] Table 17, suggest an opposite trend. Csilléry and Péter[6] found 0.60 C, 0.49 Mn, 0.18 Si in the deposit from a bare electrode containing 0.85 C, 0.55 Mn, 0.35 Si, the carbon recovery being 71%. That carbon recovery is sensitive to changes in experimental conditions has been proved by Pilarczyk (see Tables 10 to 12 of the review of literature on Welding Manganese Steels, AMERICAN WELDING SOCIETY JOURNAL, 17 (8) Suppl., Aug. 1938). For example, the recovery of carbon from a bare electrode containing 0.86 C, 0.27 Mn and deoxidized with aluminum was 55% compared with 35% for a similar electrode not aluminum killed. According to Hoffmann,[46] the loss of carbon in depositing bare electrodes is 30% greater with electrode negative than positive. Hare,[44] Table 16, changed to positive polarity for electrodes above 0.40 C because the deposition of the electrode was facilitated. The effect of dilution with

Table 16—Recovery of Carbon from Bare and Coated Plain Carbon Steel Electrodes. Hare[44]

Carbon					Manganese			Silicon			
	In Bare Electrode		In Coated Electrode Deposit			In Bare Electrode	In Coated Electrode		In Bare Electrode	In Coated Electrode	
In Rod		Recovery %		Recovery %	In Rod	Deposit	Deposit	In Rod	Deposit	Deposit	Polarity of Electrode
0.03	0.02	67	0.03	100	0.09	0.09	0.11	0.01	0.01	0.01	Negative
0.13	0.03	23	0.03	23	0.44	0.16	0.28	0.01	0.01	0.01	Negative
0.39	0.15	38	0.24	62	1.08	0.40	0.39	0.15	0.07	0.06	Negative
0.65	0.24	37	0.39	60	0.68	0.46	0.55	0.16	0.02	0.05	Positive

Table 17—Recovery of Carbon from Bare Electrodes. Tichodeev[40]

Carbon			Manganese			Silicon		
In Rod	Recovery, %, in Drop Formed at End of Rod	Recovery, %, in Weld Deposit	In Rod	Recovery, %, in Drop Formed at End of Rod	Recovery, %, in Weld Deposit	In Rod	Recovery, %, in Drop Formed at End of Rod	Recovery, %, in Weld Deposit
0.07	100	57	0.42	21	19	0.22	14	0
0.25	48	40	0.46	22	30	0.21	5	0
0.48	62	40	0.51	22	47	0.21	19	0
0.12	75	42	1.03	21	25	0.20	0	0
0.10	100	40	0.48	34	16	0.54	22	0

Table 18—Effect of Arc Length on Carbon Recovery from Bare Electrodes (D.C.). Losana[48]

Arc Length, Inch	Carbon						Manganese		
	0.08 Inch		0.20 Inch		0.39 Inch		0.08	0.20	0.39
Composition of Electrode	Carbon Content of Deposit	Recovery, %	Carbon Content of Deposit	Recovery, %	Carbon Content of Deposit	Recovery, %	Recovery, %	Recovery, %	Recovery, %
0.21 C, 0.92 Mn, 0.18 Si, 0.16 Ni	0.092	44	0.034	16	0.018	9	11	4	1
0.41 C, 0.82 Mn, 0.19 Si	0.128	31	0.051	13	0.023	6	15	5	1

Table 19—Effect of Electrode Diameter on Carbon Recovery. Losana[48]

Diameter of Electrode, Inch	Electrode = 0.08 C, 0.32 Mn, 0.06 Si		Electrode = 0.19 C, 0.28 Mn, 0.13 Si		Electrode = 0.38 C, 0.46 Mn, 0.21 Si		Electrode = 0.62 C, 0.50 Mn, 0.17 Si	
	Carbon Content of Deposit	Carbon Recovery, %	Carbon Content of Deposit	Carbon Recovery, %	Carbon Content of Deposit	Carbon Recovery, %	Carbon Content of Deposit	Carbon Recovery, %
0.08	trace	0	0.05	26	0.06	16	0.068	11
0.16	0.075	94	0.096	51	0.11	29	0.11	18
0.24	0.114	>100	0.16	84	0.25	66	0.23	37

base metal is a factor that has not been adequately considered by the majority of investigators. Zeyen's results, Table 9, suggest that the effect is considerable, and the Page Steel & Wire Company[47] found that the two-layer deposit from a bare electrode containing 1.00 C on 0.42 C steel contained 0.63 C in the outer sections, and 0.57 C 1/8 inch below the surface.

The effect of arc length on the recovery of carbon from bare electrodes 0.16 inch diameter has been shown by Losana,[48] Table 18, to consist of a decrease in carbon recovery expressed as percentage of the carbon content of the electrode, as the arc is lengthened. The current was about 100 amp. at 0.08 inch arc length and 200 amp. at 0.39 inch. Carbon recovery was increased by increasing the diameter of the bare electrode, Table 19. Automatic welding was used, the arc length and current being maintained constant for a given diameter of electrode (no details). Contrary to most investigators, Losana found that carbon recovery decreased as the carbon content of the electrode increased. In other words, the weld puddle tended to reach an "equilibrium" low-carbon content regardless of the carbon content of the electrode. The favorable effect of increasing diameter on carbon recovery may be related to reduced exposure to oxidizing influences.

Coated Electrodes

The recovery of carbon from the rod with coated electrodes is usually higher than with bare electrodes, as Zeyen, Table 9, and Hare, Table 16, have shown. However, the nature of the coating is of controlling importance. For example, Losana[48] (see Table 21 of review of literature on Welding Manganese Steels) observed that the recovery of carbon from a waterglass coating of constant thickness containing graphite decreased as the percentage of carbon in the coating was increased. Okada[49] reported a recovery of only 15% from a rod containing 0.30 C covered with a carbonless slag. Goodger,[11] Table 20, also had little success in recovering carbon from an electrode coated with white asbestos and sodium salts using a short arc (140 to 150 amp., short arc). An increase in voltage or current decreased the carbon content of the deposit in Okamoto's[50] tests of a commercial coated electrode. An example of the effect of the same coating on the recovery of carbon from electrodes of different carbon contents is provided by Morris,[51] Table 21. The composition and thickness of the coating was not stated but all tests were made at 200 amp., 25 volts. The electrode containing 0.028 C was sluggish. The electrodes with 0.14–0.21 C were best, although the electrode containing 0.35 C could be used. It is significant that the low-carbon rods picked up carbon whereas the higher carbon rods lost carbon during deposition.

Table 20—Recovery of Carbon from Coated Electrodes. Goodger[11]

Carbon Content of Core Rod of Electrode	Manganese Content of Electrode	Carbon Content of Deposit	Recovery of Carbon, %
0.08	0.22	Trace	0
0.19	0.43	0.04	21
0.43	0.65	0.13	30
0.57	0.75	0.13	23
0.63	1.12	0.24	38
0.92	0.63	0.22	24
1.20	0.40	0.35	29

Atomic Hydrogen Welding

The recovery of carbon from rods containing 0.20–0.54 Si, 0.42–1.03 Mn in Tichodeev's[40] tests varied from 86% at 0.07 C to 67% at 0.48 C in the rod, the recovery of manganese being about 50% and of silicon from 25% at 0.20 Si in the rod to 70% at 0.54 Si. These results are in agreement with Weinman's[52] statement that with steel containing up to 1.25 C the loss of carbon is 25 to 30% depending largely on the amount deposited. Dorrat[53] also stated that atomic hydrogen filler rods for high-carbon steels should contain 20 to 30% more carbon than base metal.

Carbon Arc Welding

The carbon recovery in drops deposited from rods of the composition mentioned in the preceding section, according to Tichodeev[40] was 100% at 0.07 C in base metal (actually the drops picked up carbon and contained 0.08 C), and 60% at 0.48 C. The recovery of silicon was 0 to 20% and of manganese 20 to 30%.

Table 21—Carbon Recovery from Coated Electrodes. Morris[51]

Composition of Core Rod of Electrode			Composition of Deposit			Deposit Rate,
C	Mn	Si	C	Mn	Si	Lb./Hr.
0.028	0.18	0.006	0.14	0.39	0.16	4.0
0.13	0.45	0.009	0.15	0.52	0.20	3.3
0.14	0.51	0.010	0.14	0.55	0.21	3.6
0.21	0.57	0.015	0.14	0.57	0.19	3.6
0.28	0.59	0.03	0.18	0.60	0.20	3.9
0.35	0.83	0.14	0.20	0.75	0.26	4.6

FUSION WELDING PROCEDURE

In arc welding plain carbon steels with 0.40–0.60 C Jennings[54] recommends preheating (180–260° C.), stress relieving, or heat treating (790° C.). Steels with 0.60–0.80 C should be preheated to at least 260° C. and heat treated at 730–790° C. after welding. Puddling is advantageous. E. W. P. Smith[91] also preheated the higher carbon steels above 260° C., and cooled slowly. Steels with about 0.30 C, if welded without preheating, require special electrodes, or intermediate beads. Alexander[55] recommended preheating and the use of the atomic hydrogen process for steels containing 0.65 C. If the carbon content exceeds 0.80% according to Dawson,[56] there is danger of burning in oxyacetylene welding. As a general rule in aircraft welding, Johnson[57] found that carburized or decarburized surfaces create trouble in welding. A special electrode (no details) was relied upon by Brennecke[58] for welding steels containing 0.4–0.6 C, 0.6–0.7 Mn, 0.1–0.3 Si. The maximum hardness in the heat-affected zone obtained with the special electrode was 28 C Rockwell.

In repairing a broken crankshaft 6 1/4 inches diameter containing 0.32 C, 0.60 Mn, 0.12 Si by means of oxyacetylene welding, Ebersberger[59] used water cooling in the vicinity of the weld and reheated the weld by torch to 870° C. after it had cooled to 500° C. Weld and heat-affected zone were 140–144 Brinell; base metal was 148–159 Brinell. The DVMR notch-impact value of the weld was the same as base metal, the weld being made with a low-alloy steel rod (no details). The repair of cracked shoe dies (0.9 C steel) by oxyacetylene welding involves[60] veeing the crack, using a high-carbon steel rod 1/8 inch diameter, and neutral flame. The flame should be as small as possible. The welded die is heat treated as if it were unwelded.

For half-soling worn rail heads the American Transit Association[61] uses strips 1/4 to 1/2 inch thick, 1 1/2–2 1/4 inches wide (0.40–0.50 C, 0.50–0.70 Mn, 0.15–0.25 Si)

Table 22—Tensile Properties of Flash-Welded Plain Carbon Steels. Hughes[72]

Composition				Mechanical Properties of Weld			
C	Mn	P	S	Yield Strength, Lb./In.²	Tensile Strength, Lb./In.²	Elongation, % in 2 Inches	Reduction of Area, %
0.15	0.49	0.016	0.029	36,700	59,200	35	42
0.35	0.50	0.013	0.027	51,000	83,200	27	30
0.50	0.53	0.017	0.022	69,900	101,000	10	8
0.70	0.55	0.018	0.033	103,100	140,500	..	..

plug welded at 9–12 inch centers. The plug diameter is twice the thickness. A bare or coated electrode containing 0.13–0.18 C is used. If a high carbon electrode is used, the plug is so hard that the strip rapidly becomes corrugated under traffic. With the low-carbon electrode the plug is a little harder than the strip owing to the diffusion of carbon during welding. Although in 1935 a railway magazine[92] stated that failures had been observed where welds had been made across either the head or the base of driver-burned rails and recommended that burns outside the protection of the joints should not be welded, a later writer[93] chips out wheel burns to remove defective metal and welds without puddling. The weld (no details) was 10 Brinell units softer than the unwelded rail, and had higher strength and ductility in bending. Worn tires of mine locomotives are built up by Coal Age[106] with the aid of a $^3/_8$-inch coated electrode to supply the heat and a $^3/_4$-inch rod of high-carbon steel (0.80–0.90 C) to harden the deposit. The length of rod melted in with each electrode 14 inches long was $3^1/_2$ to 5 inches. The tires were annealed before cooling to avoid cracks. In the repair of broken parts of hardened steel so as not to soften the part, Armitage[62] deposits a layer of mild steel on the part to be built up, then adds successive layers of increasing hardness up to 630 Brinell. Unless the soft layers are used as basis, cracks will appear.

The effects of carbon on fusion welding phenomena do not appear to have been studied carefully. Leitner[43] automatically deposited a single bead of weld metal (bare electrode contained 0.09 C, 0.47 Mn, 0.009 P, 0.019 S, 0.027 O_2, 0.006 N_2, trace Si, 150–170 amp., 17–20 volts, 39 ft./hr., d.c., electrode negative) on plates 12 × 8 × 0.39 inch containing 0.13–0.16 Si, 0.48–0.69 Mn, 0.005–0.018 P, 0.033–0.044 S and 0.10, 0.20, 0.30, 0.40 and 0.50 C. The deposit on the lower carbon plates contained blow-holes. At 0.40 C the porosity had almost disappeared; at 0.50 C the deposit was entirely sound. No explanation was offered. On the other hand, Esslinger[64] observed that steels containing over 0.28 C were difficult to arc weld because "carbon is liberated from base metal to appear as CO in the deposit." Metal surrounding gas pockets in arc welds is usually decarburized, according to Jenks.[65] According to Séférian,[105] the upper limit of carbon content up to which there is no reaction of FeO with carbon to form CO with attendant frothing is 0.15 to 0.20% in the absence of silicon. Addition of manganese and particularly silicon raises the carbon content at which frothing is first observed. Larson[66] found that the gas from cellulose covered electrodes consists of about 40% CO, 50% H_2 with small quantities of CO_2 and H_2O. The carbon boil can be distinguished by its effervescence from the heaving action of

Table 23—Mechanical Properties of Flash-Welded Plain Carbon Steels. Vér[73]

Tensile Tests (two specimens for each determination)

Carbon Content, %	Yield Strength, Lb./In.²		Tensile Strength, Lb./In.²		Reduction of Area, %		Elongation, % in 3.2 Inches		Elongation, % in 0.79 Inch at Weld
	Unwelded	Welded	Unwelded	Welded	Unwelded	Welded	Unwelded	Welded	
0.05	41,000	40,000–41,000	50,000	50,000	75	74–75	33–37	12–28	3.5–17.5
0.08	43,000–44,000	44,000–45,500	56,500	55,000–56,000	68–70	68–71	32	26	12.5–15.0
0.24	50,000–51,000	50,000–51,000	68,000	69,000	65	65–66	26–27	21–23	8.0–9.0
0.31	54,000	54,000	81,000	81,000	45–47	48–50	23–24	18–21	7.0
0.56	58,000	60,000–61,000	98,000	98,000–100,000	28–36	36	18–20	12–14	3.5–4.0
0.64	60,000	58,000–59,000	102,000–104,000	102,000–104,000	36	36–45	18	13	1.6–2.5
0.86	67,000–74,000	67,000–74,000	135,000–137,000	134,000–136,000	18–21	21–22	11–13	5–6	1.5

Vickers Hardness Explorations and Rotating Bend Fatigue Limits

Composition			Vickers Hardness Maximum in Heat-Affected Zone	Unwelded Water-Quenched from 900° C.	Endurance Limit (10 × 10^6 Cycles), Lb./In.²		Ratio Welded to Un-Welded Endurance Limits	Ratio of Welded Endurance Limit to Tensile Strength
C	Mn	Si	Unwelded		Unwelded	Welded		
0.05	0.30	0.01	110	143	157	32,800	>29,200	..	..
0.08	0.39	0.01	124	160	175	36,300†	28,500	0.78	0.51
0.24	0.51	0.08	155	195	290	37,000	33,400	0.9	0.50
0.31	0.31	0.17	160	212	570	37,700	28,500	0.75	0.37
0.56	0.30	0.19	212	274	840	42,700	33,400	0.78	0.34
0.64	0.27	0.21	220	278	845	44,100†	29,900	0.67	0.29
0.86	0.35	0.15	274	376	935	52,600	39,100	0.74	0.28

* S = 0.03, P = 0.01–0.02 in all steels.
† Estimated from S-N diagram.

the manganese boil in the weld puddle, according to an Australian welder.[67] Ronay[68] stated that a copper coating is essential on oxyacetylene rods containing over 0.06–0.08 C to prevent the deposition of porous weld metal. Hare[44] believed that penetration is reduced if the electrode contains over 0.18 C.

The arc from electrodes containing 0.50 C and over is made unstable by the oxidation of carbon in the plasma, according to Thomas.[69] A covering on the electrodes prevents oxidation. The voltage distribution in the d.c. iron arc, according to Rapatz,[70] is changed by increasing carbon content so that with high carbon content (no details) the voltage drop at the positive pole becomes less than that at the negative pole. He is of the opinion that carbon in the plasma is in atomic form, and states the well-known rule[8] that high-carbon electrodes should be connected to the positive pole to obtain a smooth, nonporous deposit. The embedding of diamonds in steel disks by arc welding to form diamond saws is described by Kennedy.[71]

RESISTANCE WELDING

Several investigations have been made of the mechanical properties of a series of resistance welded plain carbon steels. Flash welds ground to $^1/_2$ inch diameter for a distance of 2 inches on either side of the weld were tested by Hughes,[72] Table 22, at a speed of $^1/_8$ inch/min. All specimens broke outside the weld at a strength slightly less than that of the unwelded steel. With 0.50 C steel the Rockwell B hardness of weld, heat-affected zone and unwelded base metal was 80.5, 98 and 80.5, respectively.

Rolled bars 0.59 inch diameter were flash welded by Vér,[73] Table 23, in a 25 kw. machine and were machined to tension specimens 0.32 inch diameter, 5.5 inches long, and Farmer rotating bend fatigue specimens 0.32 inch diameter, about $1^1/_4$ inches parallel section. All tensile specimens broke outside the weld. The maximum increase in hardness in the heat-affected zone increased from about 40 Vickers units at 0.05–0.24 C to 110 units at 0.86 C. The ratio of endurance limit to tensile strength for the welded specimens decreased from 0.5 for the low-carbon specimens to 0.3 for the high-carbon specimens. However, the ratio of endurance limits of welded to unwelded specimens was about 0.75 regardless of carbon content.

The heat-affected zone revealed by etching increased from about 0.15 inch back of the weld with 0.05 C to 0.75 inch with 0.86 C, largely because the temperature limit for structural change decreases as the carbon content is increased, according to Vér. Although the grain size in the heat-affected zone was coarse, no martensite was observed. The hardness of the junction line was not higher than that of unwelded base metal for some reason.

The effect of heat treatment on flash and resistance butt welds in a series of plain carbon steels was found by Hasa and Benes[74] to be beneficial in raising the notch-impact value, Table 24. The notch-impact specimen was notched at the weld and was 0.71 inch square (span = 4 inches), the notch having a radius of 0.08 inch and being 0.20 inch deep. Tensile specimens were 0.79 inch diameter with 1-inch parallel section.

The authors stated that heat treatment raised the tensile strength of the weld to that of unwelded base metal. The reduction of area of the welds in the higher carbon steels (1.03 and 1.28 C) was nil. The highest average reduction of area in the flash welds was:

0.21 C steel—42.5% (unwelded = 60%)
0.43 C steel—20% (unwelded = 56%)
0.73 C steel—10% (unwelded = 24%).

The resistance butt welds were less ductile than the flash welds. If the steels were spheroidized before welding the notch-impact value of the welds was not significantly changed, but the as-welded flash welds in the higher carbon steels (1.03 and 1.28 C) had 10% reduction of area. The maximum Rockwell hardness of the welds occurred at the middle of the weld, contrary to Vér, and was:

0.43 C steel—105 B
0.73 C steel—37 C
1.03 and 1.28 C steels—40 C.

The grain structure of the unannealed welds appeared to be unusually coarse.

The mechanical properties and microstructure of resistance butt welds in steel $^5/_8$ inch diameter containing 0.97 C were studied by Thum,[75] Table 25. Although the welded specimens were machined to a diameter of $^1/_2$ inch for a distance of only $^1/_2$ inch over the weld, failure occurred in the fillets away from the unannealed weld. The annealed specimens fractured through the weld on account of decarburization. The steel at the weld contained only 0.51–0.57 C, the decarburization being most serious at the axis of the bar. Troostite and martensite were observed in the unannealed weld.

Among numerous references to the commercial resistance welding of high-carbon steel is Woofter's[76] statement that high-carbon razor steel strip is commonly flash welded into continuous lengths. In flash welding steel containing over 0.50 C, Woofter[69] emphasized the importance of rapidity and of immediate normalizing. However, to reduce brittleness, Remington[94] adjusted pressure and power in spot welding high-carbon steel so that the heat input was large. For thin material, such as band saws, the annealing may be done in the welding machine by moving the jaws apart after welding to expose bright surface, then switching on the current until the desired temper color is obtained. The same type of annealing is recommended by Donald[69] for rock drills

Table 24—Notch-Impact Value (Mkg./Cm.^{2}) of Flash and Resistance Butt-Welded Plain Carbon Steels. Hasa and Benes[74]

Composition			Unwelded Base	Flash Welds			Resistance Butt Welds		
					Heat Treated*			Heat Treated	
C	Mn	Si	Metal	As-Welded	10 Min.	1 Hr.	As-Welded	10 Min.	1 Hr.
0.21	0.78	0.23	18.5	4.3	11.6	14.1	2.2	8.1	8.4
0.43	0.60	0.30	7.2	0.8	4.9	4.3	0.8	6.0	4.3
0.73	0.31	0.21	2.2	0.8	2.5	2.7	0.9	2.4	2.5
1.03	0.28	0.15	1.0	0.7	..	..	0.8	...	...
1.28	0.29	0.21	1.0	0.7	..	..	0.6	...	...

* The following heat treating temperatures were used:
800° C. for 0.73 C steel
850° C. for 0.43 C steel
900° C. for 0.21 C steel

(0.60–1.10 C) that are too long for the average furnace and by Meebold[77] for resistance butt-welded high-carbon steel wire to prevent brittleness. Cooling after reheating is in lime. Annealing in the welding machine was inadequate to prevent wire containing over 0.64 C from breaking during subsequent drawing through five dies, according to a writer in *Product Engineering*[95] who tested piano wire containing 0.65, 0.85 and 0.90 C. Success with the 0.65 C wire was obtained by annealing the ends (9 to 12 inches) in a coal fire before welding, and by heating to a blood red heat in the machine after resistance welding. The 0.85 C wire was annealed before and after welding and, in addition, the cross-section at the weld was reduced a small amount to decrease the drawing strain at the weld. With all these precautions an average of only 20% of the welds in 0.90 C wire could be drawn without breakage. In a discussion of the problem L. H. Frost emphasized the importance of precise control and prolonged annealing in treating high-carbon steel wire, and a process in which the welding was performed under carbon tetrachloride was mentioned.

Füchsel[78] found that carbon content up to at least 0.30% had no effect on resistance butt welding of plain carbon steels, and in Burton's[79] tests (no details) resistance butt welds in steels containing up to 0.65 C had the same strength as unwelded bars. Above 0.50 C failures occurred in the weld with only slight elongation. The absence of burning in high-carbon resistance butt welds in contrast with forge welds was attributed to the short duration of welding and to the working of the steel under the welding pressure. Burton did not believe that the carbon content of resistance welds differed from unaffected metal and attributed the peculiarities of microstructure in resistance welded high-carbon steel to the appearance of troostite.

Table 25—Mechanical Properties of Resistance Butt Welds in 0.97 C Steel. Thum[75]

Specimen	Tensile Strength, Lb./In.²	Elongation, % in 1/2 Inch	Reduction of Area, %
Unwelded	114,100	10	12
Welded, not annealed	158,700	3	2
Welded, annealed at 750° C.	100,800	16	24

A comparative study of spot welds (welding time = 0.02 min; electrode diameter = 0.39 inch; electrode pressure = 970 lb.) in three plain carbon steels 0.20 inch thick by Dussourd,[80] Table 26, showed that strong spot welds can be made in 0.42 C steel but not in 0.83 C steel. The familiar shear fracture of spot welds in low-carbon steel was replaced in the higher carbon steels by a brittle fracture through the heat-affected zone, a lenticular piece of metal representing the fused zone sometimes separating from the fractured specimen. Furnace annealing raised the strength (based on area of sheared spot) of spot welds in 0.83 C steel to 75,000 lb./in.² Reheating between the electrodes had a good effect on spot welds in 0.43 C steel. The structure of the as-welded spot welds in 0.43 C and 0.83 C steels was martensitic. In 1922[81] there was no difficulty in spot welding steel containing 0.50–0.65 C but the welds were glass hard. Low currents and long welding times were used to reduce the quenching effect.

To attach a sheet of steel 0.031 inch thick, 15 x 16 inches, containing 0.80 C, 0.29 Mn, 0.17 Si, to a bar of low-carbon steel (0.20 C, 0.49 Mn, 0.08 Si, 3/8 x 1/2 x 12 inches), Hudson[97] drilled holes 13/64 inch diameter on 1-inch centers through the center of the 1/2 inch side of the bar. Scale was ground from the sheets, because it increased the voltage and caused holes to be melted in the sheet. Pins containing 0.06 C, 0.08 Mn, 0.04 Si 3/16 inch diameter, 1/2 inch long, with small projections on the ends were inserted in the holes and were spot welded (600 amp., 1.6 volts, 11,500 lb./in.² on pin, 3 seconds). There was no evidence of burning, and only a negligible amount of martensite was observed. Improperly executed welds contained considerable amounts of martensite in which brittle fracture always occurred in tensile tests. In direct tension each pin weld withstood 342-368 lb. before failure. Hudson found that pin welds on lower carbon steel required higher values of current, pressure and time.

FORGE WELDING

Carbon content is an important factor in forge and water gas welding. In Sweden, for example, the carbon content of steel for water gas welded boiler drums is limited to 0.12–0.15%, according to Dorey.[36] Fry[82] states that the pressure welding of high-carbon steel is difficult and should not be attempted, because its low melting point prevents the attainment of a sufficiently high temperature to weld the decarburized edges. In 1919 Abbott[83] concluded that carbon became a deterrent to good forge welding when the content exceeded 0.18%, the aim generally being 0.08–0.12 C with 0.30–0.60 Mn. Speller[84] in 1922 limited the carbon content of steel for forge welding to less than 0.30% and more recently to not over 0.22 C in accordance with A. S. T. M. Specification A-78-33. Among older investigators the effect of carbon on forge welding was not understood. Holley[85] (1877) found that forge welding became extremely difficult at 0.35 C. Böhme[86] (1883), however, secured good forge welds in a material containing 0.37

Table 26—Spot Welds in Plain Carbon Steels. Dussourd[80]

Composition of Steel			Tensile Strength, Unwelded	Shear Strength of Spot Weld Based on Area of Sheared Spot, Lb./In.²				Shear Strength of Spot Weld Based on Electrode Area, Lb./In.²				Maximum Brinell Hardness*				
C	Mn	Si	Lb./In.²	1	2	3	4	1	2	3	4	1	2	3	4	5
0.08	0.23	0.34	59,500	62,000	53,500	....	....	86,500	65,000	....	....	130	...	...	...	105
0.42	0.48	0.26	90,500	37,300	48,500	65,500	46,500	49,500	65,500	84,000	58,500	550	...	360	420	180
0.83	0.22	0.30	134,000	8,500	....	21,000	31,000	11,000	....	30,500	34,000	665	510	630	425	185

* Converted from Rockwell C.

1. As-welded.
2. Reheated between the electrodes at 27.5 kva.
3. Reheated between the electrodes at 36.7 kva.
4. Reheated between the electrodes at 50 kva.
5. Unwelded.

The steels contained less than 0.01 S, 0.01 P.

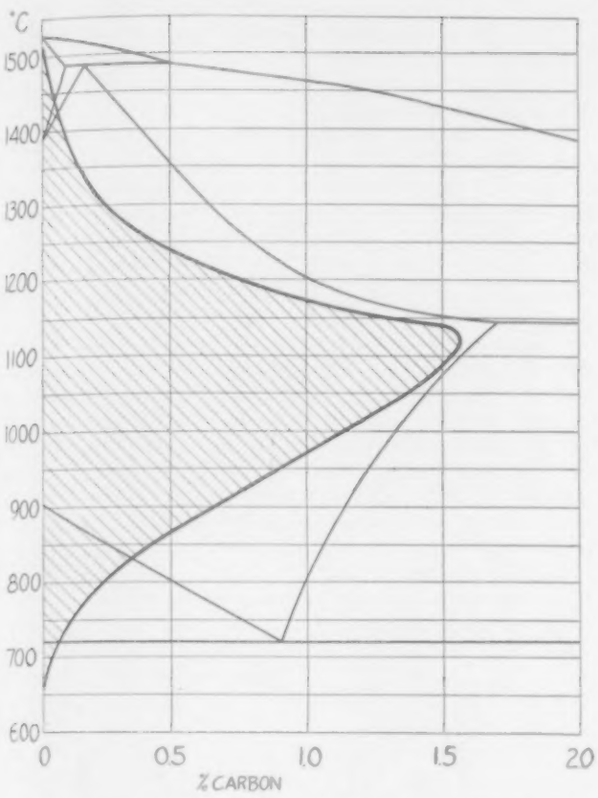


Fig. 4—The Cross Hatched Area Is the Region in Which Pressure Welds Can Be Made in Iron-Carbon Alloys. Esser[87]

C, 0.16 Si, 0.12 Mn, 0.02 P. The tensile strength of the welds was 62 to 100% of unwelded base metal but the reduction of area was 6 to 14% for the weld, 40 to 47% for unwelded. Böhme believed that carbon content had much less effect on forge welding than molecular constitution (absence of plastic behavior at welding temperature).

Some insight into the mechanism of pressure welding without fusion is supplied by Esser,[87] whose results, Fig. 4, were based on four steels containing 0.06 to 0.75 C, 0.01–0.10 Si, 0.11–0.12 Mn, 0.01 P, 0.01–0.018 S. The shaded region represents compositions and temperatures which result in appreciable strength across the junction between two plane surfaces held at temperature in vacuum under a pressure of 2800 lb./in.^{2} (time not stated). Under these conditions a weld in steel containing 0.75 C made at 1000° C. had a tensile strength of 66,000 lb./in.^{2} It has been found[88] that the forge welding of bars 1½ inches diameter containing 0.18 C was difficult if the bar had an overheated Widmannstätten structure, but was satisfactory if the bar had a fine-grained, normalized structure.

OXYGEN CUTTING

Since a thorough discussion of the effect of carbon content on the oxygen cutting of higher carbon steels appeared in the review of literature on Oxygen Cutting—Part I (American Welding Society Journal, 18 (7) Suppl., 217–248 (1939)), only a summary of the important facts is presented herewith. The preheating temperature for steel containing over 0.30 C should be 260–320° C. (500–600° F.) but not usually above 600° C. (1100° F.). Preheating prevents checking or cracking and saves gases. If the preheating temperature is too high the cut is difficult to complete. An increase in carbon content decreases the oxygen cutting speed. The oxygen pressure at the regulator in lance cutting should be 25 lb./in.^{2} higher for low-carbon steel than for medium-carbon steel up to 0.5 C, according to Thum.[89]

ACKNOWLEDGMENTS

Contributions to the review have been received from:

R. H. Aborn, United States Steel Corporation.
G. R. Brophy, International Nickel Company, Inc.
A. DiGiulio, University of Detroit.
R. G. Hudson, Massachusetts Institute of Technology.
C. F. Keel, Basel, Switzerland.
W. H. Simon, Sheffield, England.
E. W. P. Smith, Lincoln Electric Company.
W. A. Spindler, University of Michigan.
G. H. Tinker, Nickel Plate Road.

BIBLIOGRAPHY

1. Theisinger, W. G., *Iron Age*, **139**, 28–31, 115, Feb. 28; 48–53, March 4, 1937; **138**, 75–76, Dec. 10, 1936; *Rwy. Mech. Engg.*, **112**, 176–181 (1938); Doctor's Thesis, Harvard Univ., May 1, 1935.
2. Armstrong, T. N., *Trans. Amer. Soc. Metals*, **24**, 567–584 (1936).
3. Czyrski, W., *Spawanie i ciecie Met.*, **10**, 176–185 (1937).
4. Zeyen, K. L., Iron & Steel Inst. Weld. Symposium, II, 617–633 (1935).
5. Hibbard, F. G., *AERA Engg. Assn.*, 362–363, 398–400 (1930).
6. Csilléry, D., and Péter, L., *Organ Fortschr. Eisenbahnwes.*, **92**, 409–416 (1937).
7. Pomp, A., *Stahl u. Eisen als Werkstoff*, **II**, 97–101 (1928).
8. Rapatz, F., *Stahl. u. Eisen als Werkstoff*, **II**, 80–85 (1928).
9. Roberts, A. T., Iron & Steel Inst. Weld. Symposium, II, 531–537 (1935).
10. Schmitt, H., Dissertation, Verlag F. Vieweg, Braunschweig (1927).
11. Goodger, A. H., *Weld. Engr.*, **13** (2) 39–43 (1928).
12. Wright, J., *Weld. Ind.*, **6**, 292–296 (1938).
13. Zeyen, K. L., *Techn. Mitt. Krupp*, **4**, 83–89 (1936); *Stahl u. Eisen*, **56**, 654–657 (1936).
14. Aysslinger, H., *Mitt. Forsch. Anst. GHH Konzern*, **5**, 112–124 (1937).
15. Stursberg, E., *Autog. Metallb.*, **30**, 353–361, 369–380 (1937).
16. Nestor, M. B., The Effect of Annealing Above and Below the Critical Points on Welds of Low- and High-Carbon Steels, Met. Engr. Thesis, Univ. Minnesota, 56 pp., April 1934.
17. Hodge, J. C., American Welding Society Journal, **15** (1) 6–14 (1936).
18. Rapatz, F., Iron & Steel Inst. Weld. Symposium, II, 507–516 (1935).
19. Committee on Welded Rail Joints, American Welding Society, Final Report, 358 pp. (1932).
20. Cook, M., *Oil & Gas Jnl.*, **35**, 33–40, May 28, 1936.
21. Belaiew, N. T., and Séférian, D., *Twelfth Intnl. Acet. Cong., London*, **3**, 577–587 (1936).
22. Lavroff, S. J., *TZ prakt. Metallbearb.*, (20) 1929.
23. Brophy, G. R., American Welding Society Journal, **6** (9) 67–76 (1927).
24. Hatfield, W. H., Iron & Steel Inst. Weld. Symposium, II, 363–371 (1935).
25. Critchett, J. H., American Welding Society Journal, **17** (1), Suppl., 8–14 (1938).
26. Kinzel, A. B., American Welding Society Journal, **15** (5), 24–27, (10) 76–78 (1936).
27. Diegel, C., *Forscharb. Geb. Ingwes.*, **346**, 44 pp. (1922).
28. *Métallurgie*, **68** (10FP), 17–23 (1936).
29. Bierett, G., *Elektroschw.*, **8**, 148–152 (1937).
30. Münter, H., *Schmelzschw.*, **11**, 33–35 (1932).
31. Sommer, F., *Autog. Metallb.*, **26**, 22–27 (1930); Sommer, F., and Hoffmann, W., *Zwanglose Mitt. Fach. Schweisstech. VDI*, (25), 9–12, Dec. 1934.
32. Hayes, W. H., *Transit Jnl.*, **79**, 456 (1935).
33. Meunier, F., and Rosenthal, D., *Rev. Mét.*, **33**, 588–593 (1936); *Assn. Ing. Ecole Mines Mons*, **59** (4), 35 pp. (1936).
34. Heyn, E., *Mitt. Königliches Materialprüfungsamt*, **31**, 168–173 (1913).
35. Egloff, G., Morrell, J. C., and Leonhardy, E. C., *Ind. & Engg. Chem.*, **24**, 1264–1275 (1932).
36. Dorey, S. F., *Jnl. Instn. Civil Engrs.*, (6) 621–666, April 1937.
37. Hodge, J. C., American Welding Society Journal, **10** (4), 11–15 (1931).
38. *Proc. Amer. Rwy. Engg. Assn.*, **34**, 288–298 (1933).
39. Keel, C. F., *Z. Schweisstech.*, **24**, 183–190 (1934).
40. Tichodeev, G. M., American Welding Society Journal, **15** (3), 26–31 (1936).
41. Portevin, A. M., and Leroy, A., Iron & Steel Inst. Weld. Symposium, II, 475–481 (1935).
42. Mercier, M., *Rev. Mét.*, **33**, 553–555 (1936).
43. Picard, C., *Chaleur et Ind.*, **19**, 140–144 (1938).
44. Hare, K. R., *Weld. Engr.*, **13**(3), 30–32; (4), 43–46 (1928).
45. Pilarczyk, J., *Przeglad Mechaniczny*, **1**, 15–19 (1935).
46. Hoffmann, W., *Z. VDI*, **72**, 215–218 (1928).
47. *Rwy. Mech. Engr.*, **97**, 660 (1923).
48. Losana, L., *Metallurgia Italiana*, **26**, 391–403 (1934).
49. Okada, M., *Soc. Mech. Engrs. Japan, Jnl.*, **37**, 705–710 (1934).
50. Okamoto, T., Nishimura, H., and Katayama, H., *Yosetsu-Kyokwai-Shi*, **7**, 403–405 (1937).
51. Morris, R. J., The Effect of Carbon upon the Welding Properties of Electrodes, Thesis No. 404, B. S. Met. Engg., Case School of Applied Science, 40 pp. (1936).
52. Weinman, R. A., *General Electric Review*, **32**, 532–537 (1929).
53. Dorrat, J. A., *Weld. Jnl. (London)*, **20**, 6–14 (1933).
54. Jennings, C. H., *How to Weld 29 Metals*, Westinghouse Elec. & Mfg. Co., 100 pp. (1937).

55. Alexander, P., American Welding Society Journal, **11** (2), 8 (1932).
56. Dawson, J. R., *Trans. Amer. Soc. Steel Treat.*, **4**, 467–487 (1923)
57. Johnson, J. B., *Airplane Welding*, Goodheart-Wilcox Co., Inc., Chicago (1929).
58. Brennecke R., *Elektroschw.*, **7**, 151–155 (1936).
59. Ebertsberger, H., *Autog. Metallb.*, **30**, 401–404 (1937).
60. *Oxy-Acetylene Tips*, **5**, 77–78 (1926).
61. *Proc. Amer. Transit Assn. & Affil. Orgs.*, 804–817 (1937.
62. Armitage, R., *Jnl. S. African Instn. Engrs.*, **36**, 218–2[illegible]8).
63. Leitner, F., *Arch. Eisenhüttenwes.*, **7**, 311–314 (1933/34).
64. Esslinger, F. J., American Welding Society Journal, **16** (1), 20–21 (1937.)
65. Jenks, G. F., A. S. T. M. Sympos. on Welding, 86–103 (1933).
66. Larson, L. J., American Welding Society Journal, **15** (10), 14–20 (1936).
67. *Mech. & Weld. Engr.*, **3**, 156–157 (1929).
68. Ronay, B., *Jnl. Amer. Soc. Naval Engrs.*, **48**, 532–552 (1936).
69. Thomas, R. D., Evleth, E. M., Donald, R. S., and Woofter, H. A., American Welding Society Journal, **12** (3), 30 (1933).
70. Rapatz, F., *Stahl u. Eisen*, **51**, 245–253 (1931).
71. Kennedy, R. S., *Proc. Instn. Mech. Engrs.*, 106–108 (1919); Stanley, G. H., and Wilson, D. A. P., *S. African Chem. Met. & Min. Soc. Jnl.*, **38**, 365–379, 390–396 (1938).
72. Hughes, G. A., *Weld. Engr.*, **16** (7), 33–36 (1931).
73. Vér, T., *Carnegie Schol. Mem.*, **22**, 135–156 (1933).
74. Hasa, F., and Beneš, A., *Masarykovy Akad. Práce, Sbornik*, **8** (1), 29–85 (1935).
75. Thum, E. E., *Chem. & Met. Engg.*, **19**, 301–308 (1918).
76. Woofter, H. A., *Iron & Steel Engr.*, **13**, 31–32, Nov. 1936.
77. Meebold, *Draht Welt*, **30**, 316–318 (1937).
78. Fuchsel, *Glasers Annalen*, **94**, 85–93 (1924).
79. Burton, F., American Welding Society Journal, **9** (9), 78–87 (1930).
80. Dussourd, E. J. L., *Rev. Mét.*, **33**, 171–181, 210–214 (1935).
81. Remington, W., Chamberlain, J. E., and Osborne, J. A., American Welding Society Journal, **1** (1), 38–40 (1922).
82. Fry, A., *Kruppsche Monatsh.*, **12**, 201–214 (1931).
83. Abbott, E. E., *Proc. A. S. T. M.*, **19**, Part I, 150–153 (1919).
84. Speller, F. N., *Trans. A. S. M. E.*, **44**, 399–407 (1922).
85. Holley, A. L., *Trans. A. I. M. E.*, **6**, 101–124 (1877).
86. Böhme, *Mitt. köngl. techn. Versuchsanstalt*, **1**, 70–75 (1883).
87. Esser, H., *Arch. Eisenhüttenwes.*, **4**, 199–206 (1930–31).
88. *Iron & Steel Ind.*, **8**, 412–414 (1935).
89. Thum, E. E., *Heat Treat. & Forg.*, **12**, 57–59 (1926).
90. Keel, C. F., *Z. Schweisstechnik*, **27**, 168–176 (1937).
91. Smith, E. W. P., *Weld. Engr.*, **24** (1), 28–29 (1939).
92. *Railway Engg. & Maintenance*, **31**, 36 (1935).
93. *Railway Engg. & Maintenance*, **35**, 170, March 1939.
94. Remington, W., American Welding Society Journal, **3** (9), 23–26 (1924).
95. *Product Engineering*, **9**, 149, 187, 308–309 (1938).
96. Gerbeaux, H., *Bull. Société Ing. Soud.*, **3**, 563–582 (1932).
97. Hudson, R. G., *Electrical World*, **88**, 375 (1926).
98. Hodge, J. C., Iron & Steel Inst. Welding Symposium, II, 391–417 (1935).
99. Rolfe, R. T., *Ibid.*, 539–552.
100. Moses, A. J., American Welding Society Journal, **14** (4), 15 (1935).
101. Atkins, E. A., Electric Arc and Oxy-Acetylene Welding, First Edition, Pitman, London, 1923, p. 197; Ruder, W. E., *Jnl. Franklin Inst.*, **192**, 569 (1921).
102. Busch, H., *Mitt. Forschungsanst. GHH Konzern* (1), 1–14, Jan. 1939; *Elektroschwg.*, **10** (1), 9–17, Jan. 1939.
103. Wilkinson, T. B., and O'Neill, H., *Instn. Mech. Engrs. Jnl.*, 10–11, Jan. 1939; Advance Copy of Paper: Observations on the Arc Welding and Gas Cutting of High Tensile Low-Alloy Structural Steels, 18 pp., March 1939.
104. Schuster, L. W., *Iron & Steel Inst. Jnl.*, **124**, 183–185 (1931); *British Engine, Boiler & Elec. Ins. Co. Technical Report*, 1931, p. 81; Hoyt, S. L., *Trans. Amer. Soc. Metals*, **23**, 61–84 (1935).
105. Séférian, D., *Rev. Soud. Autog.*, **30**, 558–561 (1938).
106. *Coal Age*, **43** (10), 67 (1938).

SUGGESTED RESEARCH PROBLEMS

1. Porosity in welds sometimes is attributed to carbon content. Reaction between carbon and oxygen (FeO) in the weld metal in the absence of sufficient deoxidation by silicon, aluminum or other elements provides carbon monoxide which may be trapped in the solidifying weld metal. Leitner's[63] tests should be repeated using different types of covered electrodes, and base metal containing different amounts of carbon, silicon, manganese, and other alloying elements. In Leitner's tests with low-carbon bare electrodes porosity in the deposit seemed to disappear beyond about 0.40 C in the base metal. Leitner used only one welding speed and one size of electrode.

2. The arc welding of pure iron in different atmospheres has been achieved, but pure iron-carbon alloys have not been welded. Tests made on open-hearth steels contain no silicon, low carbon, unusually low manganese (0.17%) and 0.23 Cu exhibited no peculiarities. The arc and resistance welding of steels with higher carbon and lower manganese contents should be investigated both in air and in special atmospheres.

3. The physical properties of welds should be determined in a series of plain carbon steels having a wide range of carbon contents. Filler rods that are designed to deposit weld metal of the same composition as base metal should be used. Heat treatment of the welds should be studied.

4. The welding of steel often creates difficulties, but the permissible temperature for welding under different sets of (adverse) conditions has not been determined. Tests should be made to estimate the effect of temperature of base metal from −20 to +20° C. (but up to 500° C. also is necessary) on the structure (hardness, cracks) and properties (strength and ductility) of beads on, and welds in, a series of plain carbon steels, 0.10–0.90 C, made by metal arc, gas carbon arc, and resistance processes at various speeds. Similar tests should be conducted on oxygen cutting and oxygen machining at different speeds. The importance of strict regulation of all variables, such as thickness of metal, size of flame and filler rod, is emphasized. Perhaps tests in which the base metal at different temperatures is melted to different depths and allowed to cool would yield comparative results confirming the welding tests.

5. The properties and structure of atomic hydrogen welds in thin sections of higher carbon steels (over 0.35 C) should be investigated.

6. The welding of graphitic steels has not been investigated. Resistance welding and surfacing by means of fusion welding appear to be the important applications requiring study.

7. Sometimes a band of pearlite is observed at the fusion line of welds in low-carbon steel, particularly after normalizing. Several explanations have been proposed but none has been established. According to one explanation the band is not pearlite but sorbite, which creates a false impression of high carbon content. Others believe that the carbonless zone has a high oxygen content (Thompson-Willows theory). Still others point out that the weld metal usually has a lower carbon content than base metal. During cooling ferrite forms first on the low-carbon side of the fusion line and pushes the higher carbon austenite before it. Finally, it has been thought that the carbon in those zones of base metal near the weld and heated below the critical temperature diffuses into the austenitic regions by reason of the higher solubility of carbon in austenite than in ferrite (carbon solubility differential). Since the band of pearlite possibly may exert a slight effect on ductility, an inquiry into its cause and remedy would be useful.

8. An investigation should be made of the factors (arc length, size of puddle, plate thickness, speed of welding) affecting recovery and pick-up of carbon in welding with electrodes having carbonaceous coatings, with reducing carbonaceous flames, and with the carbon arc. It has been said without proof that oxidation of carbon in the plasma of an iron arc decreases its stability.

9. Experiments should be made on the best treatment to refine the grain structure of flash welds in medium-carbon steel.

10. The metallurgical phenomena in the partly fused zone in welding high-carbon steel with low-carbon filler rods should be demonstrated. The general problem of welding materials of lower melting point than the filler rod has not been solved completely. Usually there is no evidence of a partly fused zone in the structure perhaps because the heat of welding is extremely localized.

11. The properties and structure of fusion and resistance welds in the higher carbon grades of concrete reinforcing steel should be determined.

THE ENGINEERING FOUNDATION
Welding Research Committee

Sponsored by the American Welding Society « « » » and American Institute of Electrical Engineers

Supplement to the Journal of the American Welding Society, September 1939

WELDING COPPER STEELS

A Review of the Literature to July 1, 1937

By W. SPRARAGEN* and G. E. CLAUSSEN†

This report is prepared under the auspices of

THE LITERATURE DIVISION OF THE ENGINEERING FOUNDATION
WELDING RESEARCH COMMITTEE

TABLE OF CONTENTS

	Page
Summary	289-s
Introduction	290-s
Welding Copper Steels	
Mechanical Properties of Welds	291-s
General Observations	293-s
Oxygen Cutting	294-s
Resistance Welding	294-s
Forge Welding	294-s
Recovery and Pick-Up	294-s
Welding Copper to Steel	294-s
Welding Copper-Chromium Steels	295-s
Welding Copper-Nickel Steels	296-s
Welding Copper-Molybdenum Steels	298-s
Welding Copper-Nickel-Molybdenum Steels	299-s
Welding Copper-Chromium-Molybdenum Steels	300-s
Welding Copper-Nickel-Chromium Steels	300-s
Acknowledgments	300-s
Bibliography	300-s
Suggested Research Problems	301-s

Summary

Mechanical Properties of Welds

Oxyacetylene X butt welds made with copper steel filler rods in a steel having a tensile strength of 60,700 lb./in.², elongation 26.7% in 8 inches, had 90% of the strength of the unwelded plate, were 7 to 50% stronger and their ductility was much better than those made with plain carbon steel rods. An increase in carbon content from 0.06 to 0.20% in the weld rod increased the tensile strength, while an increase in silicon raised the ductility. Heat treatment was of no advantage.

Welds in plates 1.50 inches thick containing 0.24 C, 0.64 Cu made with a covered electrode containing 0.7 Cu, 0.2 Mo and some nickel, after being given a "stabilizing anneal" at 640 to 660° C. had excellent physical properties. The bend angle was 180° at room temperature, and the notch-impact value was 14.5 to 16.5 mkg./cm.² in base metal, 12.7 to 15.4 in heat-affected zone and 12.5 to 17.5 in weld. The ductility and strength of arc and oxyacetylene welds are satisfactory up to 0.75 Cu (0.10 C), beyond which both properties decrease rapidly compared with unwelded base metal. Shielded arc and oxyacetylene welds in four commercial low-alloy steel sheets containing copper had good tensile and notch-impact values. The carbon content of the steels was below 0.08 per cent.

Resistance butt welding of copper steels is satisfactory up to at least 2% Cu.

General Observations

Steels with up to 3% Cu have been successfully fusion welded without defects. With 4% Cu the weld metal cracks in the direction of welding as if it were intensely hot short. Welding rods for steel often contain copper. Copper plated rods are commonly used for oxyacetylene welding. Iron containing 7% Cu, or 1 to 3% Cu with 0.5 to 1% Al, or 3% Cu with 0.5% Si has been successfully welded.

Recovery and Pick-Up

The recovery of copper from covered electrodes is practically 100%.

Welding Copper to Steel

Copper and its alloys may be welded to steel, may be deposited as a surface layer on steel, or may be used as the bonding material to join steel parts. Copper rail bonds are sometimes welded to the rail. In arc welding flux covered wire containing deoxidizers is used. In gas welding, a little tin and some silver or phosphorus are used as deoxidizers. In carbon arc welding, phosphor bronze is used in welding copper to steel. Copper has been flash welded to iron. Copper brazing utilizes copper

*Secretary, Welding Research Committee.
†Research Assistant, Welding Research Committee.

to maintain a clean steel surface and depends on (1) alloying of copper with steel, (2) alloying of steel with copper, (3) penetration of copper along the grain boundaries, (4) growth of iron grains across the joint. The atomic hydrogen process can be used to deposit copper on low-carbon steel, Invar, and 26% Cr iron.

Welding Copper-Chromium Steels

Copper-chromium structural steels in the lower strength grades (80,000 lb./in.^{2}) may be welded by metal arc, oxyacetylene and resistance processes without fear of air hardening. In higher strength grades the weld must be heat treated to develop full ductility. Izod values in arc welded aluminum-treated steel $^1/_2$ inch thick containing 0.12 C, 0.50 Cu, 0.75 Cr were 70 ft.-lb. at room temperature and slightly higher at $-78°$ C.

Strength and ductility practically the same as base metal have been obtained in copper chromium steels (0.11 C, 0.50 Si, 1.02 Mn, 0.70 Cu, 0.35 Cr) without annealing, but with a carbon content of 0.18% ductility dropped rapidly.

Oxyacetylene butt welds made with a silico-manganese, low-alloy steel rod in steel 0.47 inch thick containing 0.17 C, 0.45 Si, 0.87 Mn, 0.49 Cu, 0.17 Cr had tensile strength equal to base metal but only about one half as high impact values.

Welding Copper-Nickel Steels

Shielded arc welds made in copper-nickel steel of the same composition as base metal, which contained 0.10 C max.; 0.50 Mn max.; 0.30 Si max.; 2.00 Ni max.; 1.00 Cu max., had the same strength as base metal. Machined specimens, $^3/_4$ x $1^7/_8$ inches, of butt welds in a steel $2^1/_4$ inches thick containing 0.17 C, 0.74 Mn, 2.00 Ni, 1.00 Cu, had about the same yield strength as, but lower tensile strength than, base metal.

The mechanical properties of U butt welds in steels containing 2 Ni, 1 Cu $^1/_2$ inch thick made with $^5/_{32}$ and $^3/_{16}$-inch flux coated electrodes of the same analysis as base metal were the same as base metal insofar as ductility and tensile strength were concerned. No cracks appeared in the 0.08 C welds but the 0.20 C welds in the heat-treated condition developed cracks $^3/_{16}$ inch long. The current should be low ($^1/_8$-inch electrodes, 80–100 amp.; $^1/_4$-inch electrodes, 200–250 amp.) and the arc length as short as possible. Layers should be not over $^1/_8$ inch thick and should be of the same width as the groove. The tacking and bottom layer should be deposited with a mild steel electrode, the mild steel being chipped out subsequently. Peening is unnecessary, but stress annealing ($1^1/_2$ hr. per inch at 650° C., furnace cool) is advisable. Welds in the higher carbon grade (0.20 C) should be preheated to 100 to 150° C. (200 to 300° F.) to secure ductility equal to the base metal. Oxyacetylene welds in a very low carbon steel containing 0.04 C, 0.36 Mn, 0.01 Si, 1.76 Ni, 0.91 Cu had good tensile properties.

Welding Copper-Molybdenum Steels

Butt welds in a steel containing 0.29 C, 0.74 Mn, 0.24 Si, 0.35 Cu, 0.22 Mo, 0.03 P made with an organic and mineral covered electrode (0.13 C in core rod, 0.5 Mo in deposit). Aside from 10% lower tensile impact value and ductility, the welds had the same properties as base metal. Stress relief at 600° C. had a good effect on tensile impact value.

Welding Cu-Ni-Mo Steels

Butt welds in steel containing 0.09 C, 0.85 Mn, 0.30 Si, 1.08 Ni, 0.10 Mo, 1.60 Cu had practically the same properties as base metal. An electrode (0.13 C, 0.50 Mo) with organic and mineral coating was used. Like other types, the Cu-Ni-Mo steels should be preheated if the carbon is relatively high (above 0.15). Tensile tests on oxyacetylene welds in a steel containing 0.07 C, 0.84 Mn, 1.45 Cu, 0.97 Ni, 0.10 Mo have resulted in fracture in the base metal.

Welding Copper Steels

INTRODUCTION

COPPER has a limited solubility in liquid and solid steel. If the carbon content exceeds about 0.05% liquid steel dissolves less than about 20% Cu and liquid copper dissolves less than about 20% iron. In solid steel with a low carbon content the solubility of copper is probably 8% or less at 1100° C., 3% or less at 800° C., and 0.35% or less at room temperature (see Alloys of Iron and Copper, by J. L. Gregg and B. N. Daniloff Alloys of Iron Monograph, McGraw-Hill Book Co. N. Y. (1934)). Since copper is not oxidized in the presence of metallic iron, the copper content of scrap is not lost on remelting. As a result all steels contain, on the average, about 0.1% Cu as an unintentional item in the composition. Copper does not combine with oxygen, nitrogen or carbon in steel. Added as an alloying element copper is usually present in copper steels to the extent of 0.2 to 1.5%. Other alloying elements are often added to form a complex copper steel. These complex steels containing copper are dealt with separately in this and other reviews of literature.

Aside from cast steels, which are not discussed herein, most of the copper steels are structural steels containing 0.1 to 0.3% C. In these as-rolled steels, up to 2% copper has a pronounced effect in raising the yield strength (see C. H. Lorig, *Metals Handbook*, 1939 Edition, pp. 554–560), without much effect on ductility. The resistance of structural steel to corrosion in many soils and atmosphere is increased by copper. The steels may be heat treated, although structural copper steels are usually used as rolled.

Copper affects the heat treatment of steel in three ways. In the first place, it mildly reduces the critical transformation rate of austenite and, consequently, favors deep hardening. Second, copper may be held in supersaturated solution at room temperature by rapidly cooling from 650° C. or higher. Air cooling is sufficiently rapid. Third, copper is precipitated from solid solution by reheating to 400 to 650° C., the precipitation being slow at 400° C., rapid at 650° C.

The temperature of heat treatment governs the mechanical properties. Quenched from the alpha region (650 to 700° C., approximately)copper steels are soft. Quenched from the austenitic state (850° C., approximately) copper steels are hardened. The hardening depends mainly on the carbon content, but austenite containing copper, like austenite containing carbon,

Table 1—Tensile Properties of Oxyacetylene Welds Made with Copper Steel Rods. Lorig and MacLaren[1]

Weld Made with:	Composition of Welding Rod* C	Si	Mn	Cu	Tensile Strength,† Lb./In.² As-Welded	Precipitation Hardened	Normalized and Precipitation Hardened	Elongation, †% in 8 Inches As-Welded	Precipitation Hardened	Normalized and Precipitation Hardened
Rod 1	0.06	nil	0.15	0.0	43,280	39,490	36,450	3.2	1.8	2.5
Rod 2	0.06	nil	0.15	1.2	52,880	53,000	53,880	6.6	8.9	10.5
Rod 3	0.20	nil	0.40	0.0	47,950	46,140	46,530	4.9	5.9	5.7
Rod 4	0.20	nil	0.40	1.2	59,150	56,230	53,400	12.2	13.5	12.4
Rod 5	0.20	0.35	0.45	0.0	55,580	47,700	51,880	9.5	5.8	9.2
Rod 6	0.20	0.35	0.45	1.2	60,430	57,470	56,180	19.6	21.5	21.6

* Phosphorus and sulfur not above 0.02% each in all rods.
† Average of two tests.

appears to possess mild hardening capacity. Upon being reheated, the steels undergo maximum precipitation hardening due to copper at 500° C.; the temperature depends a little on the quenching or normalizing temperature. The precipitation of copper is independent of precipitation of other constituents, such as oxides, nitrides and carbides.

The maximum increase in hardness due to precipitation hardening in copper steels is usually about 100 Brinell units above the air-quenched hardness. The yield strength and tensile strength may be increased 20,000 lb./in.² by precipitation hardening; the elongation may be reduced to ²/₃ its original value, and the notch-impact value may be decreased by half. During welding, a copper steel reaches the precipitation hardening and quenching temperatures in the heat-affected zone. Air cooling from welding may preserve the steel in the supersaturated condition at room temperature. If an intersecting weld is made, some portion of the quenched zone of the first weld will be heated to 500° C., the precipitation hardening temperature, by the second weld. Hoff[77] points out, without citing welding tests, that loss of ductility occasioned by precipitation hardening of the heat-affected zone is not to be feared in welding copper steels of any copper content. At 450 to 550° C., at which the hardening effect is pronounced the time to reach maximum hardness is an hour or so. At higher temperatures (600–700° C.) precipitation is more rapid but the hardening effect is slight. Stress-relieving temperatures are usually high enough to precipitate any copper in supersaturated solution, particularly if the part is slowly cooled after relief.

When copper steels are heated in an oxidizing atmosphere to 800° C. or above, a scale consisting of iron oxides is formed. Beneath or entrapped in the scale is metallic copper, which is not oxidized. If the temperature is above the melting point of copper, the molten copper penetrates along the grain boundaries of the steel and causes surface cracks. A small amount of nickel is often added to copper steel to prevent the cracking.

Aside from the copper structural steels there is almost no other important alloy of copper with iron. Beyond about 10% Cu the alloys contain a copper-rich phase which melts slightly below 1100° C. Welding of the higher copper alloys may therefore be difficult. Not strictly a subject in the welding of copper steels, the bronze-welding of steel in general is not discussed in the present review. Many low-alloy structural steels contain 0.20–0.30 Cu for improved corrosion resistance. In no instance has this small content of copper been shown to have any effect on welding.

Mechanical Properties of Welds

Oxyacetylene X butt welds made with copper steel filler rods in a steel having a tensile strength of 60,700 lb./in.², elongation 26.7% in 8 inches were tested by Lorig and MacLaren,[1] Table 1. The type of tensile specimen was not mentioned, nor were the heat treatments explained. The welds made with the copper-steel rods were 7 to 50% stronger than those made with plain carbon steel rods and their ductility was much better. An increase in carbon content increases the tensile strength while an increase in silicon raises the ductility. Heat treatment was of no advantage.

Nehl[2] butt welded two curved sheets 1.50 inches thick, 61 inches long containing 0.24 C, 0.64 Cu with a covered electrode containing 0.7 Cu, 0.2 Mo and some nickel. The weld was heated 1 hr. at 860–880° C. One half of the weld was cooled in air to 20° C.; the other half was cooled to 500° C., placed in a furnace at 640–660° C. for some time (stabilize anneal) and air cooled. Tensile (unmachined specimen, 1.18 inches wide, 2.36 inches gage length), bend, and notch impact (VGB specimen, 1.18 x 1.18 inches, 0.59 inch notch) tests were made. The specimens air cooled from 860 to 880° C. had the following properties:

Yield strength = 49,800 lb./in.²
Tensile strength = 77,500 lb./in.²
Elongation, % in 2.36 inches = 57
Reduction of area, % = 4.3
Notch-impact value, mkg./cm.² =
14.7 base metal
13.4–14.1 heat-affected zone
3.3–8.7 weld
Bend angle, degrees = 70 to 180

The tensile properties of stabilized specimens at temperatures up to 500° C. are shown in Table 2. The bend angle was 180° at room temperature, and the notch-impact value was 14.5 to 16.5 mkg./cm.² in base metal, 12.7 to 15.4 in heat-affected zone, and 12.5 to 17.5 in weld. It was immaterial whether the notch was placed in the root or the face of the V weld. The results showed that "stabilize annealing" at 640 to 660° C. was essential in order to secure a weld with good ductility and notch-impact value, but had no effect on the strength or notch-impact value of unwelded base metal.

Table 2—Tensile Tests of Arc-Welded Copper Steel. Nehl[2]

Temperature of Test, ° C.	Yield Strength, Lb./In.² Base Metal	Weld	Tensile Strength, Lb./In.² Unwelded Base Metal	Weld	Elongation Base Metal, % in 8 Inches	Weld, % in 2.36 Inches
20	48,900	49,200	76,200	77,000	27.0	19.2
200	44,100	42,400	68,000	71,200	24.1	19.9
300	38,200	35,600	67,500	69,500	21.1	19.6
400	34,500	31,400	70,500	71,500	15.6	14.7
500	28,000	28,400	61,000	71,800	17.4	15.5

According to Zeyen and Mehl,[3] Table 3, resistance butt welding of copper steels is satisfactory up to at least 2% Cu. The ductility and strength of arc and

Table 3—Mechanical Properties of Arc, Oxyacetylene and Resistance Butt Welds in Copper Steels. Zeyen and Mehl

Composition of Steel*				Yield Strength, Lb./In.²				Tensile Strength, Lb./In.²				Elongation, %				Bend Angles, Degrees		
C	Si	Mn	Cu	Base Metal	Arc	Gas	Butt	Base Metal	Arc	Gas	Butt	Base Metal	Arc	Gas	Butt	Arc	Gas	Butt
0.10	0.04	0.58	0.17	51,000	53,000	43,000	47,000	62,000	63,000	60,000	59,000	36	18	18	19	60–120	90–180	30–180
0.09	0.07	0.35	0.37	43,000	44,000	44,000	43,000	62,000	60,000	55,000	59,000	32	11	11	22	110–130	180	60–180
0.09	0.02	0.52	0.75	43,000	46,000	43,000	41,000	62,000	64,000	54,000	59,000	30	21	11	27	90–100	90–180	40–180
0.10	0.06	0.68	1.47	...	65,000	50,000	63,000	77,000	68,000	54,000	70,000	35	5	5	12	50–75	130–180	20–100
0.10	0.06	0.60	1.96	74,000	....	....	68,000	80,000	67,000	47,000	76,000	21	3	2.5	17	30–50	80–100	20–180

* The steels contained <0.01 P, <0.02 S.

oxyacetylene welds are satisfactory up to 0.75 Cu (0.10 C), beyond which both properties decrease rapidly compared with unwelded base metal. These results were confirmed by the results of a questionnaire which was circulated among German firms at the time of Zeyen and Mehl's experiments. Their welds were made perpendicular to the direction of rolling with mild steel filler rods, and were cooled in air. The bend angle was measured on unmachined specimens. If the reinforcement was ground flush, the bend angle of the arc welds was reduced 10 to 50%. Fracture of the higher copper specimens occurred next to the weld and the fractured surface was partly covered with copper or oxide.

Four commercial low-alloy steel sheets containing copper, Table 4, were tested by Epstein, Nead and Halley.[4] The oxyacetylene welds were made with strips of base metal. The metal arc welds were made with $^5/_{32}$-inch coated electrodes 120 amp. 30 volts. The core rod contained 0.17 C, 0.46 Mn, 0.012 Si, 0.007 P, 0.029 S; the coating contained 23.23% SiO_2, 0.51 Fe_2O_3, 0.34 Al_2O_3, 0.15 CaO, 17.28 TiO_2, 5.65 MgO, 8.10 Fe-Mn, 4.60 Fe-Mo, remainder organic binder. The arc weld metal contained up to 0.40 Mo. The $^5/_{32}$-inch electrode was not the correct size for the thinner sheets. It is presumed that the steels were welded in the as-rolled condition.

The tests on the plain copper steel D with bead left on or ground flush, Tables 4 and 5, reveal that the welds have nearly the same strength and ductility as unwelded base metal. The notch-impact value, Table 6, was the same as unwelded base metal at temperatures from +23 to −25° C. The notch-impact specimen was similar to the Charpy specimen but of the thickness of the sheet. The experimental values were multiplied by the ratio 0.197/0.062 for comparison with the 10 mm. Charpy specimen. The hardness of base metal was 59.5 B Rockwell; that of the heat-affected zone was B 58 in the oxyacetylene welds, and B 61 in the arc welds.

Butt welds were tested by Orr[5] using tensile specimens $^1/_2 \times 1^1/_2$ inches cut from a weld 18 inches long and roller bend specimens (plunger diameter $1^1/_2$ inches), Table 7. Base metal contained 0.28 C, 1.0 Mn, 0.5 Cu. Stress annealing was at 600° C. for $^1/_2$ hr. followed

Table 4—Tensile and Bend Tests of Unmachined Arc and Oxyacetylene Butt Welds in Low-Alloy Steel Sheets. Epstein, Nead and Halley[4]

Steel	Thickness, Inch	Yield Strength, Lb./In.² Metal Arc	Yield Strength, Lb./In.² Oxyacetylene	Tensile Strength, Lb./In.² Metal Arc	Tensile Strength, Lb./In.² Oxyacetylene	Elongation, % in 8 Inches Metal Arc	Elongation, % in 8 Inches Oxyacetylene	Reduction of Area, % Metal Arc	Reduction of Area, % Oxyacetylene	Appearance After 180° Flat Bend Metal Arc	Appearance After 180° Flat Bend Oxyacetylene
A	0.062	69,000	68,000	79,000*	79,500	10 to 18	16	..	..	Slight crack	OK
A	0.125	67,000	64,500	77,000	78,000	20	21	66	63	Weld cracked at junction	Cracked
B	0.062	59,000	68,500	81,000*	81,000	10 to 19	21	..	..	Slight crack	OK
B	0.125	67,500	65,000	79,000	79,000	21	20	66	64	OK	Cracked
C	0.062	61,000	60,500	74,000	74,000	21	17	..	..	Slight crack	Slight crack
D	0.062	41,000	32,000 to 40,500	50,000*	51,500	11 to 20	29	..	..	OK	OK
E	0.062	42,000	42,000	52,000	50,000	18	9 to 12	..	..	OK	OK

Steel	C	Mn	Si	P	S	Cu	Ni	Cr
A	0.08	0.48	0.130	0.120	0.024	1.05	0.47	...
B	0.08	0.48	0.130	0.118	0.025	1.06	0.50	0.35
C	0.07	0.42	0.224	0.010	0.023	0.80	0.45	0.39
D	0.08	0.38	...	0.008	0.026	0.25	...	...
E	0.08	0.38	...	0.007	0.026	...	...	...

* Fracture in weld (all other specimens fractured outside weld).

Mechanical Properties of Unwelded Steels

Steel	Condition	Thickness, Inch	Yield Strength, Lb./In.²	Tensile Strength, Lb./In.²	Elongation, % in 8 Inches	Reduction of Area, %	Charpy Notch-Impact Value, Ft.-Lb. +23° C.	Charpy Notch-Impact Value, Ft.-Lb. 0° C.	Charpy Notch-Impact Value, Ft.-Lb. −25° C.	Rockwell Hardness B Scale
A	As-rolled	0.062	73,000	80,000	23	...	27.9	29.3	28.4	89.3
A	Normalized†	0.062	61,500	75,000	21	..	29.3	30.5	32.3	84.1
A	As-rolled	0.125	69,100	77,900	22	68	41.7	44.2	42.2	87.9
A	Normalized†	0.125	62,500	74,500	25	68	46.0	47.0	46.3	82.2
B	As-rolled	0.062	72,000	81,500	22	..	28.1	29.7	29.7	87.9
B	Normalized†	0.062	62,500	77,250	21	..	31.2	30.9	31.5	85.9
B	As-rolled	0.125	69,900	80,300	23	65	41.7	45.8	47.0	84.3
B	Normalized†	0.125	62,750	77,750	23	71	44.1	45.7	40.7	84.0
C	As-rolled	0.062	65,000	75,000	24	..	33.8	33.9	36.0	84.1
C	Normalized†	0.062	52,000	68,000	25	..	33.9	36.8	36.8	78.4
D	As-rolled	0.062	42,400	51,250	31	..	29.8	31.3	29.5	59.5
D	Normalized†	0.062	36,750	50,000	26	..	28.9	31.6	31.0	55.8
E	As-rolled	0.062	41,000	52,000	14	..	27.1	29.3	29.0	64.7
E	Normalized†	0.062	34,750	49,000	26	..	31.1	30.2	30.6	54.7

† Heated to (1650° F.), air cooled. All specimens could be bent flat without cracking.

Table 5—Tensile and Hardness Tests of Machined Arc and Oxyacetylene Butt Welds in Low-Alloy Steel Sheets. Epstein, Nead and Halley[4]

Steel	Thickness Inch	Yield Strength, Lb./In.²		Tensile Strength, Lb./In.²		Elongation, % in 8 Inches		Reduction of Area, %		Rockwell B Hardness			
		Metal Arc	Oxyacetylene	Metal Arc	Oxyacetylene	Metal Arc	Oxyacetylene	Metal Arc	Oxyacetylene	Metal Arc In Weld	Metal Arc Next to Weld	Oxyacetylene In Weld	Oxyacetylene Next to Weld
A	0.062	....	66,500	59,000 to 73,500	66,500 to 75,000	0 to 1	3 to 7	.....	.....	94	89	95	85
A	0.125	66,000	65,500	77,000	73,000	2 to 12	9 to 17	27 to 41	20 to 48	91	86	90	83
B	0.062	....	69,000	63,000	76,000*	1	8	.....	.....	95	86	93	85
B	0.125	66,500	64,000	79,500	78,000†	13 to 19	8	21 to 61	11	93	87	94	86
C	0.062	....	65,000	45,500	73,000(*a*)	0	20	.....	.....	95	86	94	86
D	0.062	41,000	39,000	51,000(*a*)	45,500 to 50,500	26	8 to 19	.....	.....	87	61	89	58
E	0.062	39,500	39,000	35,000 to 42,000	44,500	0 to 3	2 to 6	.....	.....	87	64	66	53

* 1 specimen fractured without elongation at 47,500 lb./in.²
† 1 specimen fractured with 1% elongation at 66,000 lb./in.²
(*a*) Fracture outside weld (all other specimens fractured in the weld).

by furnace cooling. Although the covered electrodes were not described, the welding current was 10 to 20 amp. higher than for unalloyed steel. The welds fell short of base metal strength, particularly in the stress annealed condition.

According to Jennings,[6] the penstock gates of a large dam were made of open-hearth steel containing 0.23 C, 0.56 Mn, 0.24 Cu. Welds in this steel made with fluxed electrodes had a full-section tensile strength of 55,000 to 65,000 lb./in.², 30 to 50% free-bend value. Stäger[7] reported that the metal deposited by a copper steel electrode contained 0.1–0.12 C, 0.6 Mn, 0.04 Si, 0.5–0.7 Cu and had an elastic limit of 61,000 lb./in.², tensile strength = 77,000 lb./in.², elongation = 28.5% in 5 diam.

General Observations

Copper steels have not attracted much attention from the standpoint of welding difficulties. The Copper Development Association[8] states that with suitable flame adjustment oxyacetylene welding of copper steel can be done on a production basis with as high as 2% Cu. It has been stated that open-hearth iron[9] containing 0.02 C, 0.14 Mn, 0.005 Si, 0.035 S, 0.005 P, 0.42 Cu, 0.07 Mo has excellent "weldability" by both oxyacetylene and arc processes. According to Harris,[10] steels with up to 3% Cu have been successfully fusion welded without defects. With 4% Cu the weld metal cracks in the direction of welding as if it were intensely hot short. The recommendation has been made that low-alloy steels for welding should be based on copper and silicon rather than on air hardening elements. Without supplying details, a representative of I. G. Farbenindustrie[11] advised that special judgment of tip size is essential in welding copper steels. Kemper[12] stated that in oxyacetylene welding, copper in base metal increases the volume of slag (unalloyed filler rods) until with 0.75% Cu, welding is made difficult. It was this difficulty that probably accounts for the relatively poor physical properties of Zeyen and Mehl's oxyacetylene welds. Copper steels have a tendency to undercutting in arc welding, according to Miller and Lichtenwalter.[13]

Corrosion tests in two solutions: (1) 20% H_2SO_4 (sp. gr. 1.142), (2) saline solution, were made by Séférian and Leroy[14] on flame cut surfaces and arc and oxyacetylene butt welds in the steels shown in Table 8. The flame cut specimens were 2 × 4 inch thick, cut on one long edge with oxygen of 99.5% purity and dissolved acetylene, and were cooled slowly in air. The specimens were in inclined positions in 1000 ml. beakers containing 750 ml. solution. Corrosion was for 4 periods of 48 hr. each at 30° C. Corrosion appeared as a band at the

Table 7—Tensile and Bend Properties of Arc Welds in Copper Steel. Orr[5]

Treatment	Electrode	Tensile Strength, Lb./In.² Unmachined	Tensile Strength, Lb./In.² Ground Flush	Bend Test Angle, Degrees	Bend Test Elongation, % in 1/4 Inch
As-welded	I	83,000	68,000	80	36
Stress annealed	I	78,000	69,000	180	48
As-welded	II	85,000	78,500	55	28
Stress annealed	II	79,000	71,000	180	40
As-welded	III	84,500	86,000	180	36
Stress annealed	III	80,000	72,500	180	36
As-welded	IV	85,500	80,000	60	20
Stress annealed	IV	82,500	72,000	180	36

	Yield Strength, Lb./In.²	Tensile Strength, Lb./In.²
Unwelded base metal	55,000	88,000
Unwelded base metal, stress annealed	54,000	87,000

Table 6—Notch-Impact Value (Ft.-Lb.) of Arc and Oxyacetylene Butt Welds in Low-Alloy Steel Sheets. Epstein, Nead and Halley[4]

Steel	Thickness, Inch	Notch in Weld: Metal Arc +23° C.	Metal Arc 0° C.	Metal Arc −25° C.	Oxyacetylene +23° C.	Oxyacetylene 0° C.	Oxyacetylene −25° C.	Notch Next to Weld: Metal Arc +23° C.	Metal Arc 0° C.	Metal Arc −25° C.	Oxyacetylene +23° C.	Oxyacetylene 0° C.	Oxyacetylene −25° C.
A	0.062	13.7	26.9	11.9	26.5	28.0	22.1	30.4	26.9	28.7	25.4	31.0	22.1
A	0.125	26.2	22.7	6.1*	30.0	12.7	12.7	26.8	24.8	21.2	25.4	32.4	24.2
B	0.062	15.2	24.2	6.1	25.5	24.2	22.4	29.3	28.7	26.9	33.0	33.9	30.9
B	0.125	17.4	27.5	13.0	17.3	10.8	12.4	36.5	28.8	23.9	43.0	36.4	23.0
C	0.062	23.2	16.2	5.8	33.0	31.3	25.5	33.0	.	37.1	35.3	37.1	35.9
D	0.062	32.0	30.0	21.3	28.0	28.9	36.9	31.3	31.9	29.4	34.5	30.2	28.9
E	0.062	28.3	24.0	12.9	33.6	36.4	30.3	29.0	29.0	29.0	32.7	35.8	32.7

* Defect in weld.

Table 8—Steels Used by Séférian and Leroy[14]

C	Si	Mn	S	P	Cu	Cr
0.13	nil	0.49	0.040	0.016	0.40	..
0.20	0.27	0.70	0.031	0.026	0.57	0.52

depth back of the welded or cut edge corresponding to 400° C. No adequate explanation was offered except that the structure produced by reheating a rolled steel to 400° C. is most susceptible to corrosion, which is accentuated by electrolytic corrosion brought about by structural inhomogeneity.

Welding rods for steel often contain copper. Portevin[15] states that 0.5% is the amount of copper customarily added to copper steel rods in France. In 1919, according to Hobart,[16] an electrode was in use which contained 0.15–0.23 C, 0.60–0.75 Mn, 0.25 Cu. Oxyacetylene filler rods for low-alloy steel, in the opinion of Holler and Frankenbusch,[17] should contain copper to raise the corrosion resistance, but not so much as to injure the forgeability. Copper plated rods are commonly used for oxyacetylene welding. The copper coating prevents rust. Ronay[18] states that the copper coating is intended to lower the melting point and thus to reduce gassing. The I. G. Farbenindustrie[11] found that copper-coated rods have a high spatter loss, which, however, was said to be caused by cementite in the grain boundaries. Tris and Kapetensky[19] had good results with copper coated oxyacetylene rods, but copper coated electrodes were not satisfactory. The copper segregates in the weld and sets up electrolytic corrosion, according to Knox.[20] Wanamaker and Pennington,[21] on the other hand, found that the copper plated electrode was unsatisfactory because the arc was erratic.

Oxygen Cutting

Aside from Séférian and Leroy's corrosion tests and the statement by Wiss[22] that steels with 0.3% Cu give no trouble in oxygen cutting, there is no information available (see section on Cu-Ni-Mo steels).

Resistance Welding

Resistance butt welds in steels containing up to 2% Cu, 0.1% C were satisfactory in Zeyen and Mehl's[3] tests (see section on Mechanical Properties). Burgess and Aston[23] had no trouble in resistance welding iron containing 7% Cu, or 1 to 3% Cu with 0.5 to 1% Al, or 3% Cu with 0.5% Si.

Forge Welding

For some reason the forge welding of copper steels has attracted considerable attention. An article written by Bonsmann and Püngel[24] in a well-known handbook (1930) states that 0.8% Cu has no effect on the forge welding of steel and improves fusion welding properties. Schäfer[25] in his textbook also stated that copper in steel has no important effect on forge welding.

Experimenters are not in good agreement on the matter. In attempting to forge weld bars 0.71 to 0.79 inch diameter containing 0.1 C, 0.04 P, 0.04 S, trace Si and 0.58 to 1.92 Cu, Hahn[26] (1925) found that welding did not take place above 0.9% Cu. His explanation, which is not at all convincing, was that copper sulfide in the base metal has no solubility for the iron oxide formed during heating. In 1913 Clevenger and Ray[27] had increasing difficulty in making lap forge welds in steels containing 0.45 C, 0.4 Mn, 0.06 Si, 0.02 S, 0.02 P and up to 3% Cu. Steels with 4 or 5% Cu could not be forge welded. However, Burgess and Aston[23] (1909) had no difficulty in forge welding iron containing below 5% Cu. Iron with 7% Cu was difficult to forge weld. Among still earlier investigators, Stead and Evans[28] (1901) found that a steel containing 0.31 C, 0.61 Mn, 0.028 Si, 0.09 S, 0.069 P, 0.46 Cu had good forge weldability. Colby[29] had no difficulty (1899) in making forge welds having elastic limit = 35,000 to 43,500 lb./in.^{2}, tensile strength = 61,000 to 64,000 lb./in.^{2}, elongation = 10 to 25% in 8 inches in a steel 1/4 to 3/4 inch thick containing 0.138 C, 0.575 Cu, 0.39 Mn, 0.04 Si, 0.044 P, 0.038 S. In Lipin's[30] tests (1900) wrought iron with 0.65% Cu was easy to forge weld and steel with 0.1 C was easy to forge weld at 0.49% Cu but difficult at 0.61% Cu. With 1.69% Cu or more it was impossible to forge the steel, but pure iron containing 1.69% Cu could be forge welded. With 1.16% Cu the forge welded iron had a tensile strength of 51,000 lb./in.^{2}

Recovery and Pick-Up

According to Lohmann,[31] the recovery of copper from covered electrodes is practically 100%, which represents the general belief. Experimental results are given in Table 9.

Table 9—Recovery of Copper from Copper Steel Welding Rods

	Nehl's[3] Electrode		Keel's[22] Oxyacetylene Welding Rod	
	Electrode	Deposit	Filler Rod	Deposit
C	0.22	0.11	0.06	0.06
Mn	1.2	1.5	0.85	0.53
Si	0.25	0.14	0.12	0.02
Cu	0.68	0.65	0.35	0.20
Mo	0.00	0.18	0.11	0.09
Ni	0.37	0.38	..	..
N_2	..	0.021	..	..

The pick-up of copper by mild steel weld metal from a plate containing 0.07 C, 0.82 Mn, 1.38 Cu was studied by Haardt.[33] A single bead of bare electrode weld metal picked up 0.6 Cu; a single bead of covered electrode weld metal picked up 0.85% Cu. Two-layer deposits contained 0.15 Cu with bare electrodes and 0.50 Cu with covered electrodes.

WELDING COPPER TO STEEL

Copper and its alloys may be welded to steel, may be deposited as a surface layer on steel, or may be used as the bonding material to join steel parts. Copper rail bonds are sometimes welded to the rail. Swinnerton[34] gives instructions for oxyacetylene welding using a rod 3/16 inch diameter containing a little tin and some silver or phosphorus as deoxidizers. Spraragen[79] suggested the addition of 0.25% Si to the copper for deoxidation. The arc welding of copper rail bonds to the rail is described by Gailor[35] who weaves the electrode and employs carbon molds. If a carbon arc and carbon mold are employed, according to H. H. Febrey (private communication, March 1939), the arc is played on the copper bond terminal, never on the rail. A copper-silicon filler rod is added which fluxes scale or rust. The bonded rail passes the customary drop-impact test.

In 1926 a copper alloy electrode[36] was used for rail bonds, perhaps the same copper-silicon rod used by Tupholme,[37] with which molds were required but yielded deposits free from cracks and porosity. The bond terminal extended 1/4 inch or more through the bottom of the carbon molds. The pool of molten copper is con-

tinuously joined to the rail head as the mold is filled. Using a bare copper electrode, Austin[38] observed that the copper did not unite with the steel. The joint failed in shear at 14,000 lb./in.^{2}, the fracture being porous. Better fusion was obtained by directing the arc into the rail, but a brittle zone of steel-copper alloy was formed that contained numerous cracks. The weld metal again was porous. However, if a pure copper electrode coated with metallic deoxidizer were used, the copper quickly flowed into intimate contact with the steel, although the arc was not directed on the rail. There was practically no brittle alloy zone, porosity or cracks, and the joint sheared at 28,000 lb./in.^{2}, equivalent to the strength of cast copper. The deoxidizer dissolved the rust and scale on the rail, degasified the copper, and formed a protective slag. Processes[39] for oxyacetylene welding copper rail bonds to rails were in use in 1908.

According to H. H. Febrey (private communication, March 1939) the oxyacetylene process with phosphor-copper rod is most generally used. For metal arc welding a coated copper or copper-silicon electrode is preferred. Flash welding[52] is used to attach mild steel terminals to copper or combination bronze-and-steel rail bond cables. The mild steel terminal then is welded to the rail, which obviates the erratic arc (blow) observed in welding copper to steel and relieves the welder of the difficult task of joining all the cable strands. In any rail or signal bond it is essential to protect the copper cable from excessive welding heat, which causes grain growth and shortens the life of the bond. Vibration tests have established the superlative fatigue strength of the bonds possessing solid steel terminals.

Brief instructions for arc-welding copper and Everdur to steel are given by Jennings [40] The metal arc process with phosphor bronze electrodes is recommended for copper plates 1/4 inch or less in thickness. For thicker plates the long carbon arc process with phosphor bronze filler rod is best. Steele and Martin[41] (1921) had no difficulty in making strong Cyc-arc welds of brass rods and tubes to mild steel. The sleeve electrode developed by Lessel[42] is useful for welding copper to steel.

Either oxyacetylene or metal arc process may be used to weld copper to mild steel in chemical apparatus, according to Samesreuther.[43] In either case an electrolytic copper filler rod must be used, the Lessel electrode being satisfactory. Since iron reaches a high temperature faster than copper, the steel may be overheated before the copper attains welding temperature. Copper then will embrittle the steel by intergranular penetration. Peening is essential to avoid shrinkage stresses and cracking of the copper. Vollmaier and Feldmann[44] preferred the carbon arc with borax and additions of scrap copper for the welding of brass to steel instead of bronze electrodes. Otto[45] mentioned that copper has been flash welded to iron, and Miner[80] used electropercussive welding to join steel bars to copper bars 1/2 inch diameter. In spot-welding brass to cold-rolled steel, Hensel[46] found that diffusion may occur to such an extent that the steel becomes brittle and cracks in the weld. The zinc vaporizes and forms small blow-holes in the weld Handy[81] describes a process of welding copper to mild steel billets by rolling at 950° C. preparatory to making copper clad products.

The joining of steel parts by means of copper or copper alloy filler metal (bronze-welding) is too extensive a topic for the present review. An investigation of the copper brazing of mild steel was made by Webber.[47] Two surfaces of low-carbon steel (surface condition and composition not stated) were held for a long time at 1150° C. in hydrogen under a pressure of 100 lb./in.^{2} As a result grain growth occurred across the boundary and the surfaces were found to have welded together. Copper brazing utilizes copper to maintain a clean steel surface and depends on (1) alloying of copper with steel, (2) alloying of steel with copper, (3) penetration of copper along the grain boundaries, (4) growth of iron grains across the joint. Webber appears to have been mistaken in giving the black structure in the immediate vicinity of the copper brazed joint the name pearlite. M. C. Smith[48] has had success with a flux covered electrode of pure copper in welding steel, cast iron and galvanized iron. The copper weld metal picked up iron from the steel.

The coating of steel with bronze electrodes was described by Jennings,[49] who found that the steel must not be fused. Otherwise the deposit picks up iron and is porous. The atomic hydrogen process can be used to deposit copper on low-carbon steel, Invar, and 26% Cr iron, good entangling of steel by copper being obtained, according to Weinman.[50]

The intergranular penetration of bronze-welding metal (with or without zinc) into stress-free mild steel, according to Raymond,[51] may occur at about the critical range. Hook[75] and Goodrich[52] found that the penetration occurs only in stressed steel, and Connor[81] placed the temperature at 900 to 1000° C. Comstock[53] observed intercrystalline penetration of 10% tin bronze electrodes into mild steel without visible alloying. If 1% phosphorus was added to the bronze, a eutectic layer 0.002 inch thick of unknown constitution was formed. Further details of bronze-welding, copper brazing, and surfacing may be found in a review of literature. There is no difficulty in soft soldering copper to iron.[84]

WELDING COPPER-CHROMIUM STEELS

Copper-chromium structural steels in the lower strength grades (80,000 lb./in.^{2}) may be welded by metal arc, oxyacetylene and resistance processes without fear of air hardening, according to Kinzel and Crafts.[55] Stress relief is unnecessary unless the dimensions are unusual. In higher strength grades the weld must be heat treated to develop full ductility. The Izod notch-impact value at different temperatures of V butt welds made transverse to the direction of rolling in aluminum treated steel 1/2 inch thick containing 0.12 C, 0.5 Cu, 0.75 Cr is shown in Table 10. The welds were stress annealed at 650° C. and the notch was at the scarf. The Izod impact value is high even at −78° C.

Table 10—Izod Value (Ft.-Lb.) of Arc Welds in Copper-Chromium Steel. Kinzel, Crafts and Egan[55]

	Temperature, Degrees Centigrade				
Electrode	+20° C.	−50° C.	−78° C.	−100° C.	−120° C.
Mineral coated	71.0	71.2	71.5	32.8	15.2
Combustible coated	73.0	66.0	79.0	78.0	48.5

The effect of carbon content on the ductility of 60° V arc welds in copper-chromium steel 0.79 inch thick was shown by Lohmann and Schulz,[21] Table 11. The covered electrode (see Fig. 1 in review of literature on Effect of Nitrogen on the Welding of Steel (American Welding Society Journal, 17 (4) Suppl., 9-30, April 1938) for notch-impact results, electrode V) contained 0.27 C, 0.13 Si, 0.63 Mn, 0.10 Cu, and the weld contained 0.10 C, 0.13 Si, 0.69 Mn, 0.13 Cu, 0.006 N_2 in both steels. The plates were spaced 0.08 inch apart and the root was milled out and reverse welded. The tensile bars were 0.59 inch diameter. Some were annealed

Table 11—Tensile Properties of Arc-Welded Copper-Chromium Steel at 20° C. Lohmann and Schulz[31]

Composition of Steel					Yield Strength, Lb./In.²			Tensile Strength, Lb./In.²			Elongation, % in 0.39 Inch			Reduction of Area, %		
C	Si	Mn	Cu	Cr	Unwelded	Welded	Annealed	Unwelded	Welded	Annealed	Unwelded	Welded	Annealed	Unwelded	Welded	Annealed
0.18	0.40	1.03	0.72	0.40	54,000	55,000	47,500	84,000	70,000	65,500	24.9	5	5	59	41	38 to 51
0.11	0.58	1.02	0.70	0.35	55,000	56,000	51,000	76,500	77,000	71,500	29.0	21	14	64	64	50

Tensile Strength (Lb./In.²) of Arc-Welded Copper-Chromium Steel at Elevated Temperatures

Steel	Temperature, Degrees Centigrade								
	100° C.		200° C.		300° C.		400° C.	500° C.	600° C.
	Y.S.	T.S.	Y.S.	T.S.	Y.S.	T.S.	T.S.	T.S.	T.S.
0.18 C (St. 52a)	54,000	68,000	52,500	83,000	51,000	90,000	84,000	63,000	43,000
0.11 C (St. 52b)	52,500	77,000	50,000	84,000	47,000	87,000	83,000	60,000	37,000

Y.S. = Yield strength; T.S. = Tensile strength.

above A_3. The results show that welding has little effect on the strength and ductility of the lower carbon steel but has an adverse effect on the higher carbon steel. Annealing was not beneficial to tensile strength and ductility, but it increased the notch-impact value slightly at −80 to +200° C. The rotating bend fatigue strength of the welded higher carbon steel was 25,600 lb./in.² Although the German Railways limit the copper content of low-alloy steels for welding to 0.55%, Hoff[77] stated that undesirable hardening is not created by precipitation of copper in the heat-affected zone of welded copper-chromium steels (0.40 Cr, 0.20 C max.) and believed that from the welding standpoint the restriction of the German Railways had no basis.

Oxyacetylene butt welds made by Zeyen[57] with a silico-manganese, low-alloy steel rod in steel 0.47 inch thick containing 0.17 C, 0.45 Si, 0.87 Mn, 0.49 Cu, 0.17 Cr (tensile strength = 74,000 lb./in.², bend angle = >180°, DVMR notch-impact value = 12.5 mkg./cm.²) had the following properties averaged from 4 to 6 specimens: tensile strength = 92,500 lb./in.² unmachined; 78,000 lb./in.² machined flush; bend angle = 180° unmachined; 84° machined flush; DVMR notch-impact value = 6.7 mkg./cm.²

Tensile and roller bend (plunger diameter = $1^1/_2$ inches) tests were made by Orr[5], Table 12, on specimens $^1/_2$ x $1^1/_2$ inches cross section cut from butt welds in a steel containing 0.28 C, 0.8 Mn, 0.6 Cr, 0.5 Cu. Stress annealing was at 600° C for $^1/_2$ hr. followed by furnace cooling. Although the covered electrodes were not described, the current was 10 to 20 amp. higher than for unalloyed steel. Some of the unmachined welds approached very closely the strength of unwelded base metal.

Three types of covered electrodes, Table 13, were used by two Russian investigators[53] to weld Cu-Cr steel plates 0.79 inch thick. The coverings contained 3 to $4^1/_2$% copper oxide, and 7% chromium oxide, together with different proportions of water glass, ilmenite, feldspar, ferromanganese, chalk, wood pulp and starch.

Weld metal from a Smit covered electrode for low-alloy steel contains 0.35 Mn, 0.50 Cr, 0.30 Cu (carbon not stated), according to Gerritsen and Schoenmaker.[58] The yield strength is 54,000 lb./in.², tensile strength = 74,000 lb./in.², elongation = 26%, rotating beam fatigue strength = 30,500 lb./in.² (machined V weld in low-alloy steel), hardness = 160 Brinell. Sommer[59] found that bars 0.39 x 1.18 inches machined from V butt arc welds in a steel 0.47 inch thick containing 0.20 C, 0.31 Si, 0.94 Mn, 0.40 Cr, 0.82 Cu had a tensile strength of 83,500 lb./in.² (fracture in base metal). Fillet welds in tension fractured at 60,000 to 66,000 lb./in.², based on throat area where fracture occurred. The percentage loss of elements from a rod containing 0.4 Cr, 0.6 Cu in oxyacetylene welding, according to Mercier,[60] was C = 28%, Mn = 46%, Si = 69%, Cr = 5%. There was no loss of copper.

Table 13—Covered Electrodes for Cu-Cr Steel[53]

	C	Si	Mn	Cu	Cr
Base metal	0.20	0.25	1.06	0.50	0.54
Core rod	0.07	0.016	0.30	..	..
Weld metal 1	0.05	0.04	0.14	0.87	0.40
Weld metal 2	0.05	0.06	0.23	1.08	0.58
Weld metal 3	0.08	trace	0.54	0.49	0.46

Mechanical Properties of 70° V Butt Welds

	Yield Strength, Lb./In.²	Tensile Strength, Lb./In.²	Charpy Notch-Impact Value, Mkg./Cm.²	Bend Angle, Degrees
Weld metal 1	57,000	69,000	7.0	43
Weld metal 2	54,000	77,000	10.0	145
Weld metal 3	52,000	68,000	10.0	93

WELDING COPPER-NICKEL STEELS

An indication of what may be expected of welds in the only type of copper-nickel steel upon which there is any information is furnished by Youngstown Sheet & Tube Company's[61] Table 14. The welds were made with coated electrodes, either unalloyed or of the same com-

Table 12—Tensile and Bend Properties of Arc Welds in Copper-Chromium Steel. Orr[5]

Treatment	Electrode	Tensile Strength, Lb./In.² Unmachined	Tensile Strength, Lb./In.² Machined	Bend Test Angle, Degrees	Bend Test Elongation, % in $^1/_4$ Inch
As-welded	I	80,500	74,000	180	44
Stress annealed	I	74,000	67,500	180	40
As-welded	II	85,000	60,500	40	24
Stress annealed	II	77,500	69,500	180	40
As-welded	III	82,000	79,500	180	36
Stress annealed	III	79,000	78,500	180	36
As-welded	IV	85,000	78,000	80	26
Stress annealed	IV	77,500	77,000	180	40

	Yield Strength, Lb./In.²	Tensile Strength, Lb./In.²
Unwelded base metal	55,800	86,000
Unwelded base metal, stress annealed	51,300	78,500

Table 14—Mechanical Properties of Arc-Welded Cu-Ni Steel. Youngstown Sheet & Tube Company[61]

Specimen	Yield Strength, Lb./In.²	Tensile Strength, Lb./In.²	Elongation, % in 2 Inches	Reduction of Area, %	Charpy Impact Value, Ft.-Lb.
As-rolled	50,000 to 55,000	67,000 to 76,000	29 to 23*	..	43
As-welded	58,000	76,000	21	55	30
Stress annealed 1 to 1½ hr. at 620–650° C. (1150–1200° F.)	55,000	72,000	..	..	25 to 55

* % elongation in 8 inches.

position as base metal, which contained 0.10 C, max.; 0.50 Mn, max.; 0.30 Si, max.; 2.00 Ni, max.; 1.00 Cu max. Fractures occurred in base metal. The welds had the same strength as base metal; stress annealing slightly lowered the strength.

There is considerable additional information on the arc welding of structural steel containing 2% Ni, 1% Cu, most of which has been collected by Gibson.[61,62] Machined specimens ¾ x 1⅞ inches of butt welds in a steel 2¼ inches thick containing 0.17 C, 0.74 Mn, 2.00 Ni, 1.00 Cu, Table 15, had about the same yield strength as, but lower tensile strength than, base metal. Fractures occurred in the weld, which was made with an electrode containing 0.7% Mo in the coating.

Table 15—Tensile Properties of Arc-Welded Copper-Nickel Steel. Gibson[62]

Specimen	Yield Strength, Lb./In.²	Tensile Strength, Lb./In.²
Unwelded base metal	69,180	90,700
As-welded	67,500	80,500
Normalized 870° C.(1600° F.)	61,400–65,600	73,500–75,100

In other tests, of which no details are given, Gibson showed that welds in steel containing 0.08 C, 1.99 Ni, 1.02 Cu had about the same yield strength as base metal, namely, about 65,000 lb./in.² Heating or 510° C. (950° F.) for 1 hr. raised the yield strength of the weld to 80,000 lb./in.², but heating to 900° C. (1650° F.) lowered the yield strength to 55,000 lb./in.² Fracture occurred in base metal. On the other hand, welds in steel containing 0.22 C, 1.98 Ni, 0.92 Cu failed in the weld after heat treatment and the yield strength was reduced from 85,000 lb./in.² as-welded to 80,000 and 65,000 lb./in.² by heating 1 hr. at 510 and 900° C., respectively.

Charpy impact tests (V notch, no details) of weld, fusion zone and base metal (0.17 C, 2 Ni, 1 Cu with low carbon covered electrodes containing 0.7 Mo or 2 Ni, 1 Cu) showed that the impact value of the weld was 50 to 60 ft.-lb. whether or not heat treated, but the impact value of fusion zone and base metal was increased up to 146 ft.-lb. after air cooling from 900 or 680° C. (1650 or 1250° F.).

The composition of the weld metal in V butt welds in copper-nickel steels ⅝ inch thick is shown in Table 16. Heavy covered electrodes were used. There were no peculiarities in the microstructure, the heat-affected zone being fine grained in all conditions. Gibson observed 109 B Rockwell close to a flame cut in a steel containing 0.17 C, 1.95 Ni, 1.00 Cu (90 B Rockwell). It was concluded that the steel had slight tendency to air hardening. Torch drawing (no details) reduced the hardness to 90 B.

The mechanical properties of U butt welds in steels containing 2 Ni, 1 Cu ½ inch thick made with 5/32 and 3/16 inch flux coated electrodes of the same analysis as base metal were determined by Esslinger,[63], Table 17. Stress annealing was at 650° C.; full annealing at 960° C. for both steels. Unlike Gibson's results, annealing did not have much effect on the strength. The high ductility combined with high strength is noteworthy. All full-section tensile fractures occurred in base metal. No cracks appeared in the 0.08 C welds but the 0.20 C welds in the heat-treated condition developed cracks 3/16 inch long. Esslinger found that the current should be low (⅛-inch electrodes, 80–100 amp.; ¼-inch electrodes, 200–250 amp.) and the arc length as short as possible. Layers should be not over ⅛ inch thick and of the same width as the groove. The tacking and bottom layer should be done with a mild steel electrode, the mild steel being chipped out subsequently. Peening is unnecessary, but stress annealing (1½ hr. per inch at 650° C., furnace cool) is advisable. The higher carbon grade (0.20 C) should be preheated to 100 to 150° C. (200 to 300° F.).

Jennings[40] is in agreement with Esslinger and Gibson, and points out that if electrodes of the same composition as base metal are used, the welds will respond to a precipitation hardening treatment, Table 18. The precipitation hardening effect is pronounced in the lower carbon steel.

In his studies of maximum hardness of heat-affected zone as a function of welding speed, Warner[64] deposited a bead of weld metal 3 inches long manually on plates 3 x 9 x ½ inch. From the results in Table 19 Warner concluded that the addition of copper to a 2% nickel steel has no definite effect on the hardness. It was believed that 1% Cu replaces 1½% Ni from the standpoint of hardness.

The static and impact tensile properties are listed in Table 20. Stress annealing was at 600° C. and fracture occurred in base metal. The welds were 60° V butt in plates ½ inch thick and were ground flush. The parallel portion of the specimens was 1 inch. Contrary to Jennings' results, the ductility of the welds was much lower than that of base metal. Again, contrary to Esslinger's results, in the higher carbon steel stress annealing had no

Table 16—Composition of Weld Metal in Cu-Ni Steel. Gibson[62]

	Base Metal	Center of Weld	Weld Near Plate	Base Metal	Center of Weld	Weld Near Plate	Base Metal	Electrode	Weld	Base Metal	Electrode	Weld
C	0.24	..	..	0.08	..	..	0.20	low	0.08	0.06	0.06	0.095
Mn	..	..	..	..	..	..	0.80	..	0.50	0.40	0.40	0.53
Ni	1.98	0.67	1.01	1.90	0.71	0.89	1.80	..	0.50	2.01	2.01	1.86
Cu	1.01	0.43	0.52	1.05	0.42	0.46	0.90	..	0.33	1.01	1.01	1.00
Mo	..	0.21	0.20	..	0.29	0.24	..	0.70	..	(0.003 Si)	(0.003 Si)	(0.23 Si)

Table 17—Mechanical Properties of Arc-Welded Cu-Ni Steels. Esslinger[61]

Specimen		Full Section Tensile and Bend					All-Weld-Metal Tensile			
Carbon Content	Condition	Yield Strength, Lb./In.²	Tensile Strength, Lb./In.²	Fracture	Bend Angle, Degrees	Elongation, %	Yield Strength, Lb./In.²	Tensile Strength, Lb./In.²	Elongation, % in 2 In.	Reduction of Area, %
0.08%	As-welded	58,000	70,000	2.0 inches from weld	180	40	58,500	80,000	24	37
0.08%	Stress annealed	62,000	73,000	1.5 inches from weld	180	42	70,000	81,000	27	54
0.08%	Full annealed	63,000	74,000	2.0 inches from weld	180	42	64,000	77,000	26	53
0.20%	As-welded	66,000	91,000	2.0 inches from weld	45	14	69,000	95,000	13	12
0.20%	Stress-annealed	69,000	92,000	2.0 inches from weld	180	31	76,000	91,000	18	20
0.20%	Full annealed	68,000	90,000	1.0 inch from weld	180	32	69,000	87,000	17	20

effect on the ductility, although it increased the tensile-impact value. Warner's results show that the tensile strength of the copper-nickel steels is not decreased by welding, but the ductility is decreased. Stress annealing is not advisable for the welded lower carbon steel but is beneficial to the welds in the higher carbon steel. The higher carbon steel was classified as moderately air hardening by the Committee on Welding of Low-Alloy Steels.[65]

According to the Union Carbide and Carbon Research Laboratories,[65] oxyacetylene welds in a very low-carbon steel containing 0.04 C, 0.36 Mn, 0.01 Si, 1.76 Ni, 0.91 Cu had the tensile properties shown in Table 21. An uncoated filler rod was used and fracture occurred in base metal. Stress annealing had a conspicuous effect on the strength without loss of ductility.

Table 18—Tensile Properties of Arc-Welded Cu-Ni Steels. Jennings[40]

Specimen	Base Metal = 0.08 C, 1.99 Ni, 1.02 Cu: Yield Strength, Lb./In.²	Tensile Strength, Lb./In.²	Elongation, % in 2 In.	Base Metal = 0.22 C, 1.98 Ni, 0.92 Cu: Yield Strength, Lb./In.²	Tensile Strength, Lb./In.²	Elongation, % in 2 Inches
Unwelded base metal	53,000	65,000	28	.....	.....	..
As-welded	55,800	65,000	33	75,000	82,000	14
Annealed 1 hr. 510° C. (950° F.)	76,000	80,000	31	80,000	95,000	12
Annealed 1 hr. 650° C. (1200° F.)	60,000	71,000	38	69,000	88,000	16
Annealed 1 hr. 900° C. (1650° F.)	52,000	66,000	40	65,000	80,000	18

WELDING COPPER-MOLYBDENUM STEELS

According to Gibson,[62] arc welds in steel (rectangular specimens, no details) containing 0.25 C, 1.40 Cu, 0.25 Mo have the properties shown in Table 22. Fractures occurred in the weld. Heating at 510° C. raised strength and ductility. However, the precipitation hardening treatment may decrease the impact value from 25 ft.-lb. as-rolled to 16 ft.-lb. on account of cooling strains. If the weld is first normalized, subsequent heating to 510° C. may increase the Charpy value to 34 ft.-lb. (no details).

Using the type of specimen described in the preceding section, Warner[64] found that butt welds in a steel containing 0.29 C, 0.74 Mn, 0.24 Si, 0.35 Cu, 0.22 Mo, 0.03 P made with an organic and mineral covered electrode (0.13 C in core rod, 0.5 Mo in deposit) had the properties shown in Table 23. Aside from 10% lower tensile impact value and ductility, the welds had the same properties as base metal. Stress relief at 600° C. had a good effect on tensile-impact value. The maximum hardness in the heat-affected zone as a result of depositing a bead of weld metal 3 inches long manually on a plate 3 x 9 x 1/2 inch was 258 Vickers Brinell at a speed of 4 to 6 inches per minute and 413 at 10 to 12 inches per minute.

According to Doussin,[66] the rotating bend endurance limit (10 million cycles at 3000 cycles per min.) of unmachined oxyacetylene butt-welded tubes 0.95 inch o.d., 0.08-inch wall, was 16,400 lb./in.² The tube contained 0.06 C, 0.34 Mn, 0.006 Si, 0.032 S, 0.045 P, 0.50 Cu, 0.25 Mo (tensile strength = 59,000 lb./in.²), and the filler rod contained 0.13 C, 0.45 Mn, trace Si.

Table 19—Maximum Vickers Brinell Hardness of Cu-Ni Steels as a Function of Carbon Content and Welding Speed. Warner[64]

Composition of Steel					Arc Speed	
C	Mn	Si	Ni	Cu	4 to 6 Inches per Minute	10 to 12 Inches per Minute
0.06	0.35	0.005	1.73	0.93	190	190
0.16	0.60	0.20	2.09	1.06	310	370

Oxyacetylene butt welds made by Zeyen[57] with a silico-manganese, low-alloy steel rod in steel 0.47 inch thick containing 0.15 C, 0.52 Si, 0.98 Mn, 0.25 Cu, 0.11 Mo (tensile strength = 74,000 lb./in.², bend angle = 180°; DVMR notch-impact value = 12.9 mkg./cm.²) had the

Table 20—Static and Impact-Tensile Properties of Arc-Welded Cu-Ni Steels 1/2 Inch Thick. (Coated C-Mo Electrode). Warner[64]

	Base Metal = 0.06 C, 1.73 Ni, 0.93 Cu: Base Metal As-Rolled	Base Metal Stress Relieved	Weld As-Welded	Weld Stress Relieved	Base Metal = 0.16 C, 2.09 Ni, 1.06 Cu: Base Metal As-Rolled	Base Metal Stress Relieved	Weld As-Welded	Weld Stress Relieved
Yield strength, lb./in.²	48,400	45,200	47,000	40,000	54,000	56,000	48,000	52,000
Tensile strength, lb./in.²	70,700	67,700	74,000	74,000	87,300	80,500	84,000	85,000
Elongation, % in 1 inch	56	61	16	17	50	51.5	22	22
Tensile-impact value, ft.-lb.	769	730	788	651	812	797	435	727

Table 21—Tensile Properties of Oxyacetylene Welded Iron Containing Copper and Nickel. Union Carbide & Carbon Research Laboratories, Inc.[65]

	Yield Strength, Lb./In.²	Tensile Strength, Lb./In.²	Elongation, % in 2 Inches
As-rolled	48,000	61,000	30
As-welded	45,000	62,000	30
Stress relieved 650° C., 1 hr.	63,000	76,000	31

following properties averaged over 4 to 6 specimens: tensile strength unmachined = 97,000 lb./in.²; machined flush = 81,500 lb./in.²; bend angle unmachined = 180°; machined flush = 89°; DVMR notch-impact value = 6.3 mkg./cm.²

Industry and Welding[76] report that welds in a low carbon steel containing 0.045 C, 0.45 Mn, 0.01 Si, 0.30 Cu, 0.05 Mo made with an electrode containing 0.03 C, 0.36 Mn, 0.016 Si, had a yield strength = 35,000–50,000 lb./in.², tensile strength = 50,000–75,000 lb./in.², elongation = 10–20%, bend angle = 80 to 180° (no details). There was 10 to 40% greater loss in weight in the weld than in base metal in 20% HCl or 20% H_2SO_4. On the other hand, Petiniaud[78] found practically no difference in corrosion rate between unwelded iron containing 0.03 C, 0.45 Cu, 0.07 Mo and welds made with bare and covered electrodes. The welds were exposed in an oil tank on ship board, 5 ft. above the surface of the oil. Kessner and Specht[67] found that spatter and other welding factors were the same for a bare electrode depositing metal containing 0.05 C, 0.16 Si, 0.70 Mn, 0.40 Cu, 0.10 Mo, 0.075 N_2 as for unalloyed steel. Czternasty[68] mentions the use of oxyacetylene

Table 22—Tensile Properties of Arc Welds in Cu-Mo Steel. Gibson[62]

	Yield Strength, Lb./In.²	Tensile Strength, Lb./In.²	Elongation, % in 2 Inches
As-welded	80,000	93,700	28
Heated 1 hr. at 510° C. (950° F.)	88,400	98,700	31

welded superheater tubes made of Cu-Mo steel (no details).

WELDING CU-NI-MO STEELS

Using an electrode (0.13 C, 0.5 Mo) with organic and mineral coating, Warner[64] made 60° V butt welds in the steels shown in Table 24. The specimens were 1/2 inch thick, 1 inch parallel portion, and the welds, which contained 0.5 Mo, were ground flush. The welded lower carbon steel had practically the same properties as base metal. The maximum hardness in the heat-affected zone as a result of depositing a bead of weld metal 3 inches long manually on a plate 3 x 9 x 1/2 inch was extraordinarily high in the higher carbon steel. At speeds of 4–6 and 10–12 inches per minute the maximum hardness was 340 and 450 Vickers Brinell for the higher carbon steel and 220 and 260 for the lower carbon, respectively. According to Whitmer,[79] the lower carbon grade of RDS (1.5 Cu, 1 Ni, 0.1 Mo) showed a spread of less than 8 points Rockwell in the vicinity of 90 B Rockwell between arc weld, heat-affected zone, and base metal (no details). Miller[70] also made a Rockwell exploration of arc welds in 12 and 14 gage sheets containing 0.095 C, 0.70 Mn, 0.03 Si, 1.44 Cu, 0.75 Ni, 0.07 Mo. Base metal was 78 to 83 B Rockwell; the maximum in an X butt joint was 92 B; the maximum in a lap joint was 94 B. Drop impact tests of welded tanks made of this steel were successful.

Arc welds in Cu-Ni-Mo steels made with a covered carbon-molybdenum electrode (rectangular specimens, no details), according to Gibson,[62] Table 25, are improved by heating to 510° C. for precipitation hardening. Fractures occurred in the weld. Like other types, the

Table 23—Static and Impact Tensile Properties of Arc-Welded Cu-Mo Steel 1/2 Inch Thick (Coated C-Mo Electrode). Warner[64]

	Base Metal		Weld	
	As-Rolled	Stress Relieved	As-Welded	Stress Relieved
Yield strength, lb./in.²	45,000	50,000	51,200	46,800
Tensile strength, lb./in.²	90,000	88,000	93,750	86,500
Elongation, % in 1 inch	40	42	37	39
Tensile-impact value, ft.-lb.	810	820	697	754

Cu-Ni-Mo steels should be preheated if the carbon is relatively high. A French writer[71] believed that the excellent "weldability" of a steel containing 0.25 C, 1.44 Cu, 0.75 Ni, 0.70 Mn, 0.07 Mo could be attributed largely to the molybdenum content.

Like most copper-bearing steels, according to Miller and Lichtenwalter,[13] steel containing 0.5–1.5 Cu, 0.5–1.0 Ni, 0.10 Mo min. (Grade 1 = 0.12 C max., Grade 1 A = 0.20–0.30 C) is subject to undercutting during arc welding, which can be avoided by means of a special flux-coated electrode (no details). Puddling coarsens the grain structure and should be avoided. The higher-carbon grade, if 1/4 inch or thicker, must be stress relieved at 650 to 680° C. (1200 to 1250° F.) after arc welding. Unmachined, parallel-section tension specimens in 3/4-inch plate fail in the butt weld (covered electrode) if the joint is not stress relieved; otherwise fracture occurs in base metal. There is no difficulty in oxygen cutting, but the cut edge should be machined to a depth of 1/32 to 1/16 inch before welding. On account of its slightly higher heat conductivity (no details), Cu-Ni-Mo steel must be held a little longer at heat for spot welding than plain carbon steel.

Tensile tests on oxyacetylene welds in a steel containing 0.07 C, 0.84 Mn, 1.45 Cu, 0.97 Ni, 0.10 Mo have been made by the Union Carbon and Carbide Research Laboratories, Inc.,[65] Table 26. An uncoated filler rod was used and fractures occurred in base metal.

Short time creep tests (50 hr. VDE test) were made by Schottky and Ruttmann[72] on X welds in a steel contain-

Table 24—Static and Impact Tensile Properties of Arc-Welded Cu-Ni-Mo Steels. Warner[64]

Condition	C	Mn	Si	Ni	Mo	Cu	Elastic Limit, Lb./In.²	Tensile Strength, Lb./In.²	Elongation, % in 1 Inch	Tensile-Impact Value, Ft.-Lb.
As-rolled	0.23	0.72	0.02	0.79	0.15	1.56	58,200	105,000	40	773
As-welded	0.23	0.72	0.02	0.79	0.15	1.56	53,000	99,250	22	582
As-rolled	0.09	0.85	0.03	1.08	0.10	1.60	40,800	86,650	46.5	740
As-welded	0.09	0.85	0.03	1.08	0.10	1.60	44,400	85,500	32.5	688

Table 25—Tensile Properties of Arc-Welded Cu-Ni-Mo Steels. Gibson[62] and Jennings[40]

Specimen	Base Metal = 0.10 C, 1.44 Cu, 0.75 Ni, 0.10 Mo			Base Metal = 0.23 C, 1.30 Cu, 0.75 Ni, 0.16 Mo		
	Yield Strength, Lb./In.²	Tensile Strength, Lb./In.²	Elongation, % in 2 Inches	Yield Strength, Lb./In.²	Tensile Strength, Lb./In.²	Elongation, % in 2 Inches
Unwelded base metal	67,800	82,000	33	....	....	..
As-welded	70,000	85,000	29	80,590	96,460	29
Heated 1 hr. 510° C. (950° F.)	....	....	..	93,870	104,620	29

ing 0.16 C, 0.21 Si, 0.77 Mn, 0.33 Cu, 0.31 Ni, 0.34 Mo, 0.19 Cr, 2 inches thick. A covered electrode containing Mn and Mo was used and the welds were stress annealed 2 hr. at 600° C. The axis of loading was perpendicular to the weld, the gage length of 4 inches including weld and heat-affected zone. At 400 to 550° C. the welds had 10 to 20% higher short-time creep strength than unwelded base metal. The absolute values of short time creep strength depend so closely on the methods of evaluation that they are not reproduced herewith.

WELDING CU-CR-MO STEELS

The only information on surface welded Cu-Cr-Mo steels is given by Meunier and Rosenthal,[73] Table 27. The plates were 0.39 inch thick and 1 or 4 beads of arc weld metal were deposited at 180 amp. and a speed of 7.1 inches per minute. Bending was done over a mandrel

Table 26—Tensile Tests of Oxyacetylene Welded Cu-Ni-Mo Steel. Union Carbide and Carbon Research Laboratories, Inc.[65]

	Yield Strength, Lb./In.²	Tensile Strength, Lb./In.²	Elongation, % in 2 Inches
As-rolled	62,000	83,000	25
As-welded	60,000	97,000	13
Stress annealed 1 hr. at 650° C. (1200° F.)	61,000	83,000	19

1.18 inches thick, the bead being in tension. The notch-impact specimen contained a notch 0.08 inch deep, 0.08 inch diameter cut in the deposit. The base of the notch was in the heat-affected zone. The results show that a single bead greatly decreases the bend elongation. The heat-treating effect of additional beads restores ductility and decreases the hardness, which was measured on a Rockwell machine. According to Portevin,[15] a steel containing 0.10–0.15 C, 0.40–0.50 Cu, 0.30–0.40 Cr, 0.10–0.15 Mo, 0.60–0.80 Mn, 0.20–0.20 Si is used in France for high grade welds (no details).

WELDING CU-NI-CR STEELS

The average Charpy impact value at low temperatures of arc welds in steel containing 0.10 C, 0.68 Mn, 0.21 Si, 0.77 Cr, 0.58 Cu, 0.63 Ni, according to Hopkins and Blumberg,[74] Table 28 (no details) depends on the location of the notch. The heat-affected zone maintains high impact value down to −100° C. All specimens were stress annealed at 620° C. (1150° F.).

The tensile, notch impact and bend tests of arc and oxyacetylene welded Cu-Ni-Cr steel sheet reported by Epstein, Nead and Halley[4] Tables 4, 5 and 6, Steel C, showed that the welded steel was not characterized by high strength and ductility compared with some of the other steels that were tested. The notch-impact value of the heat-affected zone at −25° C. was exceptionally high.

Table 28—Charpy Notch-Impact Value (Ft.-Lb.) of Arc-Welded Cu-Ni-Cr Steel. Hopkins and Blumberg[74]

	+21° C	−46° C.	−60° C.	−80° C.	−100° C.
Weld	23.6	19.4	17.3	16.3	10.8
Heat-affected zone	50.7	35.6	48.2	44.4	37.2
Base metal*	57.3–66.2	38.3–49.6	44.9–44.9	31.2–38.2	14.4–19.3

* First value with specimen transverse to direction of rolling, second value with specimen parallel to direction of rolling.

ACKNOWLEDGMENTS

Contributions to the review have been received from:

H. Cornelius, Deutsche Versuchsanstalt für Luftfahrt, e. v. Berlin-Adlershof, Germany.

H. H. Febrey, American Steel and Wire Company.

C. H. Lorig, Battelle Memorial Institute.

BIBLIOGRAPHY

1. Lorig, C. H., and MacLaren, A. W., *Alloys of Iron and Copper*, McGraw-Hill Book Co., N. Y., 103–104 (1934).
2. Nehl, F., *Elektroschweg.*, **7**, 81–86 (1936).
3. Zeyen, K. L., and Mehl, H., *Schmelzschw.*, **10**, 264–268 (1931); *Cuivre et Laiton*, **5**, 465–471 (1932).
4. Epstein, S., Nead, J. H., and Halley, J. W., *Trans. A. I. M. E.*, **120**, 309–339 (1936).
5. Orr, J., *Trans. Instn. Engrs. Shipbldrs. Scotland*, **78**, 320–329 (1934–35).
6. Jennings, C. H., *Metal Progress*, **25** (3) 30–34, March 1934.
7. Stäger, *Arcos*, **13**, 1533–1535 (1936).
8. Copper in Cast Steel and Iron, Copper Development Assn. Publ. 29 (1937).
9. *Metallurgie*, **68**, No. 10 FP, 17–23 (1936).
10. Harris, H., Metallic Arc Welding, Longmans, N. Y. (1935).
11. *Autog. Metallb.*, **22**, 243–255, 265–270 (1929).
12. Kemper, H., *Schmelzschw.*, **11**, 156–158 (1932).

Table 27—Mechanical Properties of Arc-Welded Cu-Cr-Mo Steels. Meunier and Rosenthal[73]

	Base Metal = 0.20 C, 0.37 Mn, 0.38 Si, 0.59 Cr, 0.5 Cu, 0.1 Mo			Base Metal = 0.235 C, 0.47 Mn, 0.27 Si, 0.47 Cr, 0.55 Cu, 0.14 Mo		
	Unwelded	1 Layer	4 Layers	Unwelded	1 Layer	4 Layers
Yield strength, lb./in.²	57,000	...	...	57,000	...	...
Tensile strength, lb./in.²	78,000	...	...	80,500	...	...
Elongation, % in 7 diam.	23.6	...	...	23.6	...	...
Bend angle, degrees	180	115	180	180	90	180
Bend elongation, per cent	45	20	47	42	17	46
Notch-impact value (Mesnager), mkg./cm.²	14	11.5	17.5	13	10	18
Maximum Brinell hardness	145	235	190	155	250	185

13. Miller, H. L., and Lichtenwalter, T. R., *Steel*, **101** (16) 62–63, Oct. 18, 1937.
14. Séférian, D., and Leroy, A., *Proc. Twelfth Intnl. Cong. Acet.*, 763–780 (1936).
15. Portevin, A., *Proc. Twelfth Intnl. Cong. Acet.*, 197–217 (1936).
16. Hobart, H. M., *Trans. A. I. M. E.*, **62**, 569–613 (1920); *General Electric Rev.*, **22**, 445–460 (1919); *Weld. Engr.*, **4** (12) 40–55 (1919).
17. Holler, H., and Frankenbusch, H., **38**, 337–344, 359–363 (1935).
18. Ronay, B., *Jnl. Amer. Soc. Naval Engrs.*, **48**, 532–552 (1936).
19. Tris, P. C., and Kapetensky, M., *Proc. Steel Treating Research Soc.*, **2**, 18–24, 59 (1919).
20. Knox, H. G., *Trans. A. I. M. E.*, **62**, 660–661 (1919).
21. Wanamaker, E., and Pennington, H. R., *Electric Arc Welding*, Simmons-Boardman Publ. Co., N. Y. (1921).
22. Wiss, E., *Autog. Metallb.*, **22**, 46–53, 58–66, 74–79 (1929).
23. Burgess, C. F., and Aston, J., *Electrochem. & Met. Engg.*, **7**, 436–438 (1909).
24. Bonsmann, F., and Püngel, W., *Werkstoffhandbuch Stahl und Eisen*, E11, 1–5 (1939).
25. Schäfer, R., *Die Konstruktionsstähle, Springer, Berlin*, 125–129 (1923).
26. Hahn, P., *Stahl und Eisen*, **45**, 7–9 (1925).
27. Clevenger, G. H., and Ray, B., *Trans. A. I. M. E.*, **47**, 523–561 (1913).
28. Stead, J. E., and Evans, J., *Jnl. Iron & Steel Inst.*, **59**, 89–100 (1901).
29. Colby, A. L., *Iron Age*, **64**, 1–7, Nov. 30, 1899.
30. Lipin, W., *Stahl und Eisen*, **20**, 583–590 (1900).
31. Lohmann, F. W., *Mitt. Forschinst., Ver. Stahlwerke Dortmund*, **3**, 267–294 (1932–33).
32. Keel, C. F., *Z. Schweisstech.*, **27**, 168–176, 208–213, 235–241 (1937).
33. Haardt, E., *Elektroschweg.*, **5**, 161–164, 193–196 (1934).
34. Swinnerton, N. W., *Weld. Ind.*, **5**, 180–188 (1937).
35. Gailor, C. F., *Weld. Engr.*, **9** (1) 23–24 (1924); (carbon arc welding is described in *Railway Signalling*, **24**, 209–210 (1931)). J. B. Austin (*Coal Age*, **26**, 330–334 (1924)) secured good fatigue and impact results with the Cu-Si electrode and carbon molds.
36. *Engineer*, **142**, 370–371 (1926).
37. Tupholme, C. H. S., *Elec. Rev.*, **96**, 731 (1925).
38. Austin, J. B., *Iron & Steel Engr.*, **7**, 319–324 (1930).
39. Davis, *Acetylene Jnl.*, **10**, 140–143 (1908); Febrey, H. H., American Welding Society Journal, **2** (12) 18–24 (1923).
40. Jennings, C. H., How to Weld 29 Metals, Westinghouse E. & M. Co., 100 pp. (1937).
41. Steele, L. J., and Martin, H., *Jnl. Instn. Elec. Engrs.*, **60**, 136–157 (1921).
42. Matting, A., and Lessel, W., *Weld. Ind.*, **5**, 93–97 (1937).
43. Samesreuther, R., *Verfahrenstechnik*, (2) 68–71 (1937).
44. Vollmaier, A., and Feldmann, P., *Werkstatt u. Betrieb*, **71**, 234 (1938).
45. Otto, J., *AEG Mitt.*, 308–310 (1925).
46. Hensel, F. R., *Weld. Ind.*, **5**, 394–406 (1937).
47. Webber, H. M., *Trans. Amer. Soc. Steel Treat.*, **18**, 1144–1156 (1930).
48. Smith, M. C., *Weld. Engr.*, **19** (11) 18–19 (1934).
49. Jennings, C. H., *Iron Age*, **136**, 22–25, 178, July 4, 1935.
50. Weinman, R. A., *General Electric Rev.*, **32**, 532–537 (1929).
51. Raymond, E. J., *Mech. & Weld. Engr.*, **5**, 220–224 (1931).
52. Goodrich, W. E., *Jnl. Iron & Steel Inst.*, **132**, 43–66 (1935).
53. Comstock, G. F., *Iron Age*, **112**, 1381–1382 (1923).
54. Hook, I. T., American Welding Society Journal, **15** (2) Suppl., 7–31; (3) Suppl., 33–46 (1937).
55. Kinzel, A. B., and Crafts, W., *Alloys of Iron and Chromium*, **1**, McGraw-Hill Book Co., N. Y. (1937).
56. Kinzel, A. B., Crafts, W., and Egan, J. J., *Trans. A. I. M. E.*, **125**, 560–583 (1937).
57. Zeyen, K. L., *Tech. Mitt. Krupp*, **6**, 25–46 (1938).
58. Gerritsen, W., and Schoenmaker, P., *Polytechnisch Weekblad*, **27**, 465–472 (1933).
59. Sommer, F., *Elektroschweg.*, **2**, 225–227 (1931).
60. Mercier, *Rev. Mét.*, **33**, 553–555 (1936).
61. Williams, A., *Industry & Weld.*, **11** (3) 40–41, 44, March 1938.
62. Gibson, A. E., American Welding Society Journal, **16** (2) Suppl., 2–6 (1937); **14** (9) 2–8 (1935); *Iron Age*, **138**, 75–76, Dec. 10, 1936; Williams, A., *Industry & Weld.*, **10** (5) 34–36; (7) 24–25, 30 (1937).
63. Esslinger, F. J., American Welding Society Journal, **15** (1) 18–19 (1936).
64. Warner, W. L., American Welding Society Journal, **15** (10) Suppl., 21–32 (1936); Watertown Arsenal Report No. 642.1/21, 9 pp. (1932).
65. Critchett, J. H., American Welding Society Journal, **17** (1) Suppl., 8–14 (1938).
66. Doussin, L., *Bull. Soc. Ing. Soud.*, **5** (28) 1188–1204 (1935); **6** (37) 1939–1959 (1935).
67. Kessner, A., and Specht, H., *Arch. Eisenhüttenwes.*, **9**, 231–239 (1935).
68. Czternasty, *Autog. Metallb.*, **30**, 337–346 (1937).
69. Whitmer, V. W., *Industry & Weld.*, **10** (2) 62–63, 70 (1937).
70. Miller, H. L., American Welding Society Journal, **14** (12) 28–30 (1935).
71. *Soudure*, **3** (4) 125, July–Aug. 1937.
72. Schottky, H., and Ruttmann, W., *Wärme*, **61**, 144–147 (1938).
73. Meunier, F., and Rosenthal, D., *Rev. Mét.*, **33**, 588–593 (1936); *Assn. Ing. École Mines Mons*, **59** (4) 35 pp. (1936).
74. Hopkins, R. K., and Blumberg, H. S., *Trans. A. I. M. E.*, **125**, 591–593 (1937).
75. Hook, I. T., *Acetylene Jnl.*, **30**, 277–280 (1929).
76. *Industry & Welding*, **8** (1) 57–59, Jan. 1936.
77. Hoff, P., *Mitt. Kohle-u. Eisenfschg. G. m. b. H.*, **2**, Lfg. 1, 65–69, Feb. 1938.
78. Petiniaud, *Bull. Tech. Bureau Veritas*, **20**, 101 (1938).
79. Spraragen, W., American Welding Society Journal, **3** (4) 36–47 (1924).
80. Miner, D. F., American Welding Society Journal, **1** (7) 27–36 (1922).
81. Handy, J. O., *Ind. & Engg. Chem.*, **5**, 884–895 (1913).
82. *A. R. E. A. Bull.*, **34** (348) 44–45, Aug. 1932.
83. Strokopitov, V. I., and Guzenko, I. G., *Avtog. Delo*, **10** (6) 8–9 (1939).
84. Talmage, T. D., *Telephone Engr.*, **36** (10) 18–19, 22(1932); **35** (3) 30 (1931); Du Bois, G., *Cuivre et laiton* **7**, 419–423 (1934).

SUGGESTED RESEARCH PROBLEMS

Copper is not a trouble maker in the welding of copper steels. Consequently, there appear to be no pressing problems to suggest, aside from the general one of determining the mechanical properties that may be expected in welded joints made by different processes (including resistance welding) in the low-alloy structural steels containing copper.

Of the three chief effects of copper on steel: corrosion, red shortness, and precipitation hardening, corrosion presents no problems, red shortness is not feared in the low-alloy structural steels containing copper, and precipitation hardening is said not to affect the ductility of the heat-affected zone. Only the third effect seems to merit any further study, which might be directed toward a complete evaluation of the action of copper in multilayer welding.

Since precipitation of copper in steel must correspond with considerable atomic rearrangement, it is natural to inquire whether shrinkage stresses are increased or decreased by precipitation. Does the expansion accompanying precipitation afford relief to shrinkage stresses under some circumstances?

The flash and spot welding of copper and copper alloys (tinned or plain) to steel (galvanized or plain) is practiced to a considerable extent. The conditions necessary for successful welding are not generally known, nor have the softening effects, alloying effects, and possibilities of intergranular penetration been determined fully.

Two minor statements in the review seem to deserve attention from researchers. First, it has been said that most copper-bearing steels are subject to undercutting in arc welding. Perhaps the effect of copper in promoting undercutting might provide a clue to its fundamental causes. Second, some investigators found astonishing improvement in mechanical properties of welded low-alloy structural steels containing copper through heat treatment, whereas others found none. The difference in results has not been explained.

TESTS OF PLUG AND SLOT WELDS*

By G. E. TROXELL†

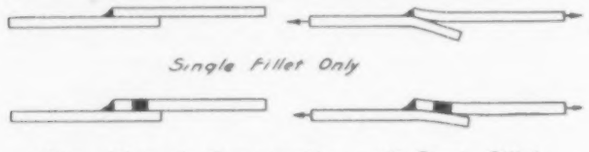
INTRODUCTION

IN CONNECTING the various members of a steel structure, any one of a variety of connection types may be employed. The joining of members by means of plug and slot welds has been proposed when connection by riveting or fillet welding is impossible, insufficient or relatively uneconomical. In the plug-welding process, two overlapping members are connected by filling a round hole in one member with weld metal which fuses to the rim of the hole in that member and to the adjacent face of the other member. When the hole is slot shaped, the weld is called a slot weld.

Many applications have been suggested[1] where plug and slot welds can be used to advantage over other types of welds. The two applications which follow will suffice to illustrate the point. In many cases, both edges of the plates in a lap joint are not accessible for fillet welding. The fillet welding of one edge only, results in failure at low loads due to the combination of shearing and high bending stresses which occur. As shown in Fig. 1, the combination of a plug weld with a single fillet provides the necessary additional shearing area for resistance to large axial loads; it also prevents severe distortion of the plates and high bending stresses in the fillet which occur when the fillet is used alone.

Plug and slot welds may be used to strengthen girders by welding on cover plates, as shown in Fig. 2. In such cases, fillet welds alone may prove insufficient in strength. According to the AMERICAN WELDING SOCIETY Specifications for Buildings (1934) and for Highway and Railway Bridges (1938),[2] the maximum width W between parallel lines of fillet welds connecting built-up members is 30 t and 24 t, respectively, where t is the thickness of the thinner member joined. As the width of plate between fillets may exceed the maximum for which connection by fillet welds alone is permitted, plug or slot welds might be used to provide the additional strength and stiffness in lieu of splitting the plate and using four fillet welds for the connection.

Fig. 1—Stiffening Effect of Plug Weld Combined with Single Fillet



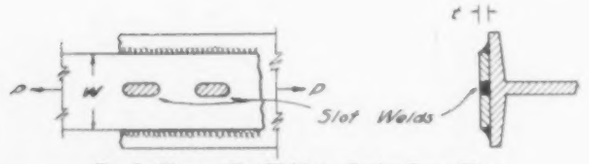


Fig. 2—Plug or Slot Welds in Girder Cover Plate

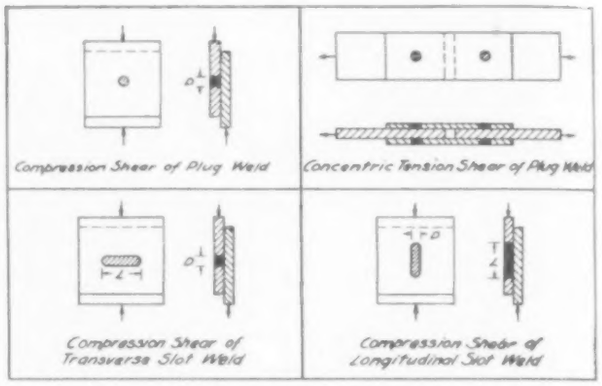

Compression Shear of Plug Weld
Concentric Tension Shear of Plug Weld
Compression Shear of Transverse Slot Weld
Compression Shear of Longitudinal Slot Weld

Fig. 3—Types of Specimens Tested

Plug and slot welds have not been used to any considerable extent due to the lack of data on the mechanical properties of this type of connection. However, in 1937, Gibson reported[1] the results of a fairly comprehensive program of tests, including specimens subjected to concentric tension shear, eccentric tension shear, compression shear, and other special loadings. Although Gibson's tests included many variables, all of the possible combinations of size of hole, depth of plug, thickness of plate, etc., were not covered, and check tests were not made for every condition.

SCOPE OF INVESTIGATION

The general purpose of this investigation was twofold. First, it was desired to investigate the strength and uniformity of plug and slot welds welded in the flat position by a faster, continuous and more simple welding procedure than the intermittent one used at Lehigh University in 1937 to fabricate the majority of Gibson's specimens, and second, the purpose was to determine the strength of plug welds of larger sizes than had heretofore been tested, so that the range of available data might be extended. An overlap with previous tests was, of course, desirable as a check, and so was provided for. A large range of size of hole, thickness of plate and depth of filling of plug was included in the test program to determine the most desirable relation between size of hole and thickness of plate, and the most desirable relation between size of hole and depth of filling of plug.

All specimens were of the filled or partly filled type;

* Contribution to Fundamental Research Division, Welding Research Committee.

† Associate Professor of Civil Engineering, University of California.

[1] Gibson, Glenn James, "An Investigation of Plug and Slot Welds," AMERICAN WELDING SOCIETY JOURNAL, Welding Research Supplement, July 1937, pp. 10–21.

[2] AMERICAN WELDING SOCIETY JOURNAL, Welding Research Supplement, May 1938, p. 18.

fillet type plug welds were not considered. Two types of plug-weld shearing specimens were used: eccentric compression shear specimens and concentric tension shear specimens. The compression shear specimens shown in Fig. 3 were chosen for the majority of the tests because of the relative economy of this type of specimen. They were tested in a special testing rig described later. The concentric tension shear specimens shown in Fig. 3 were included to determine the strength of plug welds tested in this manner in comparison with those tested in compression shear. A group of slot-welded specimens was included to determine the effect of the ratio of the length to width of slot welds upon the unit shearing strength. Slots having their longer axis parallel to the direction of the applied load (longitudinal slots) and those with the long axis perpendicular to the applied load (transverse slots) were tested as shown in Fig. 3.

PREPARATION OF SPECIMENS

All specimens were fabricated in the Alameda Works of the Bethlehem Steel Company. The steel plate was rolled in accordance with A. S. T. M. Designation *A*9–36, the standard specification for steel for buildings. All holes for the plug-weld specimens were drilled. The slots were drilled and chipped to size. In order to obtain results comparable to those for rivets utilizing the same size of hole, all plug welds were made in the regular rivet hole sizes, e.g., $^{13}/_{16}$ in., $^{15}/_{16}$ in., etc. The edges of the compression shear specimens were rounded to prevent the plates from digging in to each other when tested.

Except for the first seven specimens fabricated (Specimens *A*-1*a* to *A*-7*a*) in which heavy-coated, general-purpose welding electrode was used, all welds were made using heavy-coated electrode designed for "flat welding of deep groove joints in mild steel." The weld metals of both rods have a tensile strength of 66,000 to 74,000 psi. "Reversed" polarity current (in which the welding electrode is positive and the work negative) was used for all specimens because it seemed to give better penetration with the electrodes used.

Two distinct welding procedures, designated as *A* and *B*, were used in fabricating the plug-weld specimens. The former is the conventional intermittent procedure, whereas the latter is a much simpler, continuous one. Procedure *A* was used by Gibson in welding the majority of the specimens made at Lehigh University in 1937. It was used in the present investigation to weld one series of specimens (Series *E*) so that the results of this procedure might be compared with those secured by the use of procedure *B*.

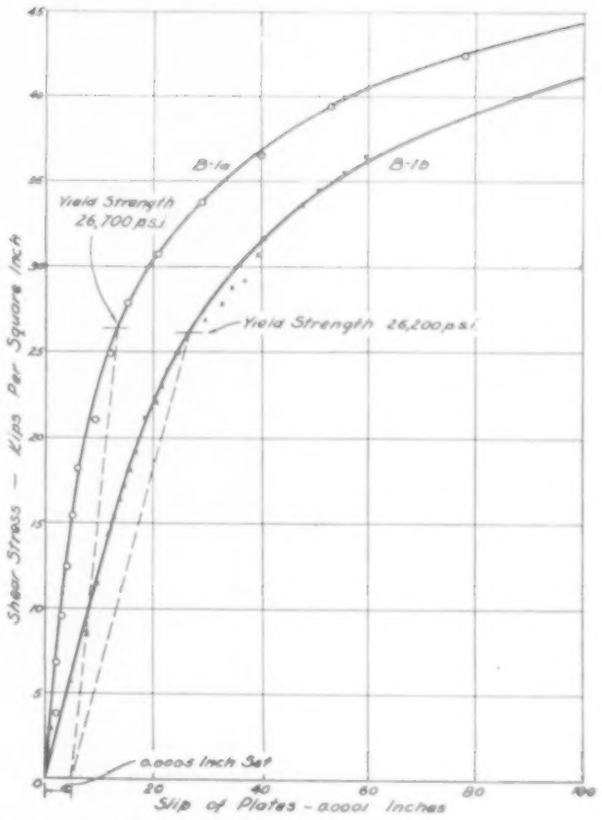


Fig. 5—Typical Stress-Slip Diagrams: Concentric Tension Shear Specimens *B1a* and *B1b*, with Two 7 x $^{1}/_{2}$-In. Plates and $^{13}/_{16}$-In. Filled Plugs

Welding Procedure "A."—All welds by procedure *A* were made using $^{3}/_{16}$-in. heavy-coated electrode using the maximum current (225 amperes at 35 volts) of the range recommended by the manufacturer. In the first pass with the electrode the corner between the edge of the hole and the plate is filled up. The electrode is inclined about 30 degrees from the vertical edge of the hole and about the normal amount in the direction of welding. These inclinations of the rod and the circular direction of the weld obviously require considerable manipulation of the electrode to keep it pointed in the correct direction, but from the indications of Gibson's tests this technique is necessary to insure full corner fusion and to make the contact area of the plug equal to the area of the hole. Another limitation of this procedure is that the ratio of diameter of hole to thickness of plate must be sufficient so that the welding rod can

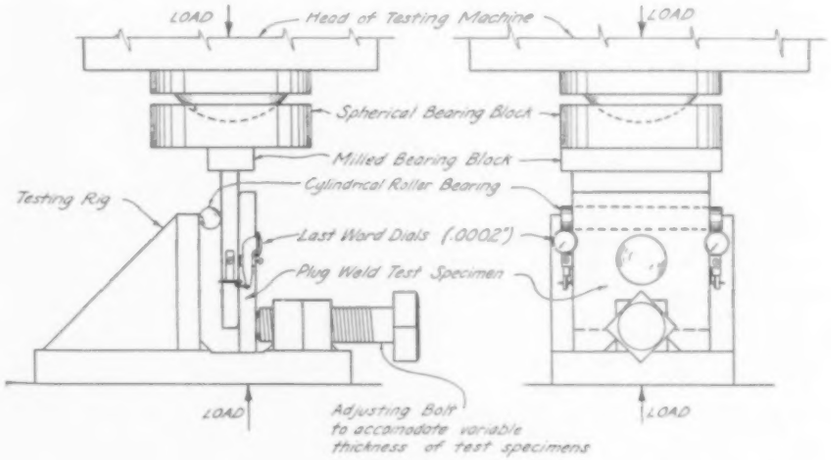


Fig. 4—Arrangement of Compression Shear Specimen in Testing Rig

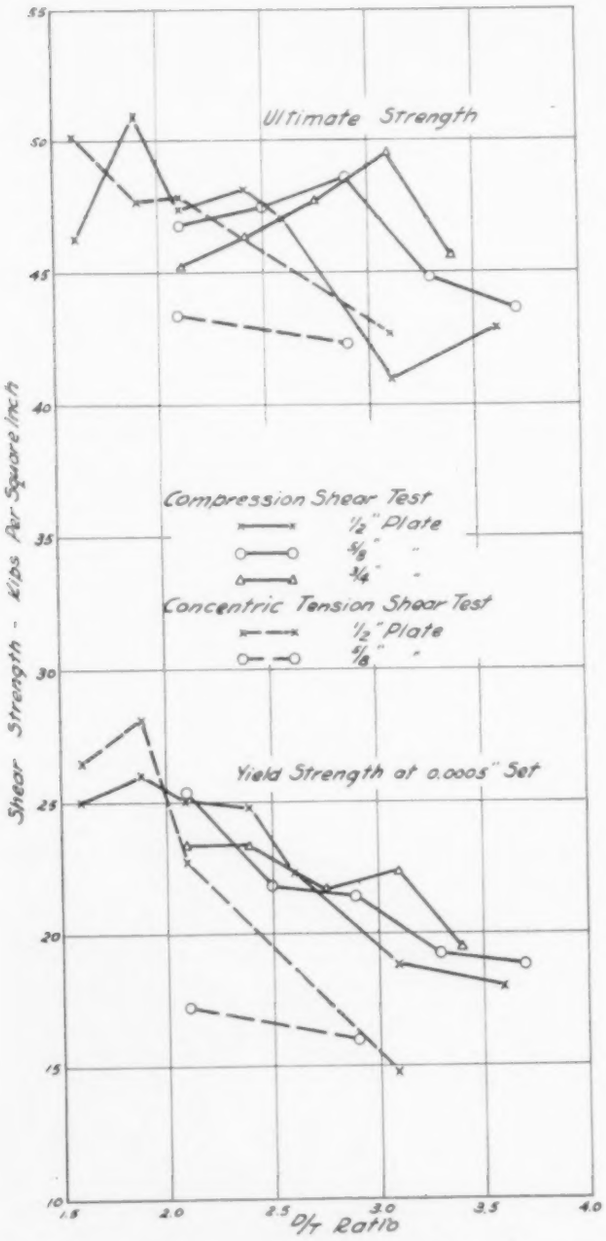


Fig. 6—Effect on Shear Strength of Ratio of Filled Plug Diameter to Plate Thickness

be properly inclined without touching the top edge of the hole. After the first pass, which forms a corner fillet, the center portion is filled up by the ordinary weaving motion. It is possible to perform these first two operations continuously and build up the plug weld about $^1/_8$ in. The slag is then chipped and blown out of the hole. By similar succeeding passes the hole is finally filled the required amount. The appearance of the finished weld can be easily made very good, but this is not an index to the quality of the weld. The most important part, the first layer, is covered up.

The necessity for chipping out the layer of slag after each pass constitutes a severe economic handicap of this welding procedure *A*. To make this type of plug welding successful commercially, it has been suggested that a welder work on a series of holes at one time and be followed by a helper who chips the slag and cleans out the holes.

Welding Procedure "B."—The majority of the specimens for this investigation were welded using a procedure designed to eliminate the handicap features of procedure *A*. It will be designated as procedure *B* and involves the welding of the plug all in one operation by allowing the molten slag to be floated out. It is only adaptable to welding in the flat position. As for procedure *A*, the first pass with the electrode serves to fill up the corner between the edge of the hole and the plate. In this case, however, the rod is held practically vertical (within 5 degrees) during the entire welding operation. This considerably simplifies the manipulation of the electrode in comparison with procedure *A*, for which case the angle of inclination is 30 degrees. The ordinary weaving motion for flat welding completes the first pass. This is repeated without stopping until the series of passes brings the plug to the desired level. The actual welding is thus a continuous process until this level is reached, or the welding rod is used up and requires replacement. An exceptionally high arc current is used to furnish sufficient heat to keep the slag in a molten condition so that it floats up ahead of the plastic weld metal deposited. The surrounding metal is hot enough from the heat of the high current so that ordinarily the slag does not freeze around the edges of the hole and thus prevent fusion between the weld and the plate, as has been experienced when using the lower arc currents of procedure *A*.

Procedure "B" Arc Currents and Electrodes.—The plug-weld compression shear specimens were all welded with $^5/_{16}$-in. rod at 375 amperes and 35 volts, except for the $^{13}/_{16}$ and $^{15}/_{16}$-in. plugs which were welded with $^1/_4$-in. rod at 325 amperes and 35 volts. The smaller rod was used for these small holes to slow up the operation and give the welder better control of the welding process.

The tension shear specimens were welded using higher currents than were used for the compression shear specimens. This was necessary to get good penetration in the thick (1, $1^1/_4$ and $1^1/_2$ in.) center plates because of the rapid heat absorption of these thick plates. For the $^{13}/_{16}$ and $^{15}/_{16}$-in. holes in $^1/_2$ in. plate, $^1/_4$-in. electrode at 475 amperes and 40 volts was used. All other specimens were welded with $^5/_{16}$-in. rod at 550 amperes and 45 volts. Both of these currents are above the maximum recommended current, so that the electrode had an especially high melting rate.

The slot-welded specimens were welded using $^5/_{16}$-in. electrode at 550 amperes and 45 volts. Even with this high current it was difficult to keep the slag in the long slots in at least a semi-molten condition so that the process could be continuous. This difficulty would account for the slag inclusions and lack of complete fusion found in the ends of some of these slots after testing. Because of the large electrode consumption in the longer slots, only about two continuous passes or layers of weld metal were deposited before the welding was stopped to replace the used electrode. The slag was of course chipped out before welding could begin again.

Effect of the Welding Current.—The effect of the welding current upon the strength of the weld should not be overlooked when considering the results of the tests for these specimens. Tests by Gibson showed that with a $^3/_{16}$-in. rod, the ultimate plug-weld shear strength varied from 35,000 psi to 50,000 psi using 150 amperes and 300 amperes, respectively. For intermediate currents the strengths were in direct proportion. These tests substantiated the theory that the shear strength depends upon the amount of penetration. The increased heat deepens and increases the area of the "burnt" surface of the fusion zone where plug welds normally fail. It thus might have been desirable that

the arc currents and size of electrodes be the same for all welds tested in this investigation to eliminate this variable. It was felt, however, that the results of tests upon specimens welded with what appeared to be the best adaptable electrode sizes and currents, such as would actually be used in commercial production, would be of more value.

TESTING PROCEDURE

The compression shear specimens were tested using a special rig shown in Fig. 4. Two dial gages were mounted on each specimen, as shown, to determine the displacement or slip of the two plates during the application of the load. The concentric tension shear specimens were held in the heads of the testing machine by the conventional wedge grips. The two dial gages mounted on these specimens were arranged to measure the displacement of the two outer plates with respect to the heavy inner plate. Dial readings of the slip were taken without stopping the testing machine. All tests were carried to complete failure of the plug. The approximate average diameter of the root of the fractured weld, as well as a description of the fracture, was recorded for all specimens.

The yield strength at a set of 0.0005 in. was determined for each specimen, by means of stress-slip curves as shown in Fig. 5. The stress corresponding to the point of intersection of the curve and a straight line paralleling the initial tangent of the curve, but at 0.0005 in. from it, determines the value of the yield strength at 0.0005 in. set. This set was an arbitrarily chosen value that seemed reasonable for these tests.

TEST RESULTS FOR FILLED PLUGS

Previous investigations have pointed out that the minimum ratio of the diameter of hole D to the thickness of plate T (D/T ratio) is limited by the smallest hole which can be successfully welded in a given thickness of plate. The maximum ratio is limited by the maximum diameter of hole for a given plate thickness that can develop the full facial shear strength between the plug and the plate. An enlargement of the hole beyond this maximum D/T ratio results in a reduction in the unit strength because primary failure occurs (1) by tearing of the plug from the tension side of the hole, (2) by diagonal shear through the plug, or (3) by progressive failure due to stress concentrations. Furthermore, if the D/T ratio is smaller than the maximum value which will produce full facial shear, the use of the filled plug is uneconomical, as some partly filled plug would produce full facial shear. Tests by Gibson have shown the optimum D/T ratio to be somewhere between 2.0 and 3.0. One object of the present investigation was to determine this optimum ratio.

All of the compression shear and concentric tension shear specimens having filled plugs failed in facial shear, i.e., along the plane between the two plates. Failure usually occurred through the burnt area of the fusion zone, extending $^1/_{32}$ to $^1/_{16}$ in. into the base plate. In the larger plugs, failure occurred partly through this burnt area and partly through the weld metal itself. At the ultimate load, the compression shear specimens had plate displacements varying from $^1/_{16}$ in. for the small plugs to $^3/_8$ in. for the large ones, while the tension shear specimens had displacements varying from $^1/_{16}$ to $^1/_8$ in.

The test results for filled plugs welded by the continuous procedure "B" are presented in Table 1. The effect of the ratio of the plug diameter to plate thickness (D/T ratio) upon the ultimate shearing strength and the yield strength is shown in Fig. 6. This figure indicates that a D/T ratio larger than about 2 produces a proportional decrease in the yield strength. This may be due to a progressive type fracture, resulting from concentrations of stress, which lowers the average shearing yield strength as the size of plug (or D/T ratio) increases. The average shearing yield strength for the filled plugs reported in Table 1, which were tested in compression shear, was 22,000 psi; the average for the concentric tension shear specimens was 20,800 psi.

In general, considering ultimate strength, the optimum D/T ratio for the compression shear tests appears to be about 2 for $^1/_2$-in. plate, and increases to about 3 for $^3/_4$-in. plate. For the tension shear tests, the optimum ratio appears to be about 2. The average ultimate strength of the tension shear specimens shown in Table 1 is 45,700 psi, while the strength of the corresponding compression shear specimens is 46,900 psi and the average for all compression shear specimens is 46,500 psi. These values are for the continuous welding procedure B. It is of interest to compare them with the results of the tests made at Lehigh University for specimens welded by the intermittent procedure A for which the average strength of the filled plug specimens tested in compression shear was 41,800 psi; for the concentric tension shear specimens the average was 41,000 psi.

TEST RESULTS FOR PARTLY FILLED PLUGS

Theoretically, the depth of plug should just be sufficient to develop the full facial shear strength of the plug.

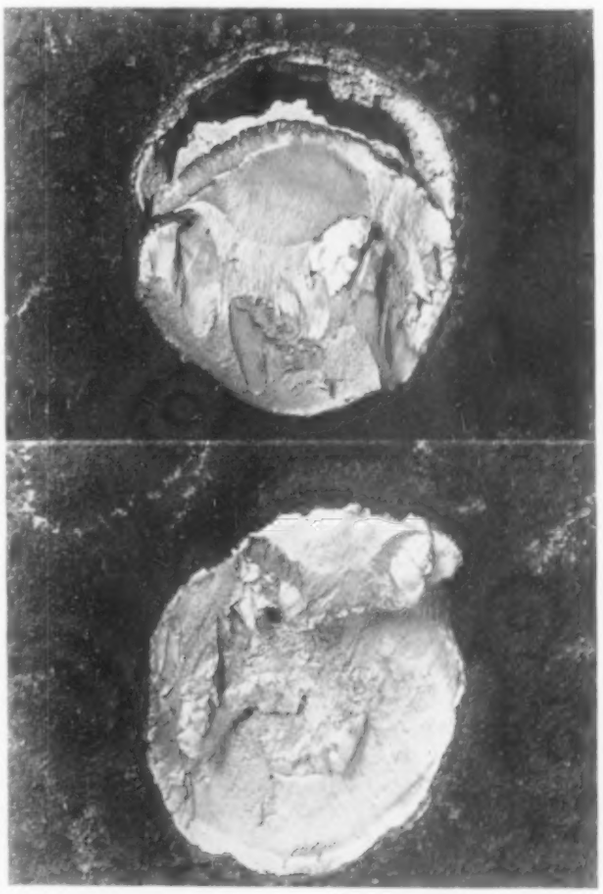


Fig. 7—Typical Tearing on Tension Side of Partly Filled Plug Having Shallow Filling or of Large Diameter Filled Plug in Thin Plate. Base Plate After Facial Shear Failure Is Shown in Lower Part of Photograph

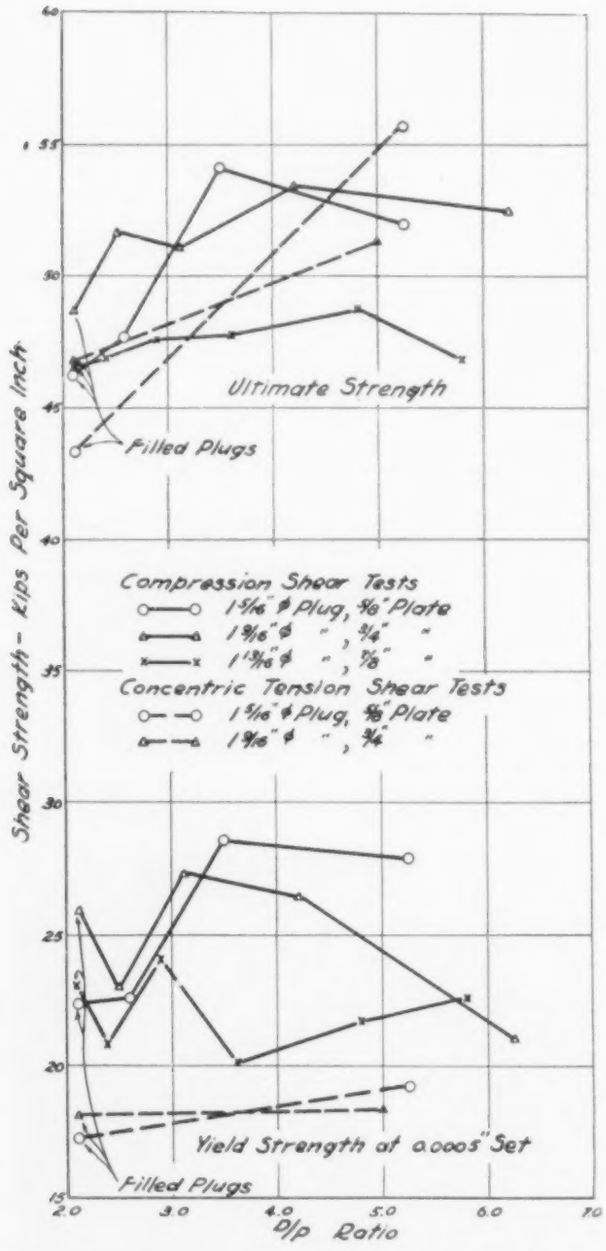


Fig. 8—Effect on Shear Strength of Ratio of Plug Diameter, D, to Depth of Filling, P

This depth of plug produces an optimum ratio of the diameter of hole D to the depth of plug P and gives the greatest strength per pound of weld metal. It will be apparent that the D/P ratio and the D/T ratio are synonymous when the hole is completely filled.

Most of the partly filled plug specimens failed in facial shear although a few of the thinner plugs (having a large D/P ratio) failed on the tension side of the plug as shown in Fig. 7. Fewer tension failures occurred than might be expected, due largely to the area of contact between the weld metal and the sides of the hole being greater than the depth of plug P would indicate. This excess area to resist tension failures is shown in Fig. 9*c*.

The results of the tests on partly filled plugs are presented in Table 2. The average ultimate strength of the compression shear specimens of this series was 49,400 psi; the average for the concentric tension shear specimens was 49,300 psi which is practically the same strength. The effect of the D/P ratio upon the ultimate and yield strengths of partly filled plugs in $^5/_8$, $^3/_4$ and $^7/_8$-in. plates is shown in Fig. 8. That the ultimate strength of a given diameter of plug in a given thickness of plate actually decreased when the hole was filled above a certain depth is evident from a study of this figure. Table 3 shows the optimum plug depth and D/P ratio for three combinations of plate thickness and diameter of hole.

It is believed that the greater strength of the plugs filled only to the optimum depth is due mostly to smaller shrinkage stresses in this type, and, to a minor degree, upon their finer grain structure. Larger shrinkage stresses would be expected in the filled plugs than in the partly filled plugs because of the greater contraction upon cooling of the larger mass of weld metal in the filled plugs. The relatively smaller mass of molten weld metal in a partly filled plug and the small amount of heat liberated, should allow more rapid solidification, resulting in a finer grain structure for partly filled plugs. Microscopic examinations verified this tendency in a general way.

The D/P ratio apparently has no pronounced effect upon the yield strength except in the case of the plugs having a diameter of $1^5/_{16}$ in., for which condition the yield strength is increased as the D/P ratio is increased, at least within the limits of the tests.

EFFECT OF WELDING PROCEDURE

The test results for specimens of $^1/_2$-in. plate welded by the intermittent procedure A and for similar specimens welded by the continuous procedure B are pre-

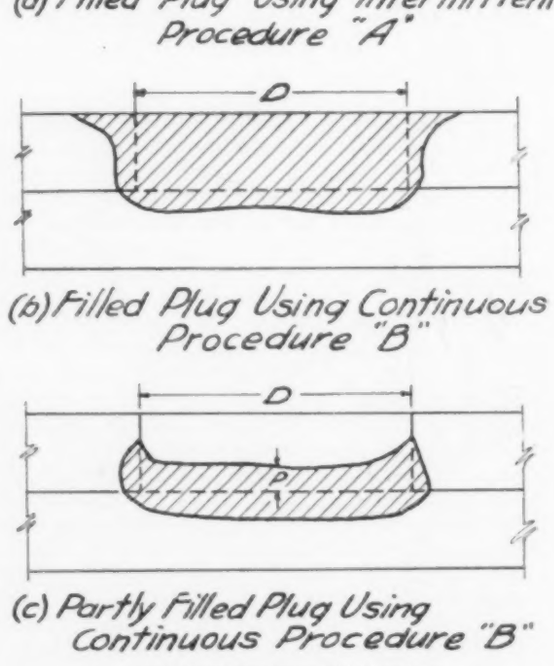


Fig. 9—Typical Plug Weld Sections

Table 1. — Results of Shear Tests of Filled Plugs Welded by Continuous Procedure "B"

Specimen No.	Diam. Plug D, in. Nominal[1]	Actual[2]	Test Plate, WxT, in.[3]	D/T Ratio	Yield Strength at .0005 in. Set, psi. Indiv.	Average	Ult. Load, kips	Ultimate Shear Strength, psi. Indiv.	Average	Remarks
COMPRESSION SHEAR SPECIMENS										
A 1a	13/16	13/16	4 x 1/2	1.6	25000	-	24.0	46300	-	Poor fusion on tension side.
A 2a	15/16	15/16	4 x 1/2	1.9	26100	-	35.2	51000	-	Blow hole on tension side.
A 3a	1-1/16	1-1/16	4 x 1/2	2.1	25000	-	42.0	47400	-	-
A 4a	1-3/16	1-7/32	4 x 1/2	2.4	24800	-	53.5	48300	-	One 1/8-in. blow hole.
A 5a	1-5/16	1-11/32	4 x 1/2	2.6	22200	-	63.6	47100	-	Poor fusion for 1 in. on comp.side
A 6a	1-9/16	1-9/16	6 x 1/2	3.1	18800	-	78.7	41000	-	Few small holes.
A 7a	1-13/16	1-7/8	6 x 1/2	3.6	18000	-	110.9	42900	-	Few small holes.
A 9a	1-5/16	1-5/16	6 x 5/8	2.1	27500	25350	65.0	48000	46800	-
A 9b	1-5/16	1-3/8	6 x 5/8	2.1	23200		61.7	45600		
A 10a	1-9/16	1-5/8	6 x 5/8	2.5	22300	21800	88.1	45900	47450	-
A 10b	1-9/16	1-5/8	6 x 5/8	2.5	21300		94.0	49000		
A 11a	1-13/16	1-7/8	6 x 5/8	2.9	22000	21400	125.3	48500	48600	-
A 11b	1-13/16	1-15/16	6 x 5/8	2.9	20800		126.0	48700		
A 12a	2-1/16	2-1/8	6 x 5/8	3.3	20800	19300	152.0	45200	44850	Plates buckled slightly.
A 12b	2-1/16	2-1/8	6 x 5/8	3.3	17800		149.5	44500		Plates buckled slightly.
A 13a	2-5/16	2-3/8	6 x 5/8	3.7	18800	18800	187.0	44500	43700	Plates buckled slightly.
A 13b	2-5/16	2-3/8	6 x 5/8	3.7	18800		180.0	42900		Plates buckled slightly.
A 14a	1-9/16	1-5/8	6 x 3/4	2.1	24800	23400	89.5	46500	45300	-
A 14b	1-9/16	1-5/8	6 x 3/4	2.1	22000		84.7	44100		
A 15a	1-13/16	1-13/16	6 x 3/4	2.4	23500	23400	122.6	47300	46450	-
A 15b	1-13/16	1-13/16	6 x 3/4	2.4	23300		117.8	45600		One 1/16-in blow hole.
A 16a	2-1/16	2-1/8	6 x 3/4	2.7	20500	21700	158.8	47400	47850	Plates buckled slightly.
A 16b	2-1/16	2-1/8	6 x 3/4	2.7	22900		162.2	48300		Plates buckled slightly.
A 17a	2-5/16	2-3/8	6 x 3/4	3.1	22500	22500	210.0	50000	49600	Plates buckles slightly.
A 17b	2-5/16	2-7/16	6 x 3/4	3.1	22500		206.8	49200		Plates buckled slightly.
A 18a	2-9/16	2-5/8	6 x 3/4	3.4	19000	19500	232.3	45000	45750	One small blow hole.
A 18b	2-9/16	2-11/16	6 x 3/4	3.4	20000		240.3	46500		Plates buckled slightly.
CONCENTRIC TENSION SHEAR SPECIMENS										
B 1a	13/16	13/16	7 x 1/2	1.6	26700	26450	51.4	49500	50200	-
B 1b	13/16	13/16	7 x 1/2	1.6	26200		52.9	50900		Three small blow holes.
B 2a	15/16	15/16	7 x 1/2	1.9	28100	28050	65.8	47700	47650	Five blow holes; 1/16 in.to 1/4 in.
B 2b	15/16	16/16	7 x 1/2	1.9	28000		65.6	47600		Two 1/16-in.blow holes; one 1/4 in.
B 3a	1-1/16	1-9/16	7 x 1/2	2.1	24300	22650	85.4	48000	47900	-
B 3b	1-1/16	1-9/16	7 x 1/2	2.1	21000		85.0	47800		One small blow hole.
B 4a	1-9/16	1-9/16	7 x 1/2	3.1	15000	14750	167.2	43500	42600	-
B 4b	1-9/16	1-9/16	7 x 1/2	3.1	14500		160.0	41700		-
B 5a	1-5/16	1-5/16	7 x 5/8	2.1	17000	17300	116.1	42900	43350	1/16 by 1/2-in. hole.
B 5b	1-5/16	1-5/16	7 x 5/8	2.1	17600		118.9	43800		Two 1/16-in.blow holes; one 1/8 in
B 6a	1-13/16	1-13/16	7 x 5/8	2.9	15000	16000	213.8	41300	42300	-
B 6b	1-13/16	1-7/8	7 x 5/8	2.9	17000		223.8	43300		-

1 - Size of hole.
2 - Includes penetration on sides of hole.
3 - Two plates for all tension specimens.

sented in Table 4 and in Fig. 10. In general, this figure shows that the ultimate and yield strengths of the plugs welded by procedure *A* are greater than for those welded by procedure *B*, although the ultimate strengths do not differ very much. Figure 9 shows that the penetration for the continuously welded *B* plugs, all of which were made by the use of a greater arc heat, is somewhat greater than for the intermittently welded *A* plugs, although the penetration was good for both types.

An interesting comparison of the two procedures and the results obtained are presented in Table 5. This comparison appears to favor the *A* procedure slightly, at least so far as quality is concerned. This may be due in part to the use of the older type general-purpose "shielded arc" electrode for the filled plugs of this series welded by procedure *B*, although all other welding was done with a "shielded arc" electrode, which has a coating especially designed for deep groove welding. The slag of this latter rod is lighter and apparently more easily floated up in the continuous welding procedure *B*, and there is thus less danger of entrapping slag around the edges of the hole than when using the general-purpose electrode. Nevertheless, in other series, slag inclusions were found to be somewhat more prevalent in the procedure *B* plugs than in the procedure *A* plugs, even when welded with the rod designed for deep groove welding.

The shrinkage stresses in the procedure *A* plug welds are believed to be considerably less than those in the procedure *B* plugs because of the difference in the two welding procedures. Each successive pass of weld metal deposited in the intermittent procedure *A* produces a normalizing action on the previously deposited metal, and thus probably relieves the shrinkage stresses. In contrast thereto, the cooling and solidification of the larger mass of molten weld metal in the procedure *B* plugs would ordinarily produce greater shrinkage stresses. Microscopic examinations of some of the weld sections revealed that the effect of the difference in crystalline structure for the two types of welds is probably insignificant in comparison with the effect of other variables.

While the procedure *A* plug may be somewhat superior

in strength to that of procedure *B*, this does not necessarily justify the use of the former in all cases. The simplicity and relative economy of procedure *B* may make it the proper choice. Even if the plug diameter must be increased, or the spacing of the plugs decreased, to develop adequate strength when using procedure *B* in preference to procedure *A*, it is plausible that the former procedure might be the most economical one.

TEST RESULTS FOR SLOT WELDS

The tests of slot welds were limited to compression shear specimens having longitudinal and transverse slot welds $^{13}/_{16}$ in. wide with a ratio of the length of slot to its width (L/D ratio) varying from 1.85 to 6.15. The test results are presented in Table 6 and in Fig. 11. All the specimens failed in facial shear, primarily through the burnt steel area in the base plate. Slag inclusions and lack of complete fusion at the ends of some of the slots were apparently due to the freezing of the slag before the next successive pass was made.

From Fig. 11 it can be seen that for the longitudinal slots there is a general tendency toward higher ultimate and yield strength as the L/D ratio increases. For the transverse slots, the yield strength is lower the larger the L/D ratio, while the ultimate strength has no definite trend, either up or down. Theoretically, the larger the L/D ratio for the longitudinal slot, the less the unit strength which might be expected because of an uneven stress distribution over its length, tending to produce a progressive failure. However, the thickness of plate of these specimens probably minimized this progressive type of failure. Another factor which might be expected to produce lower unit strengths as the L/D ratio becomes larger is the tendency for slag inclusions to occur in the longer slots.

The test results may be explained by the lower shrinkage stresses which are believed to exist in the longer slots due to the difference in the welding procedure used for the short and long slots. In the short slots the welding is a continuous process; the deposited metal remains very hot until the slot is completed and then cools as a unit, producing high shrinkage stresses. The welding of the longer slots was not entirely continuous, however, because the quantity of metal deposited

Table 2. -- Results of Shear Tests of Partly Filled Plugs Welded by Continuous Procedure "B"

Specimen No.	Diam. Plug D, in. Nominal[1]	Actual[2]	Test Plate W x T, in.[3]	Depth Plug P, in.	D/P Ratio	Yield Strength at .0005 in. Set, psi. Indiv.	Average	Ult. Load, kips	Ultimate Shear Strength, psi. Indiv.	Average	Remarks
COMPRESSION SHEAR SPECIMENS											
C 1a	1 - 5/16	1 - 3/8	4 x 5/8	5/8	2.1	22000	22350	62.5	46100	46300	-
1b	1 - 5/16	1 - 3/8	4 x 5/8	5/8	2.1	22700		63.0	46500		Small blow hole in center.
C 2a	1 - 5/16	1 - 5/16	4 x 5/8	1/2	2.6	25200	22600	62.6	46200	47700	Three small blow holes.
2b	1 - 5/16	1 - 5/16	4 x 5/8	1/2	2.6	20000		66.7	49200		One small blow hole.
C 3a	1 - 5/16	1 - 5/16	4 x 5/8	3/8	3.5	28000	28600	69.0	50900	54150	Few very small blow holes.
3b	1 - 5/16	1 - 7/16	4 x 5/8	3/8	3.5	29200		77.7	57400		Four small blow holes.
C 4a	1 - 5/16	1 - 5/16	4 x 5/8	1/4	5.2	28800	27950	66.2	48900	52000	-
4b	1 - 5/16	1 - 3/8	4 x 5/8	1/4	5.2	27100		74.6	55100		Four small blow holes.
C 5a	1 - 9/16	1-11/16	6 x 3/4	3/4	2.1	27300	25900	94.2	49000	48650	-
5b	1 - 9/16	1 - 5/8	6 x 3/4	3/4	2.1	24500		92.9	48300		-
C 6a	1 - 9/16	1-11/16	6 x 3/4	5/8	2.5	22200	23000	92.1	48000	51650	-
6b	1 - 9/16	1 - 3/4	6 x 3/4	5/8	2.5	23800		106.3	55300		Few small blow holes.
C 7a	1 - 9/16	1 - 5/8	6 x 3/4	1/2	3.1	26600	27400	93.6	48700	51100	-
7b	1 - 9/16	1-11/16	6 x 3/4	1/2	3.1	28200		102.7	53500		Few small blow holes.
C 8a	1 - 9/16	1 - 5/8	6 x 3/4	3/8	4.2	26500	26500	101.1	52600	53500	-
8b	1 - 9/16	1-11/16	6 x 3/4	3/8	4.2	26500		104.4	54400		Few small blow holes.
C 9a	1 - 9/16	1-11/16	6 x 3/4	1/4	6.2	21500	21000	103.3	53900	52500	-
9b	1 - 9/16	1 - 9/16	6 x 3/4	1/4	6.2	20500		98.1	51100		Tension failure, plug center
C 10a	1-13/16	1 - 7/8	6 x 7/8	7/8	2.1	21600	23050	117.9	45600	46600	Three 1/16-in. blow holes.
10b	1-13/16	1 - 7/8	6 x 7/8	7/8	2.1	24500		123.0	47600		Few small blow holes.
C 11a	1-13/16	1 - 7/8	6 x 7/8	3/4	2.4	21500	20850	115.8	44800	46850	1/16-in. blow hole.
11b	1-13/16	1 - 7/8	6 x 7/8	3/4	2.4	20200		126.3	48900		Four 1/16-in. blow holes.
C 12a	1-13/16	1-13/16	6 x 7/8	5/8	2.9	23000	24100	122.7	47600	47650	Three 1/8-in. blow holes.
12b	1-13/16	1 - 7/8	6 x 7/8	5/8	2.9	25200		123.3	47700		Two 1/8-in. blow holes.
C 13a	1-13/16	1 - 7/8	6 x 7/8	1/2	3.6	20000	20100	122.6	47400	47850	Tension failure, side of hole
13b	1-13/16	1 - 7/8	6 x 7/8	1/2	3.6	20200		124.9	48300		Tension failure, side of hole
C 14a	1-13/16	1-15/16	6 x 7/8	3/8	4.8	22200	21750	131.7	50900	48800	Tension failure, side of hole
14b	1-13/16	1 - 7/8	6 x 7/8	3/8	4.8	21300		120.5	46700		1/16-in. blow hole.
C 15a	1-13/16	1-15/16	6 x 7/8	5/16	5.8	20500	22600	118.7	45900	46850	Tension failure, side of hole
15b	1-13/16	1 - 7/8	6 x 7/8	5/16	5.8	24700		123.5	47800		1/16 x 1/4-in. slag inclusion
CONCENTRIC TENSION SHEAR SPECIMENS											
B 5a	1 - 5/16	1 - 5/16	7 x 5/8	5/8	2.1	17000	17300	116.1	42900	43350	1/16 x 1/2-in. hole.
5b	1 - 5/16	1 -5/16	7 x 5/8	5/8	2.1	17600		118.9	43800		Two 1/16-in. holes; one 1/8 in
D 1a	1 - 5/16	1 - 3/8	7 x 5/8	1/4	5.2	20800	19300	151.0	55700	55700	11 small blow holes.
1b	1 - 5/16	1 - 5/16	7 x 5/8	1/4	5.2	17800		151.0	55700		-
D 2a	1 - 9/16	1 - 9/16	7 x 3/4	3/4	2.1	18500	18250	179.3	46600	46900	-
2b	1 - 9/16	1 - 5/8	7 x 3/4	3/4	2.1	18000		181.2	47200		-
D 3a	1 - 9/16	1 - 5/8	7 x 3/4	5/16	5.0	19000	18600	195.8	50900	51450	1 plug failed on tension side
3b	1 - 9/16	1 - 5/8	7 x 3/4	5/16	5.0	18200		200.0	52000		2 plugs failed on tension side

1 - Size of hole.
2 - Includes penetration on sides of hole.
3 - Two plates for all tension specimens.

Table 3—Optimum D-P Ratios for Partly Filled Plugs Welded by Procedure "B"

Hole Diameter, In.	Plate Thickness, In.	Optimum Plug Depth, In.	Optimum D/P Ratio
$1^{5}/_{16}$	$^{5}/_{8}$	$^{3}/_{8}$	3.5
$1^{9}/_{16}$	$^{3}/_{4}$	$^{3}/_{8}$	4.2
$1^{13}/_{16}$	$^{7}/_{8}$	$^{3}/_{8}$	4.8

necessitated replacing the used-up electrode several times before the slot was filled to the required depth. Before beginning to weld again, the slag was cooled and chipped off. This procedure probably reduced the shrinkage stresses in the long slots as a result of the normalizing action of the intermittent layers, and hence increased their unit strengths.

It has been shown experimentally that fillet welds parallel to the direction of loading are about 75 per cent as efficient as those transverse to the direction of loading. This is due to the tendency toward progressive failure of the former type. Since longitudinal and transverse slot welds act in somewhat the same manner as corresponding fillet welds, it appears reasonable to presume that transverse slot welds should be stronger than longitudinal slot welds. However, Fig. 11 shows that, in general, the reverse is true, the average ultimate strengths for the longitudinal and transverse slots tested being 51,500 and 50,300 psi, respectively. This may have been due to the use of thick plates which kept the stress distribution over the longitudinal welds more nearly uniform so that they did not fail by progressive fracture.

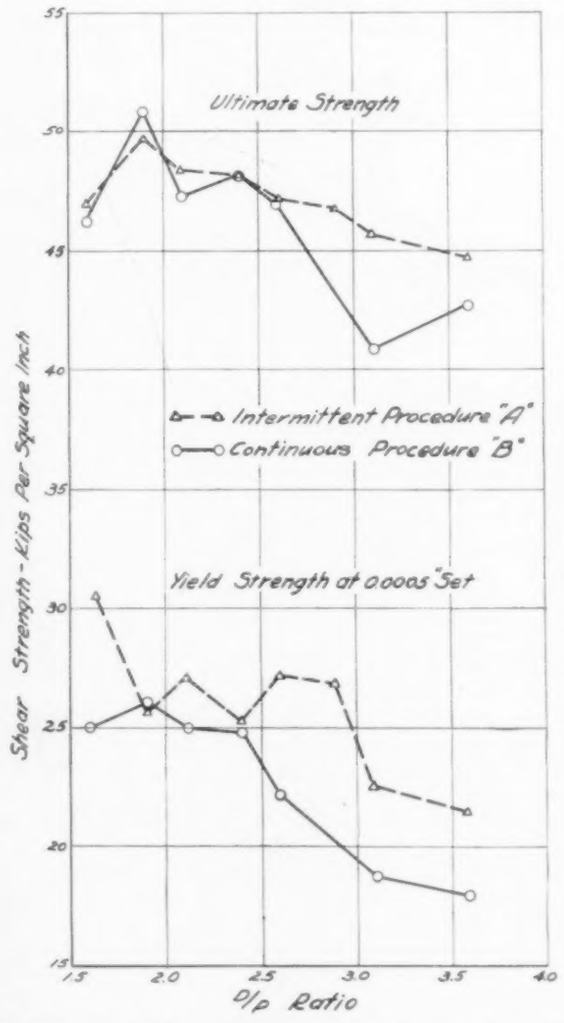


Fig. 10—Effect of Welding Procedure on Shear Strength of Filled Plugs in $^{1}/_{2}$-In. Plate

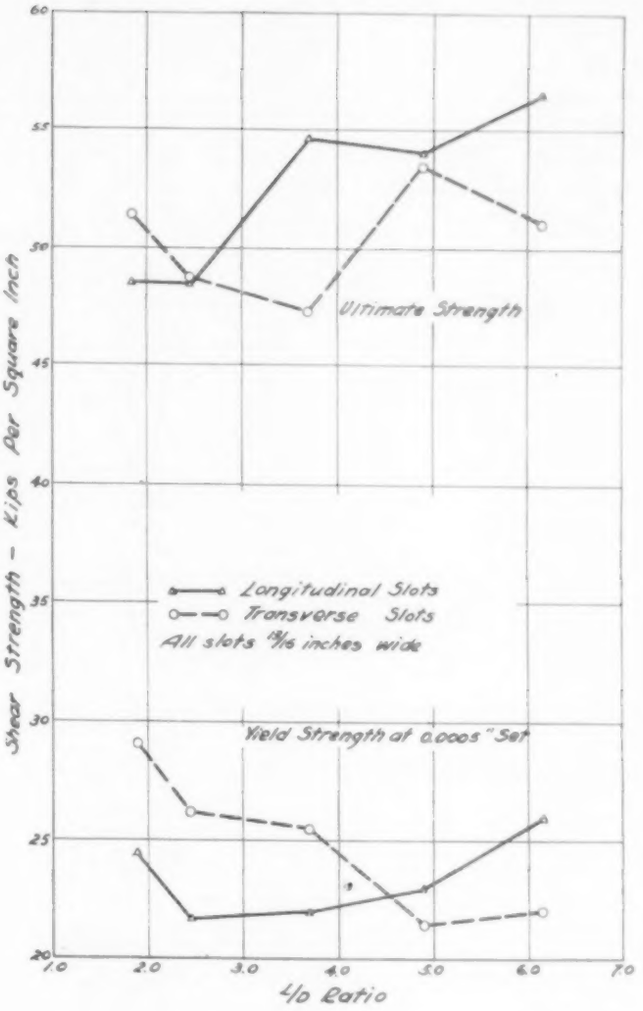


Fig. 11—Effect on Shear Strength of Ratio of Length to Width of Slot Welds

SUMMARY AND CONCLUSIONS

The following conclusions may be drawn from the investigation:

1. Complete face fusion between the weld metal and the base plate is possible with the intermittent procedure *A*, and with the continuous welding procedure *B* when an appropriate electrode designed for deep groove welding is used.

2. Even with a few blow-holes and small slag inclusions, plug welds made by the continuous procedure *B* are stronger than the plug welds made by the intermittent welding procedure *A* at Lehigh University in 1937. The filled plugs tested in compression shear averaged 46,500 and 41,800 psi, respectively, for the two groups. At least part of the difference between these average values is due to the higher welding current used in the present investigation.

Table 4. -- Effect of Welding Procedure on Shear Strength

Compression Shear Specimens With Filled Plugs

Specimen No.	Diam. Plug D, in. Nominal[1]	Diam. Plug D, in. Actual[2]	Test Plate WxT, in.	D/T Ratio	Yield Strength at .0005 in. Set, psi. Indiv.	Yield Strength at .0005 in. Set, psi. Average	Ult. Load, kips	Ultimate Shear Strength, psi. Indiv.	Ultimate Shear Strength, psi. Average	Remarks
INTERMITTENT PROCEDURE "A"										
E 1a	13/16	1 - 3/16	4 x 1/2	1.6	30700	30850	22.9	44100	47000	-
1b	13/16	1 - 3/16	4 x 1/2	1.6	31000		25.9	49900		-
E 2a	15/16	15/16	4 x 1/2	1.9	26700	25350	33.2	48200	49850	1/16—in. blow hole.
2b	15/16	1	4 x 1/2	1.9	24000		35.5	51500		-
E 3a	1- 1/16	1- 1/16	4 x 1/2	2.1	27300	27150	41.2	46400	48500	-
3b	1- 1/16	1- 1/8	4 x 1/2	2.1	27000		45.0	50600		-
E 4a	1- 3/16	1- 3/16	4 x 1/2	2.4	28000	25400	53.7	48400	48300	-
4b	1- 3/16	1- 3/16	4 x 1/2	2.4	22800		53.5	48200		One small blow hole.
E 5a	1- 5/16	1- 5/16	4 x 1/2	2.6	25100	27200	62.1	45900	47250	-
5b	1- 5/16	1- 5/16	4 x 1/2	2.6	29300		65.8	48600		-
E 6a	1- 7/16	1- 1/2	6 x 1/2	2.9	25800	26900	73.7	45400	46850	-
6b	1- 7/16	1- 1/2	6 x 1/2	2.9	28000		78.4	48300		-
E 7a	1- 9/16	1- 5/8	6 x 1/2	3.1	24000	22650	87.0	45300	45700	-
7b	1- 9/16	1- 5/8	6 x 1/2	3.1	21300		88.5	46100		-
E 8a	1-13/16	1-15/16	6 x 1/2	3.6	22500	21450	114.2	44200	44750	-
8b	1-13/16	1- 7/8	6 x 1/2	3.6	20400		117.0	45300		-
CONTINUOUS PROCEDURE "B"										
A 1a	13/16	13/16	4 x 1/2	1.6	25000	-	24.0	46300	-	Poor fusion on tension side.
A 2a	15/16	15/16	4 x 1/2	1.9	26100	-	35.2	51000	-	Blow hole on tension side.
A 3a	1- 1/16	1- 1/16	4 x 1/2	2.1	25000	-	42.0	47400	-	-
A 4a	1- 3/16	1- 7/32	4 x 1/2	2.4	24800	-	53.5	48300	-	One 1/8-in. blow hole.
A 5a	1- 5/16	1-11/32	4 x 1/2	2.6	22200	-	63.6	47100	-	Poor fusion for 1 in.on comp.side
A 6a	1- 9/16	1- 9/16	6 x 1/2	3.1	18800	-	78.7	41000	-	Few small holes.
A 7a	1-13/16	1- 7/8	6 x 1/2	3.6	18000	-	110.9	42900	-	Few small holes.

1 - Size of hole.
2 - Includes penetration on sides of hole.

3. The average strength of filled plugs welded by the intermittent procedure *A* in this investigation was 47,300 psi in comparison with 46,300 psi for corresponding plugs welded by the continuous procedure *B*. The difference between the strengths resulting from these two procedures is probably due to greater shrinkage stresses and more slag inclusions and gas pockets in the continuously welded plugs.

4. The intermittent welding procedure *A* is more difficult and requires more time than the continuous procedure *B*. The latter method is the most economical one to use to develop a given total strength, even though the unit strength is slightly less than that possible with the intermittent procedure. Larger *B* plugs, or a closer spacing of such plugs, might be used to develop total strengths equal to those of the intermittently welded plugs.

5. For filled plug welds there is some diameter of plug for each plate thickness that will develop the maximum unit shearing strength. The optimum diameter is about 1 in. for $^1/_2$-in. plate, increasing to about $2^1/_4$ in. for $^3/_4$-in. plate.

6. For filled plug welds, the yield strength at 0.0005 in. set decreases as the ratio of diameter of hole to thickness of plate increases.

7. There is some depth of filling for each diameter of hole that will produce a maximum strength when using the continuous procedure *B*. The reduction in strength when filled beyond this depth is probably due to the increase in shrinkage stresses as the depth of plug is increased.

8. The unit strength of concentric tension shear specimens is generally slightly lower than for corresponding compression shear specimens, but the difference is not appreciable.

9. The continuous welding procedure used in making the slot welds gives satisfactory results.

10. For practical purposes, there is apparently little difference in the strength of longitudinal and transverse slot welds if the plate thickness is sufficient to distribute the load over the entire weld.

11. An increase in the ratio of length to width of slot from 1.85 to 6.15 for the $^{13}/_{16}$-in. width of slot did not result in a decrease in the unit ultimate strength of the slot welds.

12. Designs based upon the AMERICAN WELDING SOCIETY value for shear stress of 11,300 psi over the entire area of the hole are safe for plug and slot welds of the sizes tested in this investigation, whether welded by the continuous or by the intermittent procedures.

ACKNOWLEDGMENTS

The most sincere appreciation is extended to J. F. Clarke and L. R. Illingworth, civil engineering students at the University of California, who conducted this investigation as an undergraduate thesis. They are to be commended for the excellent manner in which the work was accomplished. Acknowledgment is also due to Professor N. F. Ward for the macrostructure and microstructure examinations of the plug-weld sections. The writer desires to thank the Bethlehem Steel Company for furnishing the welded test specimens and to thank the several engineers of that company, including H. A. Schirmer, I. F. Kurtz and A. B. Charlton, who gave so generously of their time in planning the test program.

Table 5. -- Summary of Comparison of Plug Welds Made by Welding Procedures "A" and "B"

	Items Compared	Procedure "A"	Procedure "B"
WELDING PROCEDURES	General	Welding intermittent, slag chipped out after each pass.	Welding continuous, slag floated up due to heat of high arc current.
	Arc currents	225 amps with 3/16" coated electrode.	325-475 amps with 1/4" electrode; 375-550 amps with 5/16" electrode (the higher currents being used in welding thick concentric tension shear specimens).
	Electrode manipulation	First pass around edge of hole made with rod 30° to vertical. Difficult.	Rod held practically vertical throughout entire welding operation. Simple.
RESULTS	Penetration	Good; usually less than with Procedure "B".	Usually greater than with Procedure "A".
	Slag inclusions	Less chance than with Procedure "B".	Chances greater than with Procedure "A".
	Shrinkage stresses	Probably smaller due to normalizing of intermittent layer type welding procedure.	Probably larger due to cooling of larger mass of hot weld metal deposited by continuous welding process.
	Crystalline structure	Generally somewhat finer (effect probably insignificant).	Generally somewhat coarser (effect probably insignificant).
	External appearance	Top of weld about 1/8"-1/4" larger than nominal hole diameter.	Top of weld about 3/4" larger than nominal hole diameter.
	Average shear strengths, filled plugs in 1/2" plate.	Ultimate = 47300 psi. Yield Str. at .0005" = 26000 psi.	Ultimate = 46,300 psi., 2% lower than "A". Yield Str. at .0005" = 22800 psi., 12% lower than Procedure "A".

Table 6. — Results of Compression Shear Tests of Slot Welds

Specimen No.	Actual Slot Width D, in.[1]	Length Slot L, in.	Test Plate WxT, in.	L/D Ratio	Yield Strength at .0005 in. Set, psi. Indiv.	Yield Strength at .0005 in. Set, psi. Average	Ult. Load, kips	Ultimate Shear Strength, psi. Indiv.	Ultimate Shear Strength, psi. Average	Remarks
LONGITUDINAL SLOTS										
F 1a	7/8	1-1/2	6 x 3/4	1.85	24500	24500	52.1	48300	48600	Numerous blow holes.
1b	7/8	1-1/2	6 x 3/4	1.85	24500		53.0	49000		No fusion at tension end.
F 2a	7/8	2	6 x 3/4	2.46	20500	20500	70.9	47700	46700	1-1/4 by 1/8-in. blow hole.
2b	15/16	2	6 x 3/4	2.46	20500		68.0	45800		5/8 by 1/4-in. slag inclusion.
F 2c	7/8	2	6 x 3/4	2.46	21000	21700	72.2	48600	48500	-
2d	7/8	2	6 x 3/4	2.46	22500		72.1	48500		Two 1/8 and two 1/16-in. blow holes
F 3a	1	3	6 x 3/4	3.69	22000	22000	123.9	53900	54700	-
3b	1	3	6 x 3/4	3.69	22000		127.5	55500		Few small blow holes.
F 4a	7/8	4	8 x 1	4.91	23000	23000	155.6	50000	54000	No fusion at one end.
4b	1	4	8 x 1	4.91	23000		180.3	58000		No fusion at one end.
F 5a	15/16	5	8 x 1	6.15	26000	26000	221.5	56500	56500	-
5b	15/16	5	8 x 1	6.15	26000		221.9	56500		One 1/4-in. blow hole.
TRANSVERSE SLOTS										
F 6a	7/8	1-1/2	6 x 3/4	1.85	31000	29000	59.0	54600	51400	Two 1/8 and two 1/16-in. blow holes
6b	15/16	1-1/2	6 x 3/4	1.85	27000		52.1	48300		No fusion at one end.
F 7a	7/8	2	6 x 3/4	2.46	25000	26200	71.7	48300	48600	-
7b	15/16	2	6 x 3/4	2.46	27500		72.7	48900		Small blow holes.
F 8a	7/8	3	7 x 1	3.69	26000	25500	109.9	47800	47300	No fusion at one end.
8b	15/16	3	7 x 1	3.69	25000		107.7	46900		No fusion at one end.
F 9a	13/16	4	9-1/2x1	4.91	24000	21500	158.2	50900	53500	One 1/4 and one 1/8-in. blow hole.
9b	15/16	4	9-1/2x1	4.91	19000		174.9	56100		One 1/4 by 3/8-in. hole.
F 10a	7/8	5	11-1/2x1	6.15	18000	22000	199.6	50900	51000	One 1/4-in. blow hole.
10b	7/8	5	11-1/2x1	6.15	26000		200.2	51100		-

1 - Includes penetration on sides of hole; nominal width of all slots, 13/16 in.

NOTE: Depth of Plug P, all specimens, 1/2 in.

NOTCHED-BAR TEST BEHAVIOR OF Some Welding Steels*

By C. E. JACKSON and E. A. ROMINSKI†

MODERN fabrication methods have definitely placed an additional requirement, that of "weldability," upon a commercial steel. The term "weldability" as applied to a steel has not received any uniform definition. In fact, each investigator has set up his own definition for term "weldability."

Thesinger[1] and Warner[2] have suggested the use of the maximum hardness obtained in a single-bead weld in conjunction with the resulting micro-structure as a means of evaluating the weldability of a steel. Bruckner[3] recommends the comparison of a half width keyhole-notched Charpy test specimen made on bars of quenched material and on parent steels in the "as-rolled" condition. Meunier and Rosenthal[4] proposed the use of a hardness survey, a bending test and a Charpy notched-bar test for evaluating the weldability of a steel; free-bend specimens were prepared by welding a bead on a plate and machining off the weld metal leaving the heat-affected plate back of the weld metal to be bent in tension; the notched-bar test specimens were made using a test specimen of the Messnager type, the bottom of the notch being tangent to the weld-metal parent-metal interface. Czyrski's[5] test specimens were made using a complete weld between two plates, one plate not being scarfed. This provided a uniform heat-affected side of the weld across which Charpy V-notched bar specimens could be broken. Other investigators have suggested other types of test specimens. In the last analysis, irregardless of the type of laboratory or other tests used for qualification, the term "weldability" of a steel refers to the degree of ease of producing a welded joint satisfactory for the purpose intended. Much of the confusion may be accounted for due to the fact that in design for welding, each joint is a problem in itself and that all variables, such as thickness of plate, speed of travel, rate of power input, etc., have their effect. A type of steel may be successfully welded in one application using probably a somewhat specialized welding technique while in another application this same composition would be considered more or less unsatisfactory. For example,[6] aircraft tubing of S. A. E. 4130 chrome-molybdenum steel can be handled without too much difficulty while welded plate of this composition would demand preheating and stress-relief annealing.

Any investigation based upon a single set of welding conditions is likely to be in no way conclusive. Sometimes a steel may have a sharp break in its metallurgical behavior as the cooling rates become gradually more drastic and approach the danger zone of the critical cooling rate. It is recognized that in making a weld joint the conditions accompanying the first bead usually subject the parent metal to the most drastic of thermal and metallurgical changes and if the material is capable of withstanding the shock of the first bead without damage a successful weld is possible.

Henry[7] suggested that the zone adjacent to a weld receiving the most drastic thermal shock might have entirely different properties when backed up by the modified or heat-treated metal normally found in a weld.

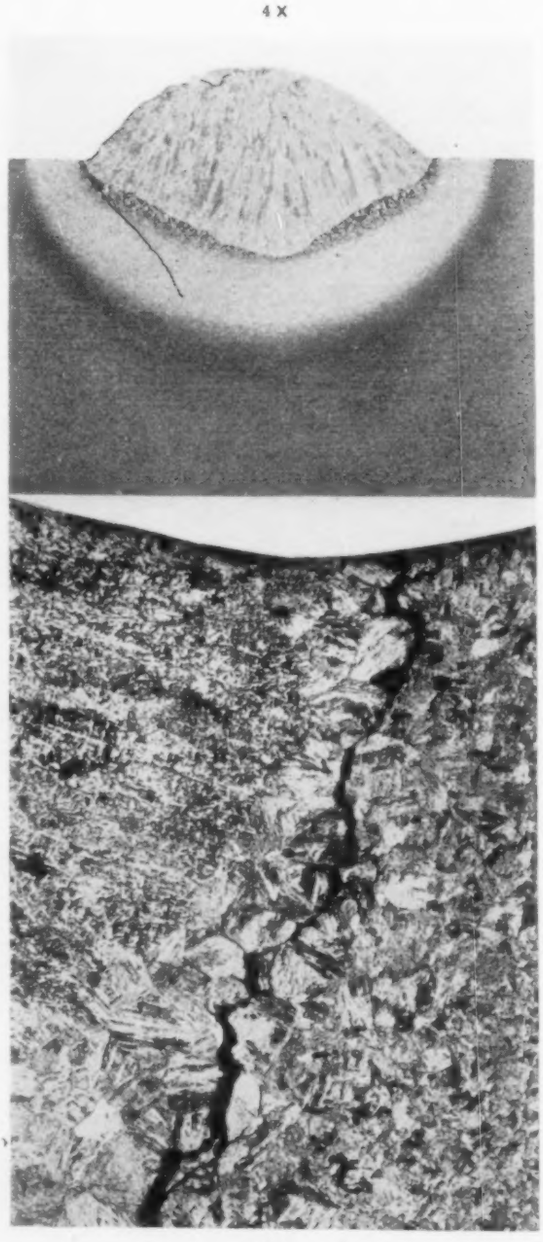


Fig. 1—Macro- and Micro-Photograph Showing Crack Formation Adjacent to a Bead Weld

A typical crack occurring adjacent to a bead weld is shown in Fig. 1. It is to be noted that the shape of the

* Published by permission of U. S. Navy Department.
† Naval Research Laboratory, Anacostia Station.

Table 1—The Chemical Analysis, Notched Bar Values in Foot-Pounds and Vickers Brinell Hardness for Steels Investigated

Steel	Chemical Composition—Per Cent								Charpy V-Notched Bar Specimen for As-Rolled Plate Material				Bead Weld Charpy V-Notched Bar Specimen		Weld Joint Charpy V-Notched Bar Specimen Standard Width			Hardness Vickers Brinell 10 Kg. Load Heat-Affected Zone			Steel
									½ Width (5 mm.)		Standard (10 mm.)	Double Width (20 mm.)	Standard	Double Width	Fusion Line	Annealed Zone	Plate	Max.	Average		No.
1	0.17	0.41	0.17						32	(D)	55	101	44	85	39	55	162	215	176		1
2	0.25	0.43	0.20						26	(D)	40	63	38	70	37	36	168	227	196		2
3	0.35	0.68	0.23						18	(D)	17.2	29	17.1	26	47	18	194	246	226		3
4	0.44	0.65	0.24						8.4		10	...	12	...	30	8	220	333	265		4
5	0.21	0.38	0.003						25	(D)	25	46	38	80	34	25	156	215	180		5
6	0.27	0.47	0.002						26	(D)	26	48	39	50	57	40	168	233	192		6
9	0.24	0.48	0.23						18	(D)	41	73	35	60	34	34	156	206	184		9
10	0.27	0.74	0.21						31	(D)	49	94	38	74	42	44	174	237	205		10
11	0.29	1.06	0.25						46	(D)	107	(D) 147	76	(D) 139	49	127	(D) 192	369	258		11
13	0.26	0.46	0.005						24	(D)	30	36	32	40	45	37	166	282	222		13
14	0.28	0.54	0.058						19	(D)	22	40	27	46	38	24	164	237	206		14
18	0.22	0.47	0.18			0.50			38	(D)	82	(D) 115	76	103	45	49	180	240	227		18
19	0.20	0.50	0.17			0.79			39	(D)	55	91	57	75	54	95	190	270	252		19
20	0.24	0.46	0.001						26	(D)	39	35	48	44	41	54	160	209	189		20
22	0.18	1.40	0.21					0.12	16	(D)	32	56	31	49	46	32	216	351	287		22
23	0.15	0.44	0.20		0.75	0.17			36	(D)	37	68	68	145	41	91	(D) 180	215	203		23
27	0.06	0.35	0.002	1.95	0.94				52	(D)	71	(D) +220 (D)	110 (D)	+220 (D)	82	110	(D) 174	195	186		27
29	0.028	0.19		2.08					57	(D)	163	(D) +220 (D)	129 (D)	+220 (D)	41	...	150	151	147		29
30	0.09	0.72	0.047	0.96	1.34	0.11			49	(D)	23	40	100 (D)	+220 (D)	48	23	212	246	239		30
32	0.12	0.75	0.48				0.34		54	(D)	121	(D) +220 (D)	118 (D)	+220 (D)	118 (D)	150	(D) 172	209	195		32
33	0.11	0.38	0.19			0.51	5.85		54	(D)	147	(D) 141 (D)	22 (C)	37 (C)	37	30	158	446	383		33
35	0.27	0.69	0.20	2.31					25	(D)	50	85	28	50	35	59	212	363	301		35
38	0.14	0.49	0.49			0.70	1.81		....		14	25	77	91	32	24	295	397	362		38

D = Ductile break.
C = Weld crack.

bead weld on the steel plate is such as to produce stress concentration at the toe of the weld. The crack initiates in the parent metal at this point adjacent to the fusion line and progresses in the direction of least resistance through the heat-affected zone.

It is felt that a full automatic control of welding conditions is a fundamental requisite in making test welds. While it is true that much perfect weld metal is deposited manually by skilled welders, it is equally true that test work requires the maintenance of uniform values of current, arc voltage, and speed of travel of electrode. As a result, after considerable experimental and design work, a full automatic welding control has been built which closely maintains the welding conditions to a predetermined value. This insures comparable and reproducible results.

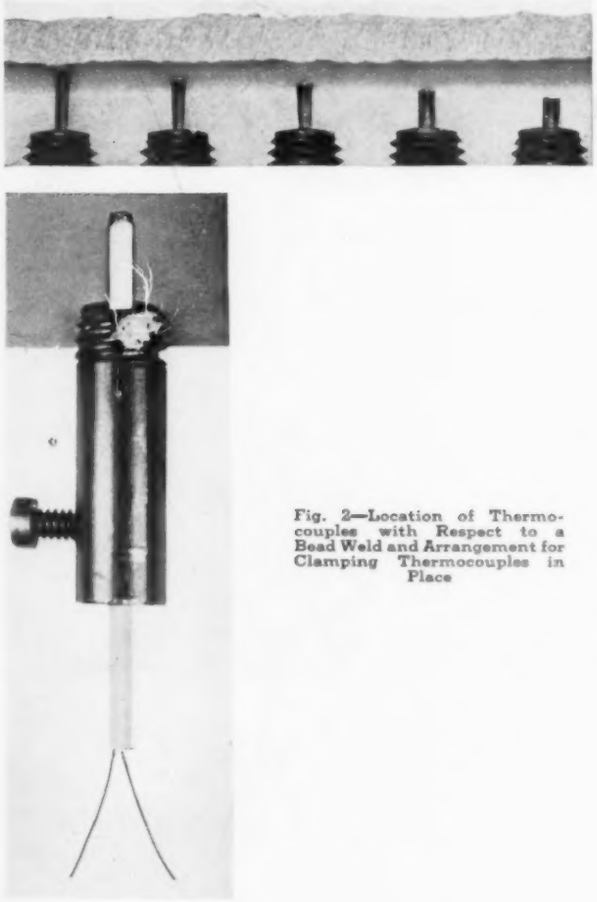
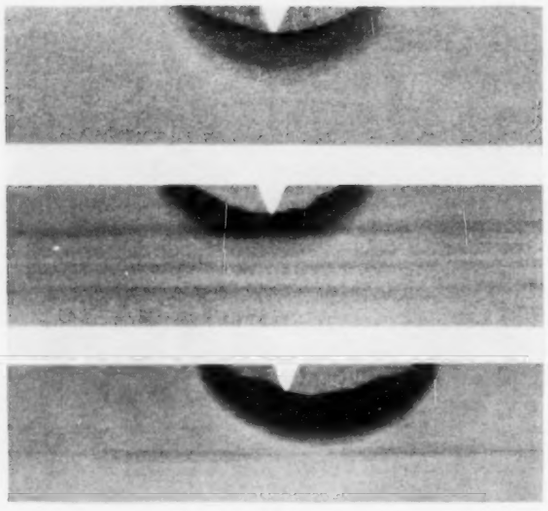


Fig. 2—Location of Thermocouples with Respect to a Bead Weld and Arrangement for Clamping Thermocouples in Place

The present investigation covers 23 hot-rolled laboratory and commercial steels whose chemical analyses are given in Table 1. The present research has been carried out in an attempt to determine an indication of the notched-bar behavior of the overheated area with its grain growth backed up by heat treated and parent metal.

Thermal conditions have been established showing the cycle of temperature through which the metal adjacent to the deposited metal passes. In the determination of this time-temperature relation, Pt, Pt-10% Rh thermocouples made from No. 40 B&S gage wire were mounted in 2 mm. (0.079 inch) thermocouple tubing and inserted into holes drilled in the bottom face of the test plate, the thermocouples being located at various distances from the deposited metal. This is clearly shown in Fig. 2. Care was taken to insure good contact of the thermocouple bead with the test plate. A movie camera was used for each thermocouple to record simultaneously the transient readings of a millivolt meter and stop-watch as the bead weld was made on the top face of the test plate. Type of electrode and power input has been varied in tests so far.

Single bead welds were deposited transverse to the direction of rolling on 6 x 7 x ½ inch plates in the as-rolled condition. The electrode was used with reverse polarity at 175 amperes, 25 volts, and a speed of travel of 6 inches per minute. An approach plate was used in order to make a greater portion of the test plate available for investigation. The plates were sectioned to give specimens for micro and macro examination. Five standard (0.394 x 0.394 x 2.165 inch) and two double width (0.394 x 0.788 x 2.165 inch) notched-bar bead weld specimens were also prepared. After



2 X

Fig. 3—Bead Weld Notched-Bar Specimens

these were ground and etched the location of the V-notch was determined by using a comparator or micrometer for measurement. The apex of the standard V-notch was machined tangent to the fusion line as in Fig. 3. Five standard and two double width specimens were also made for each plate material tested.

In a second series of test plates, single bevel groove welds were prepared, one plate being scarfed to a 45° angle, and the other being cut with no scarf. A backing strip was used with a root opening of slightly over $^3/_{16}$ inch. The weld was completed with six passes using reversed polarity, at 190 amperes, 27 volts and a travel of 5 inch per minute. During the first two beads, the plates required positioning in order to obtain full penetration at the root of the weld groove. Six standard width, notched-bar specimens were machined, three with the V-notch at the fusion line and three with the V-notch at the outer edge of the heat-affected zone as shown in Fig. 4. It is felt that weld joint, double width specimens would add much to the weld joint data.

Vickers Brinell hardness measurements have been made on the bead weld macro specimens using a 10-kg. load with the diamond pyramid. The notched-bar specimens were broken in an Amsler pendulum type machine at temperatures between 70° and 75° F.

The relation of thermal conditions, structure and hardness as the result of arc welding a mild carbon steel is presented in Fig. 5.

Notched-bar test values for the plate material, bead weld and weld joint specimens together with a hardness summary for the bead weld are presented in Table 1.

(*a*) *Facts Established*

Certain thermal distribution curves showing the time-temperature cycle in bead welds have been established.

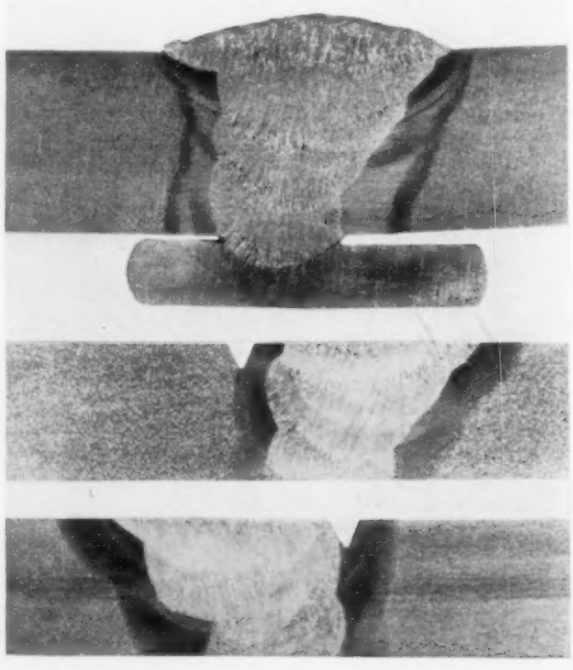
The use of standard width, 10 mm. (0.394 inch) and double width, 20 mm. (0.788 inch) Charpy notched-bar test specimens definitely gives information regarding toughness of plate material in the as-rolled and welded condition. This information is not given by one-half width, 5 mm. (0.197 inch) Charpy notched-bar test specimens or by any single test specimen.

Microstructure, thermal conditions and hardness values for a mild steel can be correlated with changes predicted by the iron-carbon equilibrium diagram. Since the heat-affected zone in a bead weld contains the whole gamut of heat-treated steel, studies of thermal conditions and microstructures may be used in predicting certain portions of equilibrium diagrams. The effects of low-alloy additions in shifting known equilibrium phases may also be studied. Fundamental data of this type would help much in a clearer understanding of the metallurgy that takes place in the parent metal adjacent to a weld zone.

Figure 2, a longitudinal cross section of a bead weld, shows the location of a set of five thermocouples used for measuring the thermal cycle at various distances from a bead weld. It is interesting to note the roll of the metal as it has been deposited. It was necessary to section the plate along the line of thermocouples in order to determine their distances from the weld metal. The curve to the right in Fig. 5 shows the relation of maximum temperature and distance from weld metal for a low-carbon steel. The curve is not continuous, a change in slope taking place between 774° and 910° C. indicating the A_3 transformation.

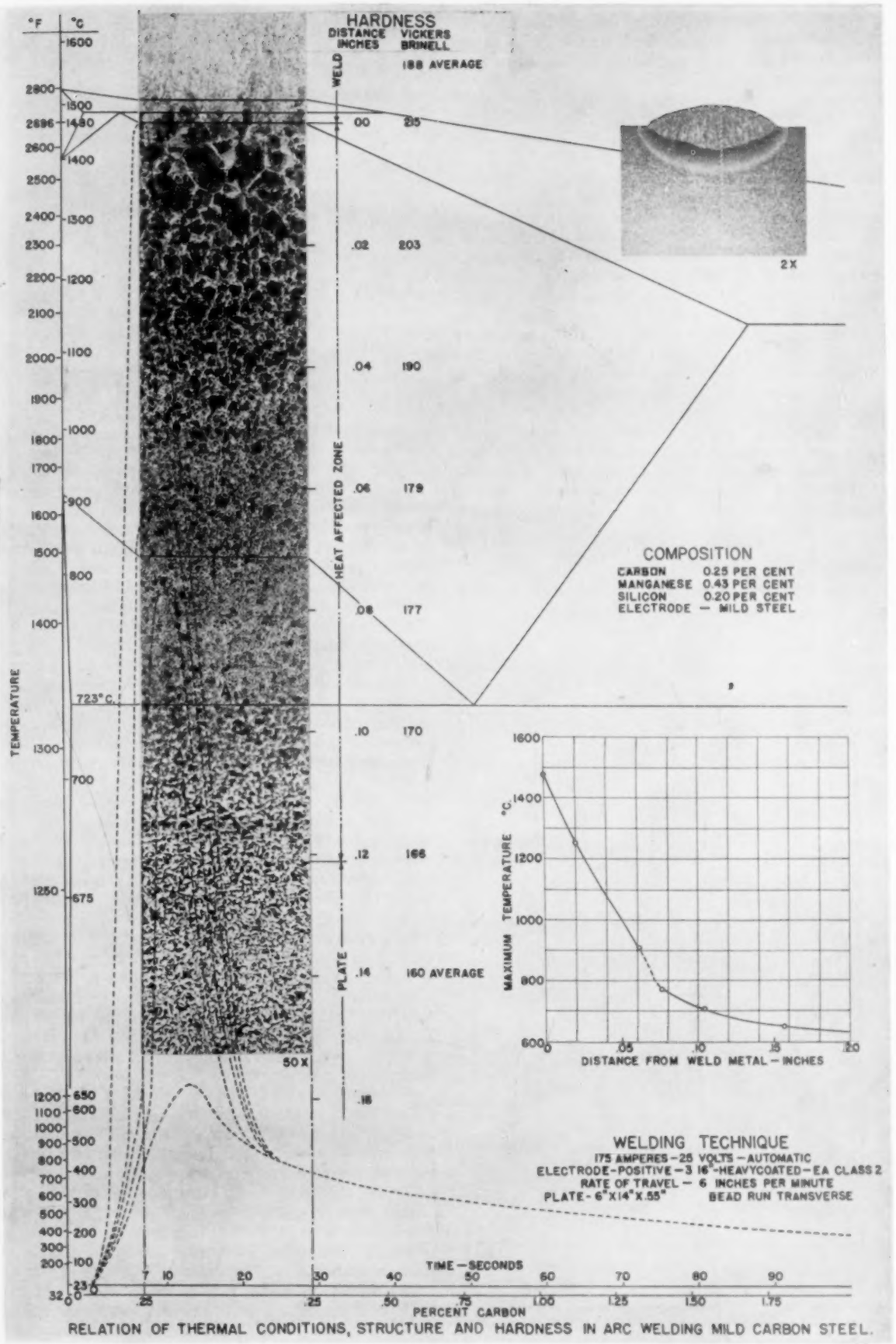
Across the heat-affected zone of the bead weld, as seen in Fig. 5, exists the whole range of heat-treated material. Any welding procedure will necessarily produce conditions comparable to both good and bad heat-treating practice. At the weld-metal parent-metal interface the material has been heated up to the melting point of the parent metal and cooled rapidly or quenched through the austenitic range by the surrounding parent metal. The grain growth in this zone will depend directly upon the maximum temperature attained over the A_3 point. The final structure will be dependent upon the composition of the steel and the rate of cooling.

The very coarse structure just below the weld-metal parent-metal interface is a condition typical of overheated steel. The degree of grain growth is greatest just below the fusion line and becomes less as the maximum temperature decreases until the A_3 transformation point is reached. The normal result of heat treatment as ordinarily practiced is a refined grain size, and the grain growth associated with welding is the result of the steel having been at temperatures well in excess of the A_3 transformation point. In general, the higher



2 X

Fig. 4—Weld Joint and Weld Joint Notched-Bar Specimens



RELATION OF THERMAL CONDITIONS, STRUCTURE AND HARDNESS IN ARC WELDING MILD CARBON STEEL.

Fig. 5

the hardening temperature, the slower is the critical rate of cooling necessary to develop full hardness.

In the area which has reached a maximum temperature below the A_3 and above the A_1, austenite begins to form which in turn is transformed to the ferrite-cementite phase during the cooling cycle.

For temperatures just below the A_1 transformation, the carbide particles tend to diffuse and coalesce into spheroidal particles.

Transformations and changes require a definite amount of time but are hastened by increased temperatures. Lower carbon and alloy contents will tend to permit the changes to take place at shorter intervals of time. The effect of higher carbon and alloy contents is to increase the sluggishness of transformations and to shift the critical rate of cooling to a higher value.

It is to be emphasized that in the heat-affected zone of the parent metal, adjacent to a weld, the maximum temperature attained and the rate of cooling at any point will determine the final structure produced. As we leave the fusion line, the temperature difference between any point and the parent metal becomes less and less, which in turn will reduce the thermal gradients and decrease their metallurgical effects. The use of preheating and multiple bead deposits will affect the structure of the parent metal only in so far as the thermal cycle through which a particular structure passes is affected.

(*b*) *Discussion of Data*

As a general survey of the data presented in Table 1, the values, for example, of the standard width specimens may be compared with the half-width V-notched specimens. At first glance, the correlation would seem to be poor but when one recognizes the variables which affect the notched-bar test, such as shape of bar, size of bar and type of notch, it is surprising that the correlation is as good as it is. It has been pointed out by Hoyt[8] and McAdam and Clyne[9] that a narrow notched test specimen may, at room temperature, have a higher notch toughness than a wider specimen. Notch sensitive material will show a decided decrease in energy absorption per unit area as the specimens increase in width. The curves presented in Fig. 6 are typical curves in this phenomenon.[10] For different steels, the set of curves may be shifted laterally to the right or to the left. The greater the width of a specimen, notched on only one side, the greater is the hindrance to contraction along the notch. Increase in specimen width in any particular steel, like an increase in depth or sharpness of the notch, raises the temperature at which the steep, low temperature drop of the curve occurs. Heat treatment will also shift the notched-bar values. Grain size and degree of uniformity of microstructure will also affect the notched-bar value. Typical effects of heat treatment are shown by the curves given in Fig. 7, which represent data obtained on a carbon steel.[11] Cognizance of these variables, the type of specimen and the heat treatment is essential in interpreting notched-bar values. Notched-bar testing will only be of full value when the variables which affect its results are recognized.

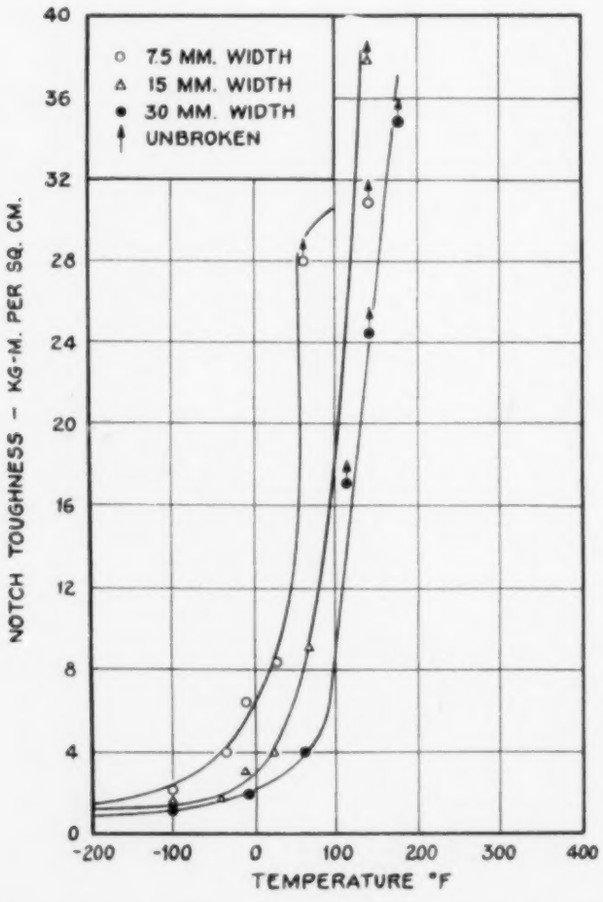


Fig. 6—Relation of the Width of a Notched-Bar Test Specimen to the Temperature of Cold Brittleness. (Maurer and Mailänder)

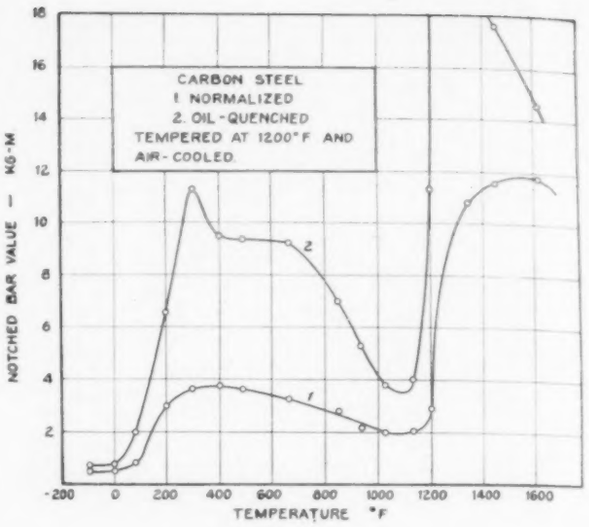


Fig. 7—Effect of Heat Treatment on the Variation of Notched-Bar Value with Temperature. (Greaves and Jones)

Hardenability can only be considered as a rough indication of a steel's weldability because steels of different compositions may, for an equivalent hardness, possess different degrees of toughness or brittleness.[12] However, this approximate criterion of hardness may be used and the steels divided into four groups.[13] Group 1 would include steels in which the average hardness of the heat-affected zone of the parent metal is below 200, Group 2—200 to 250, Group 3—250 to 325 and Group 4—those showing hardnesses above 325. Group 1 includes steels which may be welded without preheating and without necessity of stress-relief annealing after welding. Steels Nos. 1, 2, 5, 6 and 9 are plain carbon steels, hardenability being kept at a low level by reason of the low carbon and manganese content. Notch sensitiveness shown by the high ratio of double width to single

width test values in these steels is low both in the as-rolled as well as the welded condition. Steel No. 20, in spite of low hardenability, shows a decrease in the double width Charpy notched-bar test value compared to the single width. This indicates that this steel is notch sensitive at room temperature and would make the use of this steel questionable. Welding heat treatment does not change this behavior. Comparison of single and double width notched-bar specimens of plate material and of the bead weld type show some beneficial effect of the particular thermal cycle used in these welding tests. Weld joint properties show an improvement. This behavior of Steel No. 20 is not shown by the $^1/_2$ width specimens. In Steel No. 29, it was not possible to obtain a break across the annealed zone in the weld joint specimen, as in each test an irregular breakdown through the fusion line occurred. From this series of tests, the samples of Steels Nos. 27 and 32 indicate the best welding properties. Tests on other samples, and a wider range of tests, will be required before further recommendations can be made.

Group 2 includes those steels which require slight preheating for heavier sections; and stress-relief annealing is advisable for lighter sections and mandatory for heavier sections. Steel Nos. 3, 10, 13 and 14 are plain carbon steels with somewhat higher carbon and manganese than the steels of Group 1. Steel No. 13 with some degree of notch sensitivity at room temperature is not particularly affected by the present welding thermal cycle. Steel No. 18, a carbon 0.50% molybdenum and Steel No. 23, a copper-molybdenum steel show good notched-bar properties in all cases. Steel No. 30, a Mn-Ni-Cu-Mo steel shows exceptional improvement in notched-bar values, in the heat-affected zone of the weld joint specimen and in the bead weld test. In consideration of the notched-bar test values, Steels Nos. 10, 23 and 30 may well be included in Group 1.

Group 3 includes those steels which require preheating to 300° F. or higher, and a stress-relief anneal is necessary for both light and heavy sections. Steel No. 4 is a plain carbon steel which has a coarse grain, indicating a finishing temperature considerably above the A_3 transformation point. The coarse grain enhances the effect of carbon and manganese giving brittle notched-bar results in all cases. No double width test values were obtained. Steel No. 11 is a steel with carbon of 0.29%, and manganese of 1.06%. All notched-bar test values are comparatively high, although the ratio of the double to standard width notched-bar in the plate material is low. Weld heat treatment gives an improved ratio. Steel No. 19, containing 0.79 per cent molybdenum, shows some decrease in the ratio of the double to the standard width bead weld notched-bar, when compared with the ratio for plate material. This indicates an increase in notch sensitiveness of this material when subjected to weld heat treatment. The standard width weld joint annealed zone shows a decided improvement. Steel No. 22, a manganese-vanadium steel, is somewhat more notch sensitive in the bead weld test than as-rolled. Steel No. 35, a carbon, manganese, 2.31 per cent nickel steel, is decidedly influenced in its notch sensitivity by the present welding thermal cycle. Steels Nos. 19 and 22 may well be included in Group 2 classification.

Group 4 includes those steels whose average hardness of heat-affected zone would require preheating to 400°F. or higher and the welded structure not being cooled until stress relieved or annealed. Steel No. 33, a 4 to 6 per cent chromium-molybdenum steel is decidedly sensitive to the present weld thermal cycle, as shown by the results of the bead weld test. The double width specimen of the plate material does not show as high a notched-bar value as the standard width, although both of these values are high. Steel No. 38, a chromium-molybdenum steel with high initial hardness and coarse grain in the as-rolled condition, shows a low notched-bar value which is decidedly improved by the bead weld heat treatment. Despite the high hardness, this steel is a better welding steel than Steel No. 3, for example, and may well be placed with the steels of Group 3 or even Group 2.

SUMMARY AND CONCLUSIONS

1. The use of a single and a double width Charpy V-notched test specimens of a welding steel in as-rolled condition may be used to indicate the degree of notch sensitivity of a welding steel.
2. The use of a single and a double width Charpy V-notched test specimen of a welding steel with the apex of the notch tangent to the heat-affected zone under a bead weld may be used to indicate the degree of notch sensitivity of the welding steel after being subjected to a weld thermal cycle.
3. By comparing the notched-bar value of plate material with that of the bead weld notched-bar, an indication is obtained of the effect of weld heat treatment on the notch sensitivity.
4. The use of the $^1/_2$ width Charpy notched-bar test presents results under only one condition and the data are in no way complete. Also, inherent in this size of specimen is a lowering of the temperature at which the steep low temperature drop of the total work curve occurs; hence, ductile breaks and erratic conclusions are possible.
5. The use of bead weld notched-bar specimens takes into consideration the effect of the annealed zone in those steels which have a brittle annealed zone as well as those steels which have a brittle fusion zone backed up by a less brittle annealed zone.
6. Welding technique may be varied in the preparation of the bead weld specimen in order to determine the critical rate of cooling below which the thermal cycle may produce erratic behavior.
7. Welding technique and conditions, that is, preheat, post heat, plate thickness and welding process, may be varied in preparation of the bead weld notched-bar to suit commercial practice used in any particular welding shop. Standard conditions should be set up for comparison purposes. Some steels will improve decidedly under special welding technique.
8. Data have been presented suggesting the use of single and double width V-notched-bar of the Charpy type in the study of notch toughness of plain carbon and low-alloy steel plate material in the as-rolled and welded condition.

ACKNOWLEDGMENTS

The authors wish to express their appreciation to R. H. Canfield, Superintendent of the Division of Metallurgy, Naval Research Laboratory, whose interest and support in the work has been an inspiration. Acknowledgment is also made to G. A. Ellinger of the Metallurgical Division, National Bureau of Standards, with whom the authors have engaged in many helpful metallurgical discussions. Assistance rendered by the Steel Castings Section, Naval Research Laboratory, is also gratefully acknowledged.

REFERENCES

1. W. G. Theisinger, "The Heat Effect in Welding," *The Iron Age*, **138**, October 1936, pp. 81–82, 86, 88, 89, 92; Dec. 17, 1936, pp. 30–45; **139**, February 25, 1937, pp. 28–31, 115; March 4, 1937, pp. 49–53.
2. W. L. Warner, "Arc Welding of Structural Alloy Steels," AMERICAN WELDING SOCIETY JOURNAL, **15**, October 1936, Supplement pp. 21–32.
3. W. H. Bruckner, "The Use of the Charpy Test as a Method of Evaluating Toughness Adjacent to Welds," American Society for Testing Materials, *Proceedings*, **38**, 1938, Part II, Technical Papers, pp. 71–90.
4. Francis Meunier and David Rosenthal, "Arc Welding of Low-Alloy Steels," *Publications de L'Association des Ingenieurs de l'école des mines de Mons*, **59**, 1936, pp. 587–617.
5. W. Czyrski, *Spawanie i Ciecie Met.*, **10**, 1937, pp. 176–185; *Przeglad Mechaniczny*, **3**, 1937, pp. 658–667.
6. J. H. Critchett, Report of Sub-Committee on Welding of Low-Alloy Steels, AMERICAN WELDING SOCIETY JOURNAL, **17**, January 1938, Supplement pp. 8–14.
7. O. H. Henry, "Impact Tests of Welds," AMERICAN WELDING SOCIETY JOURNAL, **17**, February 1938, pp. 9–10.
8. S. L. Hoyt, "Notched Bar Testing," *Metals and Alloys*, **7**, 1936, pp. 5–7, 39–43, 102–106 and 140–142.
9. D. J. McAdam, Jr., and R. W. Clyne, "The Theory of Impact Testing, Influence of Temperature, Velocity of Deformation, and Form and Size of Specimen on Work of Deformation," American Society for Testing Materials, *Proceedings*, **38**, 1938, Part II, Technical Papers, pp. 112–132.
10. E. Maurer and R. Mailander, "Zur Frage der Blausprodigkeit," *Stahl und Eisen Zeitschrift für das deutsche Eisenhüttenwesen*, **45**, March 1925, pp. 409–423.
11. R. H. Greaves and J. A. Jones, "The Effect of Temperature on the Behavior of Iron and Steel in the Notched-Bar Impact Test," *Journal Iron and Steel Institute*, No. 2, 1925, pp. 123–165.
12. J. C. Hodge and C. R. Sadler, "Weldability and Properties of Materials for Casing String," AMERICAN WELDING SOCIETY JOURNAL, **17**, May 1938, pp. 26–37.
13. A. E. Gibson and J. C. Hodge, "Low Alloy Steels," *Welding Handbook*, **1938**, AMERICAN WELDING SOCIETY, pp. 464–497.

PHOTOELASTIC ANALYSIS of STRESS in the Lap Plates of Fillet-Welded Joints*

By ARSHAG G. SOLAKIAN†

SYNOPSIS

In two previous publications [1,2] a detailed photoelastic analysis was given for the stress distribution over a cross-sectional area of various types of fillet welds, with the results tabulated as to type of fillets and the corresponding stress-concentration factors. The present study considers the stress distribution in the lap plates of a symmetrical fillet-welded joint under a static tensile force. Only three types of standard welded joints are considered in this investigation, namely: end fillet welds, side fillet welds and the combination of the two, so-called box fillet welds.

THE analytical or experimental analysis of stress distribution in welded connections has been of great interest to engineers since the use of welding of multi-plate connections, to replace gradually the universal practice of riveted joints in machines or structures. The analytical solution makes use of the equations of elastic deformation of homogeneous bodies, acted upon by a system of forces under a certain type of distribution. The experimental investigation deals with the results obtained from the prototype or a model, tested by extensometer or photoelastic methods for quantitative results, or by one of the various following methods for qualitative results: brittle coating, rubber or plaster model, fatigue test, etching reagent test, glued paper model, net of cotton threads, rubber diaphragm or membrane analogy, hydraulic analogy, etc.

The main difficulty in any analytical or experimental solution of the problem under consideration consists in the fact that the distribution of the applied force over the edges of the lap plate is not of constant intensity across the thickness of the plate, as demonstrated in one of the previous publications.[1] The regions in the lap plate most affected by this 3-dimensional type of stress are those at the immediate neighborhood of the fillet welds. However, when the thickness of the plate is small as compared with the width, the problem may be considered to be of 2-dimensional type of stress, and hence the results obtained in any solution will be approximately good for practical purposes.

For the present photoelastic study a transparent model made of Marblette was used, consisting of one $^1/_4$-inch thick main plate with a middle splice, and two $^1/_8$-inch lap plates placed symmetrically on either side of the main plate as in Fig. 1. The edges of the lap plates were joined to the surfaces of the main plate through $^1/_8$ in. x $^1/_4$ in. triangular section strips, cemented by means of a special plastic cement developed for this

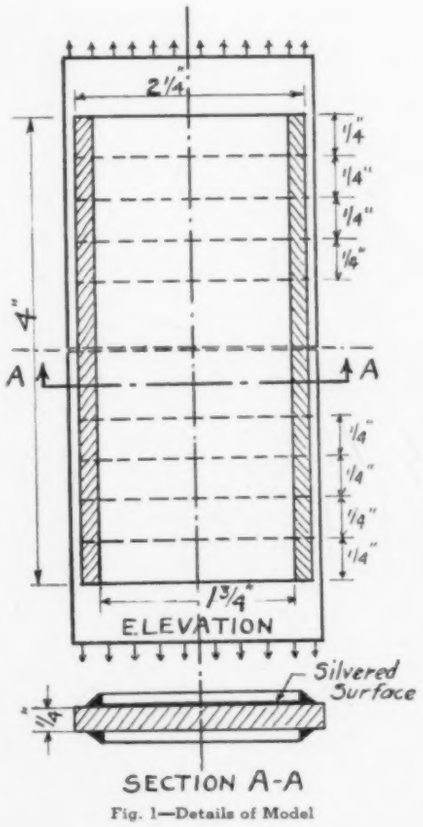


Fig. 1—Details of Model

* Contribution to the Fundamental Research Division, Welding Research Committee.

† Polarized Light and Photoelastic Co., New York, N. Y.

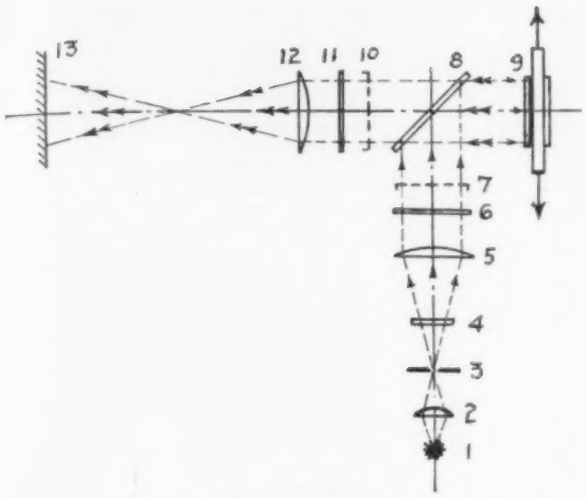


Fig. 2—Reflection Type Polariscope

purpose by the author, which had the useful properties of hardening at room temperature, developing a high bond strength and remaining clear and transparent after setting. The second surface of the first lap plate facing the incoming light rays was silvered to act as a reflecting mirror. In all the three important types of fillet-weld tests, the model used had originally the lap plate and the four fillet strips four inches long for the first test. For the succeeding four tests, these lengths were reduced to 3.5, 3.0, 2.5 and 2.0 inches, respectively, by sawing off $^1/_4$-in. lengths from both ends simultaneously.

The applied load was 330 lb. total in all the tests, applied as tension uniformly distributed at the ends of the main plate. The resulting stress pattern in the lap plate was photographed in reflected plane polarized light in the manner shown in Fig. 2. Under this arrangement of letting the light rays pass through and back in the first lap plate, the stress pattern will not be affected by the stresses in the middle main plate or those in the second lap plate.

Figure 2 is a graphical outline of the Reflection Type Polariscope[3] and the model representing the welded connection. A ray of ordinary white light from its source at (1) after passing through a convenient filter at (3) becomes monochromatic thereafter. After passing through the polarizer at (6), the beam is reflected from the glass plate at (8) toward the model at (9) at normal incidence. The ray upon its passage through the first lap plate is reflected back at the mirrored surface and thus doubles its optical path through the lap plate and re-traverses its path from the lap plate up to the glass plate. It continues in this direction, passes through the analyzer at (11) and finally forms the image of the model on the screen at (13). The two quarter-wave retardation plates (7) and (10) are used in case circular polarized light is desired to eliminate the isoclinics from the pattern of stress fringes. The lenses (2), (5) and (12) are used to direct the path of the ray and focus the image of the model on the screen.

The three unique features in connection with the model were (*a*) use of Marblette as material for the model, (*b*) the new plastic cement for joining the pieces and (*c*) the multiple-plate composition of the model.

Marblette was adopted because of its high stress-optical sensitivity, resulting in a pattern of stress fringes under stresses of relatively low intensity. This fact is of prime importance in the model analysis of welded connections, for the reasons that the cemented areas are of limited dimensions and the applied stresses on these areas cannot be very large to prevent failure of the joints, before a sufficient number of fringes are produced in the stress pattern.

In previous publications on the use of cemented multi-plate models in photoelastic tests,[4] a different kind of plastic cement consisting of liquid Marblette and hydrochloric acid was used, which although insuring a sufficiently strong bond between the cemented areas at room temperature, turned milk-white after setting and destroyed the transparency of the cemented joint, thus preventing the passage of light rays through the affected regions. The new plastic cement besides its extremely strong bond, after setting at room temperature keeps the transparency of the joints intact. Although transparency was not needed in this test, however, the solution of a great many of important stress problems relating to welded or riveted connections, and other multiple-plate models, as well as the study of surface stresses in 3-dimensional problems, will be greatly facilitated by the use of this plastic cement. The use of a cemented multiple-plate model has the double advantages of simplicity of construction of complicated models, and the possibility of obtaining a model initially free of internal strain effects provided the plates used are initially strain-free by annealing. This phenomenon is important from the point of view of the correctness of the results obtained in the test.

I. END FILLET-WELDED JOINTS

The model in Fig. 1 for this test had the fillet-weld strips cemented to the upper and lower extremities of the lap plates. The total load of 330 lb. produces an average stress of about 750 lb. per sq. in. across a transverse section of each of the lap plates. The resulting stress pattern for this type of welded connection, where the lap plates are stressed by constant stress at their extremities, was similar to that of a test-piece of rectangular section in pure tension, namely, the entire field of the lap plates was of same color, thus showing the existence of constant stress intensity all over the plates. Only at and near the corners a slight increase of stress intensity was observed from the existence of higher-order color at these localities, of not sufficient significance (less than $^1/_2$ fringe order) to be photographed, or to be reproduced here. The same was true for the models of this series with smaller length of lap plates. The distribution of the intensity of stress across two typical sections as obtained photoelastically is graphically shown in Fig. 3. It is evident that the assumption of a constant stress distribution for lap plates with end fillet welds is well justified.

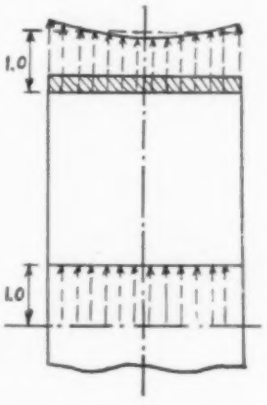


Fig. 3—Stress Distribution in Lap Plate with End Fillet Welds

REFERENCES

1. W. G. Theisinger, "The Heat Effect in Welding," *The Iron Age*, **138**, October 1936, pp. 81–82, 86, 88, 89, 92; Dec. 17, 1936, pp. 30–45; **139**, February 25, 1937, pp. 28–31, 115; March 4, 1937, pp. 49–53.
2. W. L. Warner, "Arc Welding of Structural Alloy Steels," American Welding Society Journal, **15**, October 1936, Supplement pp. 21–32.
3. W. H. Bruckner, "The Use of the Charpy Test as a Method of Evaluating Toughness Adjacent to Welds," American Society for Testing Materials, *Proceedings*, **38**, 1938, Part II, Technical Papers, pp. 71–90.
4. Francis Meunier and David Rosenthal, "Arc Welding of Low-Alloy Steels," *Publications de L'Association des Ingenieurs de l'école des mines de Mons*, **29**, 1936, pp. 587–617.
5. W. Czyrski, *Spawanie i Ciecie Met.*, **10**, 1937, pp. 176–185; *Przeglad Mechanicsny*, **3**, 1937, pp. 658–667.
6. J. H. Critchett, Report of Sub-Committee on Welding of Low-Alloy Steels, American Welding Society Journal, **17**, January 1938, Supplement pp. 8–14.
7. O. H. Henry, "Impact Tests of Welds," American Welding Society Journal, **17**, February 1938, pp. 9–10.
8. S. L. Hoyt, "Notched Bar Testing," *Metals and Alloys*, **7**, 1936, pp. 5–7, 39–43, 102–106 and 140–142.
9. D. J. McAdam, Jr., and R. W. Clyne, "The Theory of Impact Testing, Influence of Temperature, Velocity of Deformation, and Form and Size of Specimen on Work of Deformation," American Society for Testing Materials, *Proceedings*, **38**, 1938, Part II, Technical Papers, pp. 112–132.
10. E. Maurer and R. Mailander, "Zur Frage der Blausprodigkeit," *Stahl und Eisen Zeitschrift für das deutsche Eisenhüttenwesen*, **45**, March 1925, pp. 409–423.
11. R. H. Greaves and J. A. Jones, "The Effect of Temperature on the Behavior of Iron and Steel in the Notched-Bar Impact Test," *Journal Iron and Steel Institute*, No. 2, 1925, pp. 123–165.
12. J. C. Hodge and C. R. Sadler, "Weldability and Properties of Materials for Casing String," American Welding Society Journal, **17**, May 1938, pp. 26–37.
13. A. E. Gibson and J. C. Hodge, "Low Alloy Steels," *Welding Handbook*, **1938**, American Welding Society, pp. 464–497.

PHOTOELASTIC ANALYSIS of STRESS in the Lap Plates of Fillet-Welded Joints*

By ARSHAG G. SOLAKIAN†

SYNOPSIS

In two previous publications [1,2] a detailed photoelastic analysis was given for the stress distribution over a cross-sectional area of various types of fillet welds, with the results tabulated as to type of fillets and the corresponding stress-concentration factors. The present study considers the stress distribution in the lap plates of a symmetrical fillet-welded joint under a static tensile force. Only three types of standard welded joints are considered in this investigation, namely: end fillet welds, side fillet welds and the combination of the two, so-called box fillet welds.

THE analytical or experimental analysis of stress distribution in welded connections has been of great interest to engineers since the use of welding of multi-plate connections, to replace gradually the universal practice of riveted joints in machines or structures. The analytical solution makes use of the equations of elastic deformation of homogeneous bodies, acted upon by a system of forces under a certain type of distribution. The experimental investigation deals with the results obtained from the prototype or a model, tested by extensometer or photoelastic methods for quantitative results, or by one of the various following methods for qualitative results: brittle coating, rubber or plaster model, fatigue test, etching reagent test, glued paper model, net of cotton threads, rubber diaphragm or membrane analogy, hydraulic analogy, etc.

The main difficulty in any analytical or experimental solution of the problem under consideration consists in the fact that the distribution of the applied force over the edges of the lap plate is not of constant intensity across the thickness of the plate, as demonstrated in one of the previous publications.[1] The regions in the lap plate most affected by this 3-dimensional type of stress are those at the immediate neighborhood of the fillet welds. However, when the thickness of the plate is small as compared with the width, the problem may be considered to be of 2-dimensional type of stress, and hence the results obtained in any solution will be approximately good for practical purposes.

For the present photoelastic study a transparent model made of Marblette was used, consisting of one $^1/_4$-inch thick main plate with a middle splice, and two $^1/_8$-inch lap plates placed symmetrically on either side of the main plate as in Fig. 1. The edges of the lap plates were joined to the surfaces of the main plate through $^1/_8$ in. x $^1/_4$ in. triangular section strips, cemented by means of a special plastic cement developed for this

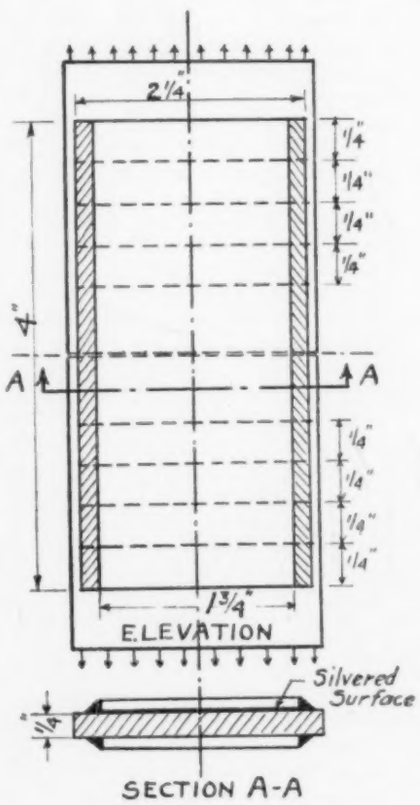


Fig. 1—Details of Model

* Contribution to the Fundamental Research Division, Welding Research Committee.

† Polarized Light and Photoelastic Co., New York, N. Y.

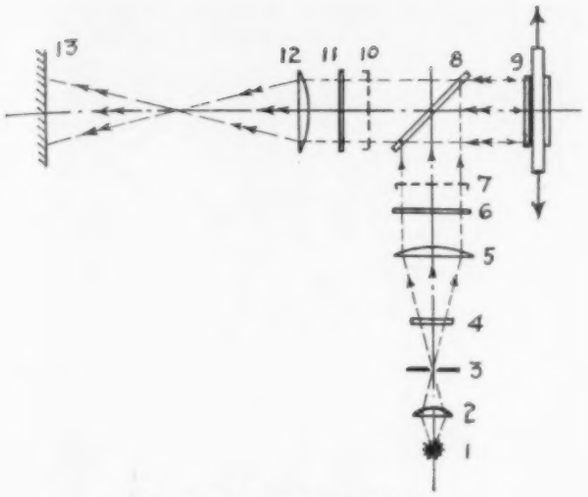


Fig. 2—Reflection Type Polariscope

purpose by the author, which had the useful properties of hardening at room temperature, developing a high bond strength and remaining clear and transparent after setting. The second surface of the first lap plate facing the incoming light rays was silvered to act as a reflecting mirror. In all the three important types of fillet-weld tests, the model used had originally the lap plate and the four fillet strips four inches long for the first test. For the succeeding four tests, these lengths were reduced to 3.5, 3.0, 2.5 and 2.0 inches, respectively, by sawing off $^1/_4$-in. lengths from both ends simultaneously.

The applied load was 330 lb. total in all the tests, applied as tension uniformly distributed at the ends of the main plate. The resulting stress pattern in the lap plate was photographed in reflected plane polarized light in the manner shown in Fig. 2. Under this arrangement of letting the light rays pass through and back in the first lap plate, the stress pattern will not be affected by the stresses in the middle main plate or those in the second lap plate.

Figure 2 is a graphical outline of the Reflection Type Polariscope[3] and the model representing the welded connection. A ray of ordinary white light from its source at (1) after passing through a convenient filter at (3) becomes monochromatic thereafter. After passing through the polarizer at (6), the beam is reflected from the glass plate at (8) toward the model at (9) at normal incidence. The ray upon its passage through the first lap plate is reflected back at the mirrored surface and thus doubles its optical path through the lap plate and re-traverses its path from the lap plate up to the glass plate. It continues in this direction, passes through the analyzer at (11) and finally forms the image of the model on the screen at (13). The two quarter-wave retardation plates (7) and (10) are used in case circular polarized light is desired to eliminate the isoclinics from the pattern of stress fringes. The lenses (2), (5) and (12) are used to direct the path of the ray and focus the image of the model on the screen.

The three unique features in connection with the model were (*a*) use of Marblette as material for the model, (*b*) the new plastic cement for joining the pieces and (*c*) the multiple-plate composition of the model.

Marblette was adopted because of its high stress-optical sensitivity, resulting in a pattern of stress fringes under stresses of relatively low intensity. This fact is of prime importance in the model analysis of welded connections, for the reasons that the cemented areas are of limited dimensions and the applied stresses on these areas cannot be very large to prevent failure of the joints, before a sufficient number of fringes are produced in the stress pattern.

In previous publications on the use of cemented multi-plate models in photoelastic tests,[4] a different kind of plastic cement consisting of liquid Marblette and hydrochloric acid was used, which although insuring a sufficiently strong bond between the cemented areas at room temperature, turned milk-white after setting and destroyed the transparency of the cemented joint, thus preventing the passage of light rays through the affected regions. The new plastic cement besides its extremely strong bond, after setting at room temperature keeps the transparency of the joints intact. Although transparency was not needed in this test, however, the solution of a great many of important stress problems relating to welded or riveted connections, and other multiple-plate models, as well as the study of surface stresses in 3-dimensional problems, will be greatly facilitated by the use of this plastic cement. The use of a cemented multiple-plate model has the double advantages of simplicity of construction of complicated models, and the possibility of obtaining a model initially free of internal strain effects provided the plates used are initially strain-free by annealing. This phenomenon is important from the point of view of the correctness of the results obtained in the test.

I. END FILLET-WELDED JOINTS

The model in Fig. 1 for this test had the fillet-weld strips cemented to the upper and lower extremities of the lap plates. The total load of 330 lb. produces an average stress of about 750 lb. per sq. in. across a transverse section of each of the lap plates. The resulting stress pattern for this type of welded connection, where the lap plates are stressed by constant stress at their extremities, was similar to that of a test-piece of rectangular section in pure tension, namely, the entire field of the lap plates was of same color, thus showing the existence of constant stress intensity all over the plates. Only at and near the corners a slight increase of stress intensity was observed from the existence of higher-order color at these localities, of not sufficient significance (less than $^1/_2$ fringe order) to be photographed, or to be reproduced here. The same was true for the models of this series with smaller length of lap plates. The distribution of the intensity of stress across two typical sections as obtained photoelastically is graphically shown in Fig. 3. It is evident that the assumption of a constant stress distribution for lap plates with end fillet welds is well justified.

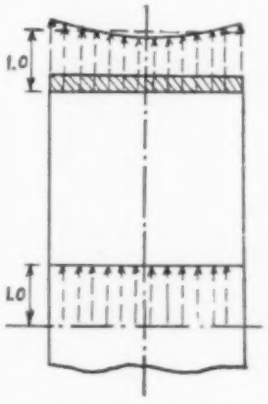


Fig. 3—Stress Distribution in Lap Plate with End Fillet Welds

II. SIDE FILLET-WELDED JOINTS

In this type of models the fillet-weld strips are cemented to the side edges of the lap plates, as in Fig. 1. Five different lengths of lap plates (keeping the width constant at 1.75 inches) were tested, namely: 4.0, 3.5, 3.0, 2.5 and 2.0 inches, giving the ratios l/w of length of plate to its width of 2.47, 2.16, 1.85, 1.54 and 1.23. Considering the length of the fillet weld on either side of the middle line of the lap plate as transmitting the stress from the main plate to the lap plate in shear, the above ratios are 1.23, 1.08, 0.93, 0.77 and 0.61, respectively. The same model was used for all the tests after being modified by sawing off the lengths of lap plates and fillet strips, as mentioned above.

Corresponding to the total applied tensile load of 330 lb., the resulting computed average shear stress produced at each of the two edges of the five different lap plates are as follows: 330, 380, 440, 530 and 660 lb. per sq. in. of cemented area, respectively, assumed to be uniformly distributed.

The fringe stress patterns for the three models with ratios of l/w of 1.23, 0.93 and 0.61 (and acted upon at their edges of an average shear of 330, 440 and 660 lb. per sq. in.) are given in Figs. 4, 5 and 6. Photographs for the two intermediate models with ratios of l/w of 1.08 and 0.77 being similar in nature and intermediate to the ones given, are not reproduced here.

From Figs. 4, 5 and 6, the distribution of stress across four typical sections for the three models is obtained, and is graphically represented in Fig. 7. In addition, the stress curves for the two intermediate cases are also shown partially in Fig. 7. The applied load of 330 lb. total produced in each lap plate of 0.125 in. x 1.75 in. area an average tensile stress (at the free midsection) of 750 lb. per sq. in. For comparison purposes, the stress curves are drawn with ordinates representing stress intensities to correspond to an applied force of one lb. per sq. in. instead of 750 lb. per sq. in.

From Fig. 7, the following results are important to observe; for the lap plate:

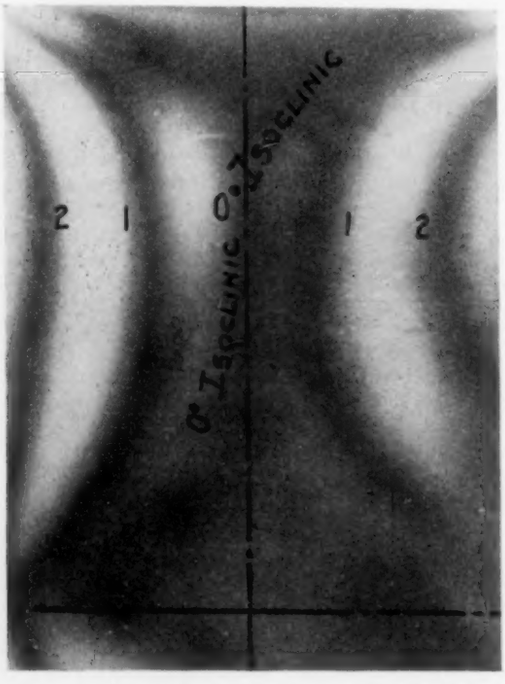


Fig. 4—Stress Fringe Pattern for Lap Plate with Side Fillet Welds (l/w = 1.23)

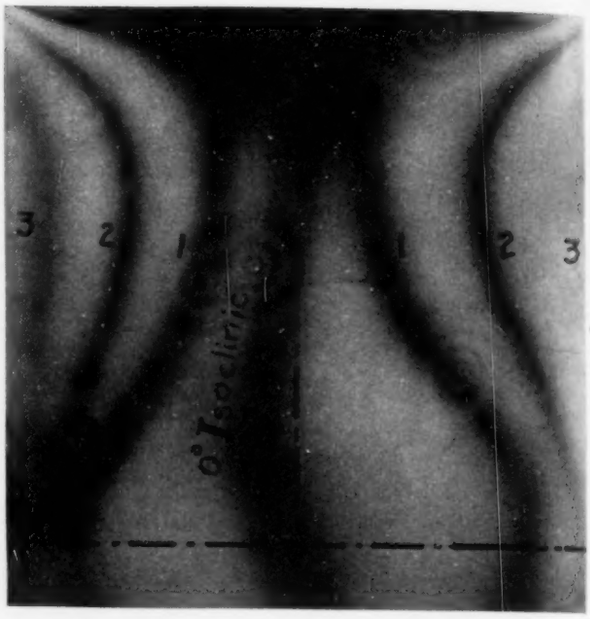


Fig. 5—Stress Fringe Pattern for Lap Plate with Side Fillet Welds (l/w = 0.93)

A. Midsection.—(1) The stress concentration factors (ratio of maximum stress to applied stress) for the five different length lap plates are 1.3, 2.0, 2.5, 2.8 and 3.1, respectively. (2) The stress concentration factor increases with the decrease in length of the fillet-weld strips. (3) The stress distribution curve approaches that of a constant stress intensity line, with the increase in length of the fillet-weld strips.

B. Intermediate Section.—(1) The stress concentration factors for this section are 2.5, 3.0, 3.3, 3.7 and 4.0 for the various lap plates. (2) As above, this factor decreases with increase in length of fillet-weld strip. (3) The intensity of stress is largest near the edge regions of the lap plate, gradually approaching zero at the middle of the section.

C. End Section.—(1) The stress concentration factors for this section are 0.8, 1.1, 1.3, 1.8 and 2.3 for the various lap plates. (2) As above, this factor decreases with increase in length of the fillet-weld strip. (3) As in (*A*), the stress distribution curve approaches that of a constant stress intensity line, with the increase in length of the fillet-weld strips.

D. Sections A, B and C.—From a comparison of the areas under the stress curves, it appears that of these three sections, the intermediate section is the one carrying the greatest percentage of the total applied load, with the balance being taken by the main plate. The reverse is true for the two sections, *A* and *C*.

E.—The distribution of stress along the sides of the lap plates is of different type from that across the width. Here the maximum intensity occurs at a point slightly above the intermediate section, *B* (toward the end of the lap plate), the intensities of stress at midsection *A* and end section *C* being nearly the same.

F. The numerical values of stress (concentration factors) for the three typical sections are given in Table 1 and Fig. 8, for various ratios of l/w.

The above results are for lap plates of constant width, with the lengths of the welded parts varying. Similar results can be obtained from a series of tests where the reverse relation may be used in proportioning the dimensions of the model, and will be of interest for a complete knowledge of the problem under consideration.

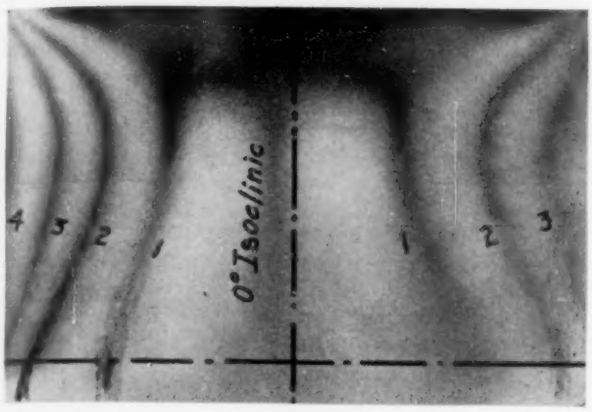


Fig. 6—Stress Fringe Pattern for Lap Plate with Side Fillet Welds (l/w = 0.61)

III. BOX FILLET-WELDED JOINTS

This type of joint is obtained by the combination of end and side fillet welds. The stress picture for the same total applied load of 330 lb. is as in Fig. 9. The distribution of stress across the various sections is practically of constant stress type, except the midsection (A), where the intensity of stress is greater at the cemented edges of the lap plate to the fillet-weld strips, for a very small length near the midsection. It is evident from the uniform stress intensity of small magnitude all over the lap plate that this type of welded connection is comparatively of greater strength than any of the other two previous types. This result confirms in general the mathematical deduction by K. Ježek (*S.-B. Akad. Wiss. Wien, Abt. IIa*, **146,** 543–546 (1937)) that box fillet-welded joints have more favorable stress distribution than side fillets alone. Inversely with the end fillet-weld type of joint, the stress distribution curve is of constant intensity for the end section, and of slightly concave curved variation for the midsection.

Table 1—Stress Concentration Factors for Lap Plates with Side Fillet-Welded Joints

Ratio l/w = Length of Weld / Width of Plate	Stress Concentration Factors: Mid-section	Inter-mediate Section	End Section	Reference (Fig. 7)
1.23	1.3	2.5	0.9	Curve I
1.08	2.0	3.0	1.1	Curve II
0.93	2.5	3.3	1.3	Curve III
0.77	2.8	3.7	1.8	Curve IV
0.61	3.1	4.0	2.3	Curve V

ACKNOWLEDGMENT

The present photoelastic investigation was made possible by a special research grant from the Fundamental Research Division of the Engineering Foundation—Welding Research Committee, for which the author is much indebted. Also, special appreciation is felt of the hearty cooperation of Professor L. Perez, of Cooper Union Institute in the conduct and preparation of this test.

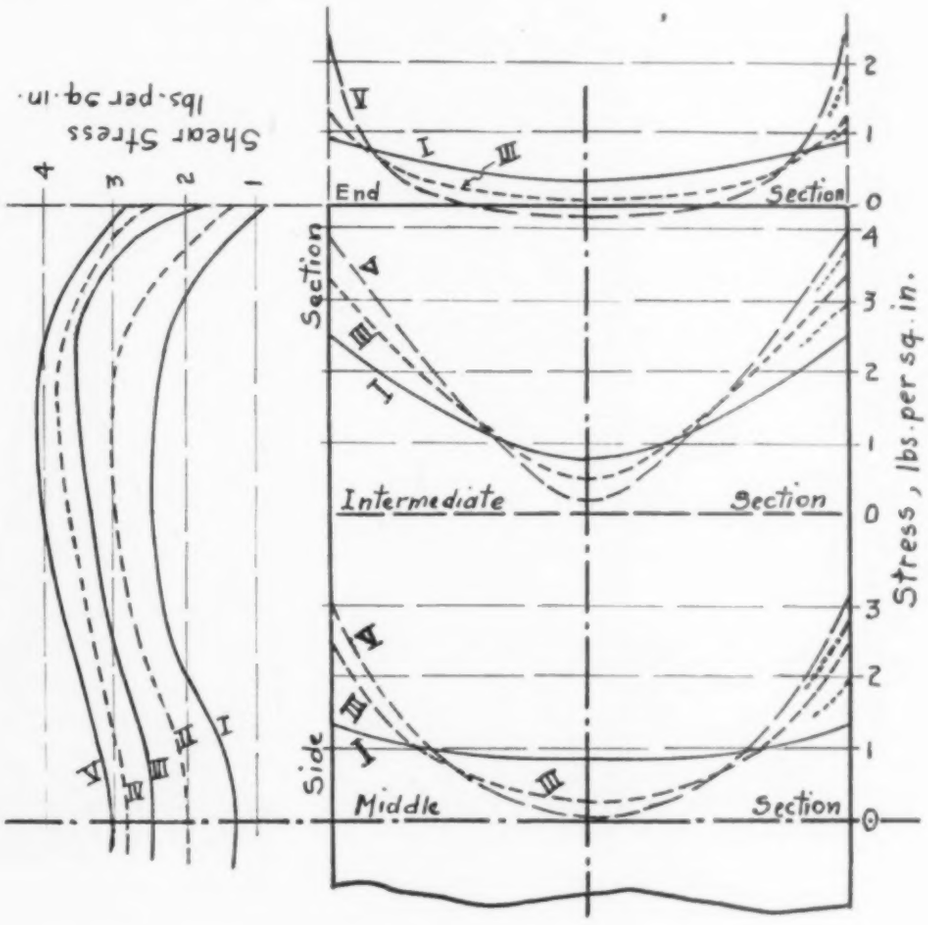


Fig. 7—Stress Distribution Curves for Lap Plates with Side Weld-Fillets (l/w = 1.23, 1.08, 0.93, 0.77 and 0.61)

Note: For comparison purposes, the lengths of the lap plates for curves I, II, III, IV and V are reduced hypothetically to the same dimension for the side section.

REFERENCES

1. Solakian, A., "Stresses in Transverse Fillet-Welds by Photoelastic Methods," AMERICAN WELDING SOCIETY JOURNAL, February 1934.
2. Solakian, A., "Effect of Weld Penetration on Stresses in Fillet-Welded Joints," AMERICAN WELDING SOCIETY JOURNAL, September 1936.
3. Manufactured by the Polarized Light and Photoelastic Company, 55 West 42nd Street, New York, N. Y.
4. Solakian, A., "Photoelastic Models with Cemented Elements," *Photoelastic Journal*, January 1938.

APPENDIX

The photoelastic results described in the foregoing report represent but one of numerous recent efforts to elucidate the stress distribution in fillet-welded joints. The following paragraphs summarize some recent published research.

A review of literature has been made by L. F. Denaro ("Welding of Steel Structures," Dept. Sci. & Ind. Research, London, 1938, 215–240). D. Rosenthal and P. Levray (AMERICAN WELDING SOCIETY JOURNAL, **18** (4) Suppl., 140–149. A good general discussion of the subject of stress distribution in fillet welds is given by D. Rosenthal, *Bull. Soc. Ing. Soudeurs*, **8** (46), 2793–2810 (1937)), made extensometer measurements on full size arc-welded joints as well as observations on rubber models of side fillets. When the ratio l/a (l = length, a = throat of weld) is less than 20, the stress concentration at the ends does not exceed 15%. When l/a exceeds 20, the stress concentration may reach 50%, and the static fracture load is decreased up to 25%. The rubber models showed that the interior fibers are displaced to the same extent as the corresponding exterior fibers, except in the immediate vicinity of the welds for a distance nearly equal to the thickness of the plate (equal to the distance between the lines drawn on the surface of the plate). Over this width the interior fibers are displaced less than the exterior, the difference becoming less as the top of the weld is approached.

Plastic behavior of fillet welds has been studied by E. P. S. Gardner (*Quarterly Trans. Institute of Welding*, **2** (1), 45–57 (Jan. 1939)). End, side and box fillet welds were tested. Breaking tests and extensometer measurements (stress-strain curves) were made. Motion pictures were made (24 frames per second) to show the nature of the deformation of the welds during the breaking tests. The initial source of failure of end fillets was the root of the fillet. Three stages were distinguished in the deformation of side fillets under load. The first stage is plastic yield. The second stage consists in the formation of a crack at the root at one end of the weld. In the third stage the crack spreads to the surface and along the weld. There appeared to be little difference in the plastic movements of end and side fillets if the movements are determined relatively up to a load at which the initial crack commences. The yield stresses of end and side fillets were 82 and 77% of the maximum stresses at fracture and the maximum extensions were approximately 0.058 and 0.151 inch, respectively. Within the limits of the tests the strength of end and side fillets did not vary with their size.

The plastic behavior of end, side and box fillets also has been studied by M. Cymboliste and H. Gerbeaux (*Proc. Twelfth Intnl. Cong. Acetylene*, London, 387–430 (1936); see also *Le Remplacement des Rivets par la Soudure dans la Charpente Métallique*, by H. Gerbeaux, published by Institut de Soudure Autogène, Paris, 17–18 (1937)). These investigators coated annealed welds in mild steel with a brittle varnish that cracked only after plastic strain. With end fillets the elastic limit was reached when the stress at the throat was 0.55–0.70 of the usual elastic limit stress. Whereas fatigue failure starts at the toe of an end fillet the brittle varnish cracked first at the root. The elastic limit of side fillet welds, as determined by the appearance of the first crack in the varnish, was always a little higher in long, than in short, seams. Plastic strain originated at the middle of side fillets, contrary to usual experience, because there were said to be transverse stresses caused by eccentricity of load. In box joints the end fillet is

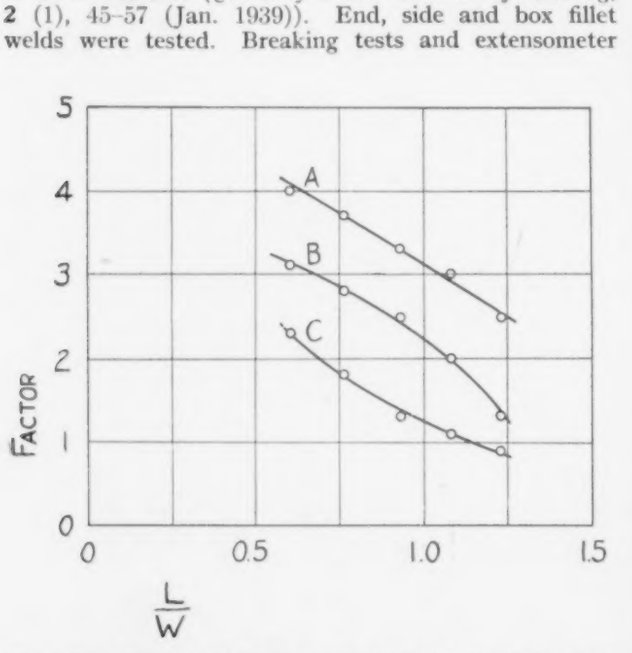


Fig. 8—Stress Concentration Factors at the Edge of the Lap Plates in Side Fillet Welds at Three Sections

A—intermediate
B—middle
C—end
L = length of weld
W = width of plate

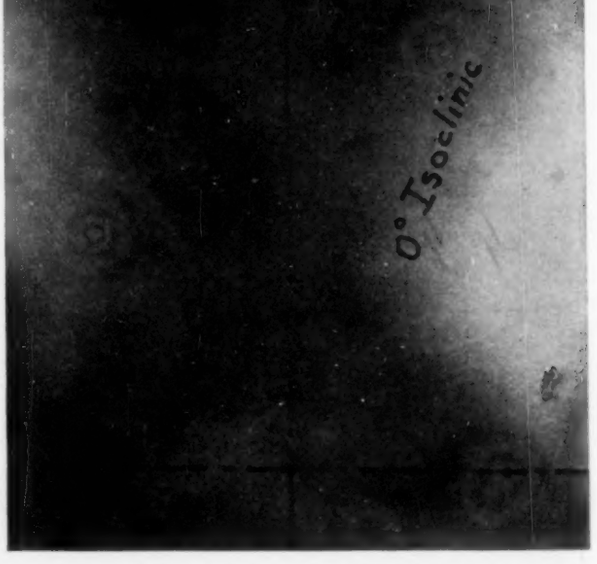


Fig. 9—Stress Fringe Pattern for Lap Plate with Box Fillet Welds (l/w = 1.23)

the last to deform, yet the first to break. In end fillets of different angularity the throat section is the first to deform plastically.

The plastic behavior of an annealed end fillet weld between two bars of mild steel, $^1/_2$ x $^{13}/_{16}$ inch each, was studied by C. W. MacGregor (*Metals Technology*, T. P. 1036, pp. 7–10 (April 1939)) by means of his plastiscope. Contrary to Gerbeaux, yielding commenced at the toe of the fillet. Then a flow layer appeared through the throat of the weld. After this the base metal began to yield, and final failure took place through the throat of the weld parallel to the flow layer. R. H. Kinkead's rubber models of fillet welds were brought to the fore once again by E. W. P. Smith (*Product Engineering*, **9**, 400–401 (1938)). A photoelastic investigation by T. U. Matthew (*Jnl. Royal Tech. College, Glasgow*, **4** (1), 121–134 (1937)) of the stress distribution in fillet-welded plates at right angles demonstrated the importance of removing stress raisers.

R. S. Hale ("Welded Steel Construction," Pitman Publ. Corp., N. Y., 113–118 (1938)) has prepared tables for designing side fillet welds in angle, channel and T connections. T. H. Rust (*Proc. Amer. Soc. Civil Engrs.*, **64** (9), 1829–1845 (Nov. 1938); discussion Feb. 1939, 378–380; April 1939, 695–696 describes a device for representing the stress patterns in gusset plate joints, riveted or welded. The effect of eccentricity on the strength of fillet-welded joints has been determined by breaking-load tests by W. W. Stecker (AMERICAN WELDING SOCIETY JOURNAL, **17** (11) Suppl., 8–11 (1938)). The stress distribution in fillet welds subjected to bending has been investigated experimentally by C. D. Jensen and R. E. Crispen (AMERICAN WELDING SOCIETY JOURNAL, **17** (10) Suppl., 22–24 (1938)). For repeated loads the authors recommend discarding the straight-line concept of stress distribution, although no fatigue tests were made. Static tests by a firm in Holland (*Smit Laschtijdschrift*, **2** (4), 134–145 (1938)) showed that as the throat size increased from 0.06–0.39 inch the breaking stress on end fillet welds decreased from 71,000 to 46,000 lb./in.^{2} In side fillets the breaking stress increased from 47,000 to 57,000 lb./in.^{2} as the throat was increased to 0.24 inch, then decreased to 45,000 lb./in.^{2} at 0.39 inch. Extensometer measurements of stress distribution in the plates adjacent to side fillet welds revealed the usual stress concentrations at the ends of the welds, whose presence was confirmed by fatigue tests. Pulsating tension fatigue tests on end fillet welds also have been reported by G. A. Hankins and P. L. Thorpe ("Welding of Steel Structures," Dept. Sci. & Ind. Research, London, 1938, 179–180).

Several mathematical discussions of stresses in fillet welds have appeared. T. Naka (*Journal Japanese Welding Society*, **8**, 67–77 (1938)) derived expressions for the stress distribution in continuous and intermittent side fillet welds in the elastic region and at fracture using cosh formulas and difference equations. The derived expressions fitted experimental results on the breaking load as a function of length better than linear relationships. In another Japanese work the distribution of stress over the cross section of an end fillet weld is derived according to a method similar to that used by Nikolaieff. One or two of the cases considered by H. Bay (*Bauingenieur*, **19**, 349–356 (1938)) in his analysis of the stress distribution in triangular and rectangular plates by means of difference equations may be found to have some application to end fillet welds, particularly his case *b*.

Discussion (*Jnl. Instn. Engrs. Australia*, **9**, 229–232, 443–444 (1937)) of a paper by D. V. Isaacs mentioned in a review of literature (AMERICAN WELDING SOCIETY JOURNAL, **16** (5) Suppl., May 1937) brought to light details of the methods used by Isaacs. W. H. H. Gibson and C. F. Koerner supplied conditions which must be fulfilled before membrane analogy methods can be applied to side fillet welds. It was emphasized that a side fillet distorts to the greatest extent at the ends.

K. Ježek (*Bauingenieur*, **19**, 228–236 (1938)) has continued his mathematical analysis of stress distribution in side fillet welds. Expressions are worked out and evaluated in graphical form for single lap and double-lap side fillets with plates of the same or different thickness. He points out that previous attempts to solve the problem have been based on the assumption that the stress system is uni-axial, that is, that the plates are thin and that the distance between welds is small compared with the length of the weld. Ježek assumes a system of plane stress, thus covering the joints in which the breadth of the plates (distance between welds) is comparable with the length of the weld, the plates being thin and the stress being assumed uniform across the thickness. The analysis is based on Melan and Girkmann, and is in better agreement with the experimental results of Hollister and Gelman and Gaber than previous analyses. Ježek points out the absence of information on the deformation of the plates of a side fillet joint perpendicular to the welds. The calculations show that the maximum stress at the ends of the welds is generally about twice the average stress, and provide an approximate means for determining the fatigue strength of side fillets, which agrees with Graf's experimental results.

"FIRECRACKER" WELDING*

By GEORG HAFERGUT

SINCE the customary automatic arc welder is high in first cost and requires fairly expert attention, the simple firecracker method of automatic arc welding has been brought once again to the fore. The firecracker method was described in a patent in 1912 in the following terms: "An electrode covered with a suitable fusible insulating covering is laid along the whole length of the joint, the insulation being of such a thickness that the electrode is held at the right distance from the work. One end of the electrode is exposed. The arc is struck between electrode and work and slowly travels along the joints. The insulation, formed of asbestos smeared with sodium silicate, aluminum silicate, or the like, fuses and forms a flux." Similar processes have been described by S. Akazaki, in *Zosenkiokai*, **57**, 15–32 (1935), and by Russian investigators in *Avtog. Delo*, **10** (4), 5–8 (1939).

Unless special precautions are observed, the firecracker process is difficult to apply. The electrode may twist

* Abstract of "Ein neues automatisches Lichtbogenschweissverfahren besonderer Einfachheit," published in *Elektroschweissung*, **10** (7), 126-128 (1939).

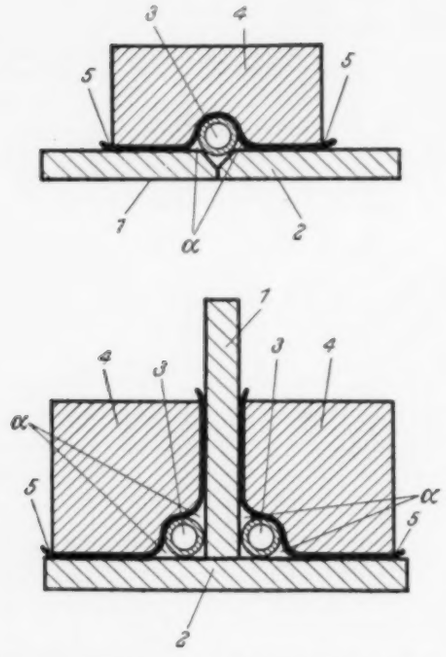


Fig. 1—Arrangements for the Elin-Hafergut Method. Upper Diagram for Butt Welds. Lower Diagram for Fillet Welds

1 and 2, materials to be welded; 3, covered electrode; 4, copper bar to cover the electrode; 5, sheet of paper inserted between electrode and copper bar; α, space for slag

out of the scarf owing to the heat of the arc and the resistance of the electrode. The twisting of the electrode may lengthen and extinguish the arc. Inadequate current quenches the arc or provides inadequate penetration. Too large a proportion of the electrode spatters away if the current is too high.

All disadvantages disappear if a suitably shaped bar of copper, Fig. 1, is placed over the electrode. A strip of any sort of paper is inserted between copper and the base metal and electrode. Spatter is prevented and the short arc, determined by the thickness of the coating, yields the best penetration. The electrode melts in the small space allowed by the bar, combustion of the paper removing oxygen from the welding atmosphere. The welds are more uniform than those produced by manual or other automatic processes and have satisfactory mechanical properties.

In applying this patented process a space, α in Fig. 1, must be provided for the slag, otherwise the arc may be quenched or the weld may be full of slag inclusions. Electrodes 5 feet long have been used, and longer electrodes may be used if necessary. Extruded covered electrodes of a wide range of compositions have been deposited using the same polarity and current as for manual welding. Excellent welds have been made with electrodes 0.08–0.47 inch diameter.

One unskilled operator can manage "firecrackers" at four locations simultaneously. He draws an arc at one electrode with a metal or carbon rod. While the electrode is being deposited (3 to 5 minutes for an electrode 5 feet long, depending on diameter), he sets other electrodes in position and draws the arc. Only 10% of the welding time is occupied in water cooling the copper bars and in placing them over other electrodes.

CONTROL OF FILLET WELDS*

By E. P. S. GARDNER

SUMMARY

IN THE first place it is contended that the hidden faults that may occur in single bead, downhand fillet welds, and that may be considered attributable to incorrect manipulation of the electrode by the operator, are lack of root penetration and fusion. Lack of fusion was not found to have occurred in any of the welds tested, numbering over a thousand, and it is not regarded as a serious possibility, at least within the safe limits of procedure determined.

The main procedure tests were conducted on specimens prepared with one type of heavy covered, mild steel electrode 0.13, 0.16 and 0.19 inch diameter using alternating current to determine the influence on root penetration of variation in the size of fillet applied, current value, and plate thickness. Check tests were conducted using direct current, larger diameters of electrodes and six alternative types. Mechanical tests were conducted on end and side fillet welds, with various degrees of root penetration, in order to determine the influence of the latter on their strength.

The Tee specimens for the procedure tests consisted of two plates at right angles. Each specimen was sawn into four sections, thus providing four cut profiles of each weld which were polished and etched. A magnifying glass with a fixed millimeter scale was used for measuring the leg lengths, throat thickness and degree of root penetration of each profile. Subsequent to measuring the degree of root penetration on the cut sections, each length of weld was notched with a longitudinal saw cut through the throat and broken open in order to determine the average degree of root penetration along each length.

The specimens for the mechanical tests consisted of two plates joined by two cover plates, one on each side. The plates were mild steel, tensile strength = 62,500–71,500 lb./in.^{2}, and were so proportioned that their maximum average stress when failure occurred in the test welds was less than the yield stress of the material. The strength tests were conducted on fillet welds deposited with various degrees of root penetration and ranging in size from $^1/_8$ in. to $^3/_8$ in. leg length. The larger sizes were multi-run.

It will be appreciated that an extended and comprehensive investigation of all combinations of variables would require much time and entail considerable expense, and that even if conducted any conclusions drawn from them must, as in the present investigation, be subject to confirmation by further check tests on specimens prepared under practical yard and workshop conditions.

* Summary of the first interim report of the Sub-Panel on Arc Welding Procedure of the Institute of Welding (London), *Quarterly Trans.*, 3 (2) 69–95 (1939).

Nevertheless, it is suggested that the evidence provided by the present investigations and confirmed by check tests on specimens prepared under normal "workshop" and "yard" conditions by an engineering works, one railway company and three shipyards, is sufficient to warrant the following tentative conclusions.

1. The degree of root penetration in single bead fillet welds will not be less than −1.0 mm. (x and y = −0.039 inch), Fig. 1, provided that the following limitations and procedure are adhered to:

(*a*) The size (leg length) of a single run fillet weld deposited by a:

0.13-inch electrode should not be greater than $^3/_{16}$ in.
0.16-inch electrode should not be greater than $^1/_4$ in.
0.19-inch electrode should not be greater than $^1/_4$ in.
†0.225-inch electrode should not be greater than $^5/_{16}$ in.
$^1/_4$-inch electrode should not be greater than $^5/_{16}$ in.
0.28-inch electrode should not be greater than $^5/_{16}$ in.
$^5/_{16}$-inch electrode should not be greater than $^3/_8$ in.
0.31-inch electrode should not be greater than $^3/_8$ in.
$^3/_8$-inch electrode should not be greater than $^1/_2$ in.

(*b*) The current employed should be within the medium of high values in the range recommended by the manufacturer for the diameter of electrode in question.

2. The strength, i.e., ultimate gross throat stress computed on the throat measured along the diagonal from surface of weld to intersection of the plates, of fillet welds will not be less than the minimum values required by British Standard Specification No. 538 for Metal Arc Welding as applied to Steel Structures i.e., 60,500 lb./in.^{2} for end fillets and 40,500 lb./in.^{2} for side fillets, provided the root penetration is not less than −1.0 mm. (x and y = −0.039 inch, Fig. 1).

That is to say, provided single bead fillet welds are applied within the limits of procedure stated above, and are free from visible defects, their strength should conform to the requirements of British Standard Specification No. 538.

It is assumed that the electrical conditions of the welding circuit are suitable for the type of electrode employed, and that the latter meets the requirements of British Standard Specification No. 639 for Grade A electrodes. The Specification required that Grade A electrodes deposit all-weld-metal having the following minimum mechanical properties:

Tensile strength = 62,500 lb./in.^{2}
Elongation = 20% in $1^1/_2$ inches
Reduction of area = 35%
Izod notch-impact value = 30 ft.-lb.

It frequently happens in practice that the fillet welds deposited are slightly larger than those required on the drawing. This natural tendency arises through a desire to avoid rejection of welds because they are undersize and the consequent expense of applying an additional bead of weld metal.

The important point, therefore, arises in the practical interpretation of these limitations, that an inspector should not necessarily reject fillet welds that are slightly over the safe limits proposed, for the strength required by the designer may still be ensured.

Consider, for example, an extreme case in which a fillet with $^1/_4$-inch leg is required and the operator, using an electrode 0.16 or 0.19 inch diameter, actually deposits a fillet with $^5/_{16}$-inch leg. It will be seen from Fig. 1 that under such conditions the root penetration may be as low as −2.0 mm. (x and y = −0.079 inch, Fig. 1), consequently that the gross throat stress may drop to, say, 49,500 lb./in.^{2} However, in the design it was assumed that the ultimate strength of the fillet would not be less than $^1/_4 \times 0.7 \times 60{,}500$, or 10,500 lb. per inch, whereas the minimum ultimate strength of the weld deposited would be $^5/_{16} \times 0.7 \times 49{,}500$, or 10,800 lb. per inch. In other words, its strength would still be sufficient to carry the working load required to be transmitted with an adequate factor of safety, in spite of its relatively poor root penetration.

The tentative limitations proposed may, therefore, be regarded as applying to the weld sizes required, rather than the sizes of the welds as deposited, although the difference in these values should be as small as possible.

In view of the fact that the procedure tests have been restricted to single bead fillet welds it might be thought that but a small section of the investigations on downhand welding has been covered. However, the author suggests that this is hardly the case, for adequate fusion and penetration should be assured in beads subsequent to the first of multi-layer butt and fillet welds, if applied within the limits of procedure determined. In fact, it should be possible to broaden these safe limits. Two dimensional limitations require to be investigated, i.e., the maximum permissible depth and width of each bead of metal deposited, in relation to the size of electrode employed.‡

The degree of fusion and penetration attained in the first or root bead of a butt weld can generally be determined by inspection. The practice, common in many works, of cutting out the back of the first bead provides a further precaution against the occurrence of hidden faults in the root.

† The weld size limitations or electrodes 0.13, 0.16 and 0.19 inch diameter are based on records obtained from some 600 specimens, including check tests made at various works. The limitations for the larger diameters of electrodes were obtained from approximately 70 specimens and further check tests on the latter are therefore desirable.

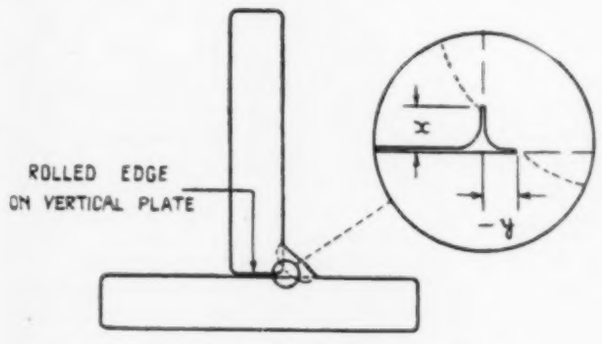


Fig. 1—Method of Recording Penetration in Fillet Welds

‡ A simple rule based on practical experience that is often applied to ensure adequate fusion and penetration, is that the maximum depth of any single layer or run of weld metal shall not be greater than $^1/_2$ in. and its width shall not exceed $3^1/_2$ times the diameter of the electrode employed. In this connection the following extract from the American Welding Society's Specifications for Design Construction, Alteration and Repair of Highway and Railway Bridges by Fusion Welding (1936) may be noted.

"A single layer of weld metal, whether deposited in one pass or made up of several parallel beads, shall not exceed $^1/_2$ in. in thickness, parallel with the throat, except that the bead at the root may be $^1/_4$ in. in thickness if the position of welding and viscosity of the weld metal permit control of the metal, so that it does not overflow upon unfused base metal.

"The maximum size of fillet weld which may be made in one pass is $^5/_{16}$ in."

TENSILE and TENSILE IMPACT TESTS of Stainless Steel Welds at Low Temperatures*

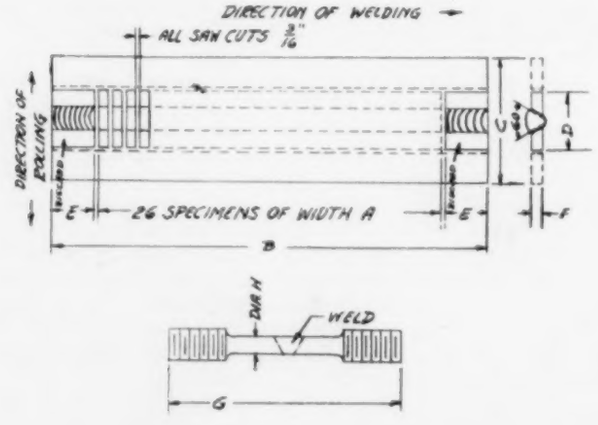
By OTTO H. HENRY†, JOSEPH J. CORDIANO and MORTON S. UMANOFF

THE investigations described herein represent a continuation of a study begun several years ago at the Polytechnic Institute of Brooklyn on the mechanical properties of welded joints at low temperatures.[1,2,3] The present paper gives the results of tensile and tensile impact tests made at temperatures between +20° and −80° C.

METHODS OF TESTING

Static tensile tests were performed at a series of temperatures between +20° and −80° C. The specimen dimensions and a description of the test apparatus were given in the October 1937 issue of THE WELDING JOURNAL.[2]

The tensile impact tests were made at the same series of temperatures, the specimen dimensions and apparatus being as shown in the October 1936 issue of THE WELDING JOURNAL.[1] Since it was found that the full capacity of the testing machine was not sufficient to produce rupture with a single blow, the specimens were given two blows, and the impact values added. The results thus obtained are probably not comparable with other data based on a single impact, but are considered satisfactory for indicating relative impact strength.



TYPE OF SPECIMEN	DIMENSIONS OF TEST PLATE & SPECIMEN*							
	A	B	C	D	E	F	G	H
TENSILE	13/16"	32	12	6	3	3/4	6	0.505
TENSILE IMPACT	1/2"	21	12	2 3/4	1 1/2	1/2	2 3/4	0.200

* CONSULT REFERENCES GIVEN IN TEXT FOR DETAILED DIMENSIONS OF SPECIMENS.

Fig. 1—Location of Specimens in Welded Test Plate

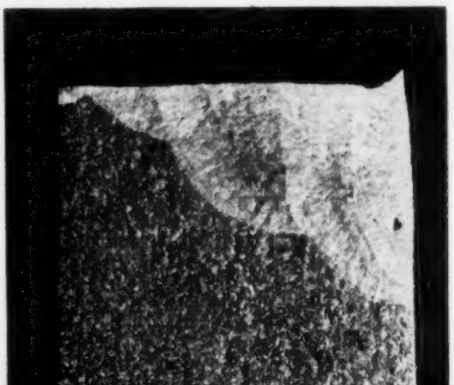


Fig. 2—Tensile Specimen Ruptured Entirely in Weld Metal. (Oxalic Acid Electrolytic Etch. Mag. 3 X)

MATERIALS TESTED

The specimens were supplied through the courtesy of the Carnegie-Illinois Steel Corporation, and had been machined from butt-welded plates in the manner indicated in Fig. 1. The chemical composition of the plate metal was as follows:

Chromium	16 to 20%
Nickel	8 to 12%
Manganese (Max.)	0.50%
Silicon (Max.)	0.50%
Carbon	0.05 to 0.15%

After welding, the plates were annealed by reheating to 1950° F. and cooling in air.

The welded plates and all of the broken specimens were examined both macroscopically and microscopically to determine the quality of the welded joints. In every case there was found to be good fusion of the weld with the base metal, practically no slag inclusions, and no cracks or harmful blow-holes. Notwithstanding, several of the fractures in the tensile specimens occurred either wholly or partly in the scarf. Figures 2, 3 and 4 illustrate the three types of failure that were encountered, namely: (1) entirely in the weld metal, (2) partly in the weld metal and partly in the scarf and (3) entirely in the scarf.

* Abstracted from B.S. theses in the Department of Mechanical Engineering, Polytechnic Institute of Brooklyn, by Joseph J. Cordiano (tensile tests), and Morton S. Umanoff (tensile impact tests). Submitted as a contribution to the Fundamental Research Division, Welding Research Committee.

† Associate Professor of Metallurgical Engineering, Polytechnic Institute of Brooklyn.

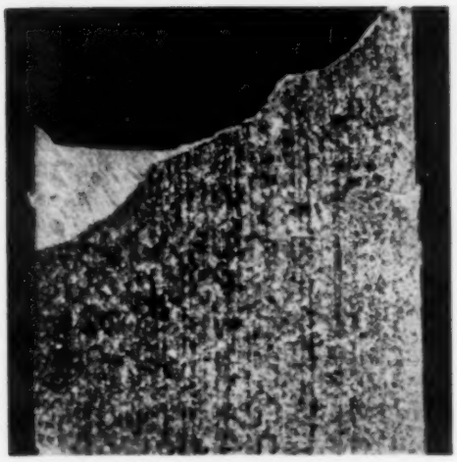


Fig. 3—Tensile Specimen Ruptured Partly in Weld Metal and Partly in Scarf. (Oxalic Acid Electrolytic Etch. Mag. 3 X)

One rather unusual microstructural characteristic of these specimens was noted, and will be described for purposes of record. It was observed in every case that the base metal adjacent to the weld was relatively fine-grained, but that at a distance slightly further removed from the weld there was a noticeable band of a coarse-grained structure. The grain size in this band was definitely coarser than that close to the weld, and likewise coarser than that of the unaffected base metal.

A hardness survey of the plate from which the stainless steel tensile specimens were taken is shown in Fig. 5. This photograph also shows the manner in which the individual beads of weld metal were deposited.

TENSILE TESTS

Tensile tests were made in duplicate at temperatures of +20, 0, −20, −40, −60 and −80° C. For purposes of comparison, similar tests were made with specimens machined entirely from the base metal. The results of the tests are given in Table 1, and the more important data are summarized graphically in Fig. 6.

Table 1—The Effect of Low Temperatures on the Tensile Properties of Stainless Steel Welds and Base Metals

Temperature of Test, ° C.	Tensile Strength, Psi	Proportional Limit, Psi	Elongation in 2 In., %	Reduction of Area, %	Modulus of Elasticity, Psi
		Base Metal Tests			
20	95,000	17,500	66.0	80.0	29.2×10^6
0	125,500	23,000	58.5	74.5	28.8×10^6
−20	137,500	26,500	45.5	57.2	28.8×10^6
−40	156,300	25,000	54.2	68.5	28.1×10^6
−60	163,000	27,500	55.5	69.0	31.2×10^6
−80	171,000	22,500	49.0	60.0	29.6×10^6
		Weld Tests			
21	83,300	22,500	32.5	27.5	27.1×10^6
21	82,000	25,000	31.0	24.0	26.9×10^6
0	103,500	25,000	34.3	25.5	26.9×10^6
0	104,500	25,500	40.5	35.5	28.9×10^6
−20	92,000	25,000	22.5	17.0	28.1×10^6
−20	84,000	30,000	21.0	17.0	31.8×10^6
−40	103,500	25,500	19.3	42.0	29.6×10^6
−40	133,000	25,000	23.0	25.5	30.5×10^6
−40	101,000	25,000	21.0	17.0	31.6×10^6
−60	97,500	27,500	17.0	15.0	29.6×10^6
−60	93,000	25,000	17.0	23.5	32.9×10^6
−80	121,000	22,500	19.3	23.5	30.4×10^6
−80	130,000	22,500	22.5	20.5	28.0×10^6

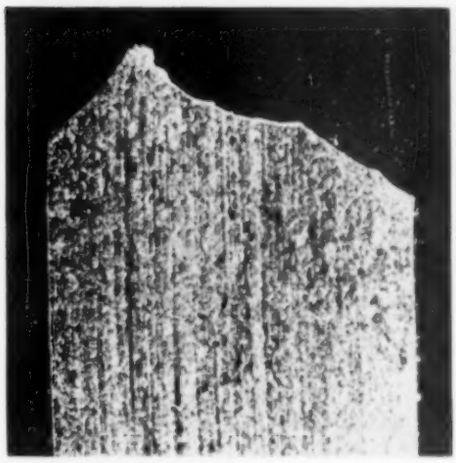


Fig. 4—Tensile Specimen Ruptured Entirely in Scarf. (Oxalic Acid Electrolytic Etch. Mag. 3 X)

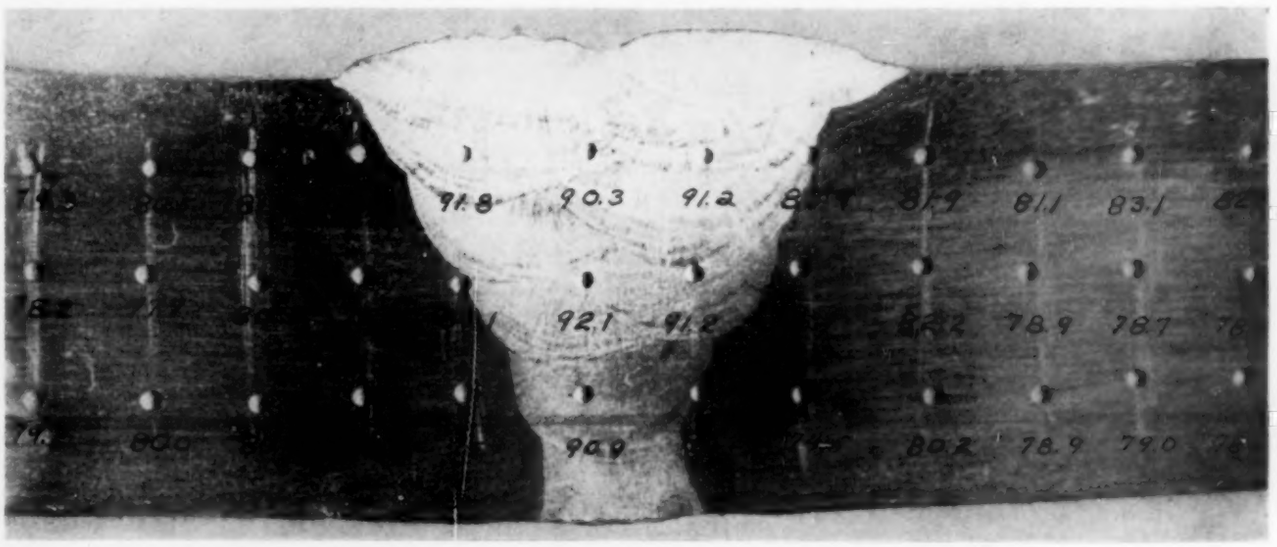


Fig. 5—Rockwell "B" Hardness Survey on Cross Section of Test Plate for Stainless Steel Tensile Specimens

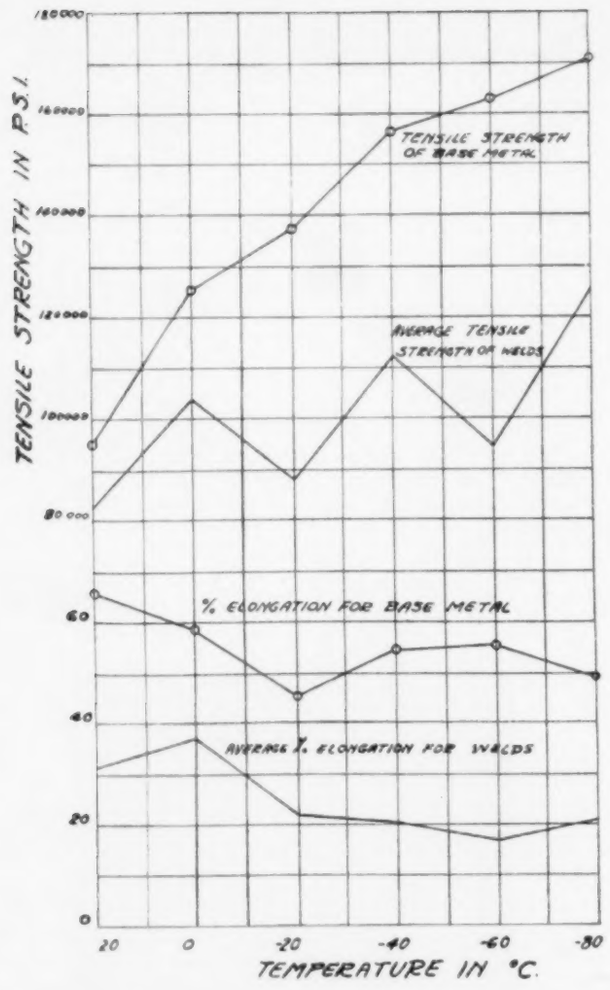

TENSILE STRENGTH IN P.S.I.
TENSILE STRENGTH OF BASE METAL
AVERAGE TENSILE STRENGTH OF WELDS
% ELONGATION FOR BASE METAL
AVERAGE % ELONGATION FOR WELDS
TEMPERATURE IN °C.

Fig. 6—The Effect of Low Temperatures on the Tensile Properties of Stainless Steel Welds and Base Metal

Table 2—The Effect of Low Temperatures on the Tensile Impact Properties of Stainless Steel Welds and Base Metal

Temperature of Test, ° C.	Impact Strength Ft.-Lb. Test A	Test B	Test C	Elongation in 0.80 Inches, Per Cent			Reduction of Area, Per Cent		
				Weld Tests					
26.6	108.0	116.2	115.5	46.3	46.3	45.0	69.1	69.7	67.3
0	120.8	161.0	168.8	43.8	57.5	57.7	66.0	67.9	61.8
−20	158.5	163.0	152.8	48.8	48.8	48.8	45.4	37.6	63.3
−40	110.2	149.0	147.8	36.3	42.5	43.8	20.9	48.2	44.1
−60	151.5	142.5		42.5	40.0		44.6	37.6	
−80	138.0	140.3		33.8	37.5		29.6	36.0	
				Base Metal Tests					
26.6	218.2			82.5			73.5		
0	186.9			66.3			70.7		
−20	174.5			58.8			65.2		
−40	178.0			56.3			68.2		
−60	174.0			50.0			65.9		
−80	174.2			48.8			63.4		

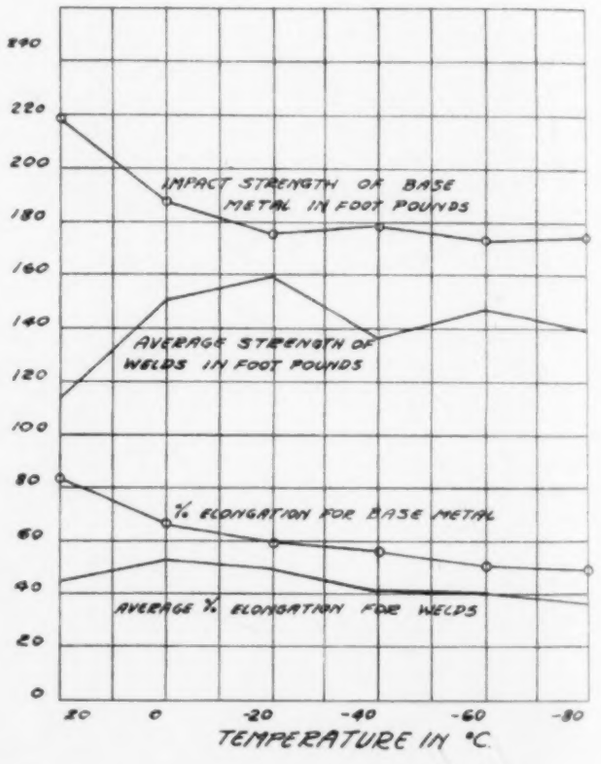

IMPACT STRENGTH OF BASE METAL IN FOOT POUNDS
AVERAGE STRENGTH OF WELDS IN FOOT POUNDS
% ELONGATION FOR BASE METAL
AVERAGE % ELONGATION FOR WELDS
TEMPERATURE IN °C.

Fig. 7—The Effect of Low Temperatures on the Tensile Impact Properties of Stainless Steel Welds and Base Metal

It will be seen that the tensile strength of the base metal increased greatly as the temperature was lowered, while the elongation and reduction of area decreased slightly. The same trends were shown by the welded specimens, but to a lesser degree.

TENSILE IMPACT TESTS

The same series of temperatures was used for the tensile impact tests. Single specimens were used for the test of the base metal, and duplicate or triplicate specimens for the tests of the welds. The results of the tests are given in Table 2, and the more important data are summarized graphically in Fig. 7.

These data show that the impact strength of the base metal decreases rather sharply as the temperature is lowered from room temperature to −20° C., and then remains fairly constant for temperatures down to −80° C. The welded specimens gave somewhat erratic results, from which it was impossible to draw definite conclusions as to trends. At temperatures between 0° and −80° C., however, the welds were about 80% as strong as the base metal.

BIBLIOGRAPHY

1. Henry, Otto H., "The Effect of Low Temperature on the Tensile Impact Resistance of Iron, Steel and Welded Joints," THE WELDING JOURNAL, Research Supplement, **15**, No. 10, p. 2, Oct. 1936.
2. Henry, Otto H., "Static and Impact Tensile Properties of Some Welds at Ordinary and Low Temperatures," THE WELDING JOURNAL, Research Supplement, **16**, No. 10, p. 41, Oct. 1937.
3. Henry, Otto H., "Tensile Impact Tests on Welds at Low Temperatures," THE WELDING JOURNAL, Research Supplement, **17**, No. 8, p. 23, August 1938.

THE ENGINEERING FOUNDATION

Welding Research Committee

Sponsored by the American Welding Society « « » » and American Institute of Electrical Engineers

Supplement to the Journal of the American Welding Society, October 1939

AN INVESTIGATION OF THE SPOT Welding of Automobile Grade Mild Steel*

By WENDELL F. HESS, D.Eng.,† and ROBERT A. WYANT, E.E.‡

INTRODUCTION

WORK on this material was suggested by the Resistance Welding Section of the Industrial Research Division of the Welding Research Committee. The material is a low carbon steel, 0.036 inch in thickness, which has been processed by cold rolling to a very smooth finish and then annealed in a protective atmosphere to prepare it for deep drawing operations, and at the same time preserve the smooth, bright finish.

The principal purpose of this investigation was to establish optimum conditions for welding the material. Other objectives were to compare oily with degreased stock, and to study the effect of short time of dwell of electrode pressure after the cessation of current flow.

* To be presented at Annual Meeting, A. W. S., Chicago, Oct. 23 to 27, 1939. A contribution to the Industrial Research Division, Welding Research Committee.

† Associate Professor of Metallurgical Engineering, Head of Welding Laboratory, Rensselaer Polytechnic Institute, Troy, New York.

‡ Research Fellow, Department of Metallurgical Engineering, Rensselaer Polytechnic Institute, Troy, New York.

An interesting avenue of study, which opened up during the course of the investigation, was the effect of fine grain versus coarse grain on weld properties. Grain sizes in the material supplied for this work ranged from A. S.-T. M. No. 5 down to No. 7. The material was supplied in comparatively small sheets cut from different large ones, and thus effectively sampled a sizable tonnage of steel. For comparative purposes, a strength-current survey was made with four-inch radius, dome-shaped electrode tips, using six cycles and 750 pounds electrode pressure. This is approximately the same total pressure as was used with the 1/4-inch diameter flat tips to obtain 15,000 psi. To completely study the question of dome-shaped electrodes will require an extensive investigation.

The electrode material used in this investigation is the same as that used in the previous work, a copper alloy of approximately 80 Rockwell B hardness, 85 per cent electrical conductivity and elastic limit of 60,000 psi.

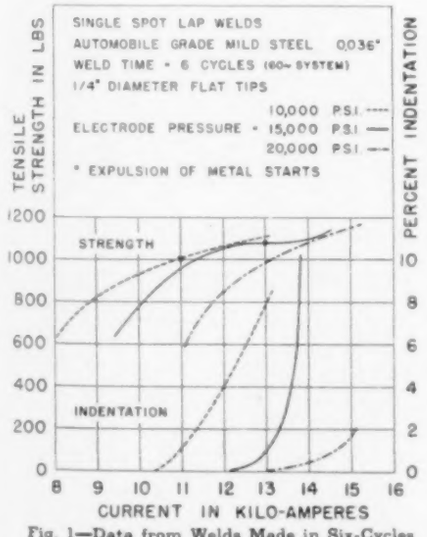


Fig. 1—Data from Welds Made in Six-Cycles with 1/4 Inch Diameter Flat Tips at Different Electrode Pressures

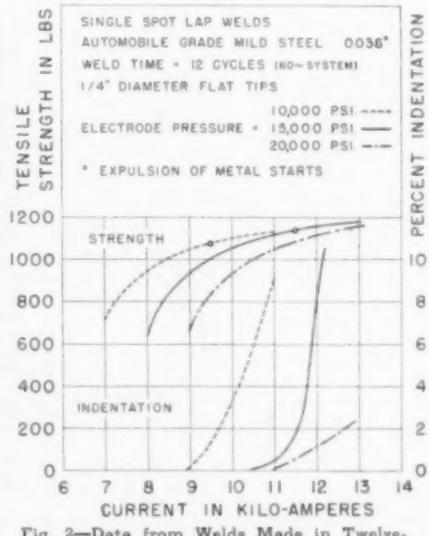


Fig. 2—Data from Welds Made in Twelve-Cycles with 1/4 Inch Diameter Flat Tips at Different Electrode Pressures

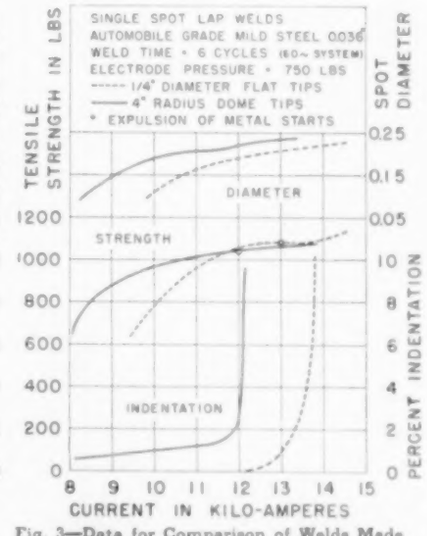


Fig. 3—Data for Comparison of Welds Made with 4-Inch Radius Dome-Shaped Tips and Welds Made with 1/4-Inch Flat Tips

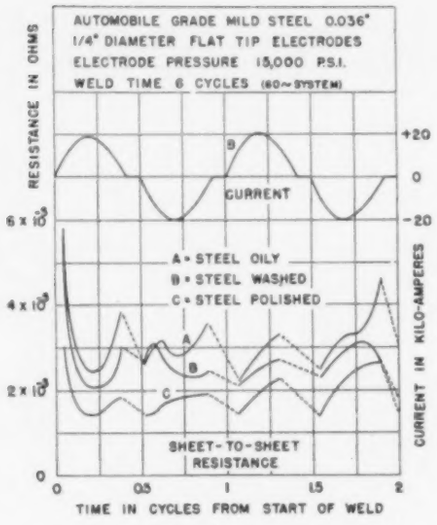

AUTOMOBILE GRADE MILD STEEL 0.036"
1/4" DIAMETER FLAT TIP ELECTRODES
ELECTRODE PRESSURE 15,000 P.S.I.
WELD TIME 6 CYCLES (60~ SYSTEM)
RESISTANCE IN OHMS
CURRENT IN KILO-AMPERES
CURRENT
A = STEEL OILY
B = STEEL WASHED
C = STEEL POLISHED
SHEET-TO-SHEET RESISTANCE
TIME IN CYCLES FROM START OF WELD

Fig. 4—Sheet-to-Sheet Resistance Characteristics for Welds Made Under Different Surface Conditions

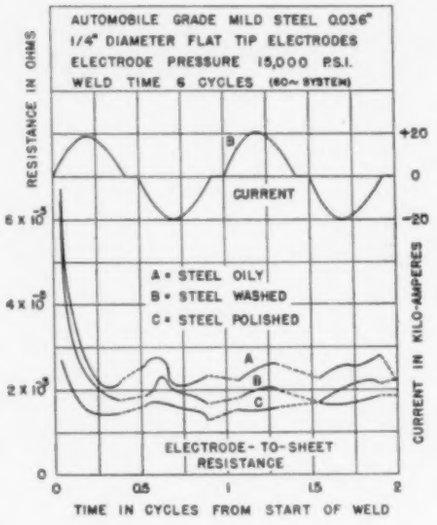

AUTOMOBILE GRADE MILD STEEL 0.036"
1/4" DIAMETER FLAT TIP ELECTRODES
ELECTRODE PRESSURE 15,000 P.S.I.
WELD TIME 6 CYCLES (60~ SYSTEM)
RESISTANCE IN OHMS
CURRENT IN KILO-AMPERES
CURRENT
A = STEEL OILY
B = STEEL WASHED
C = STEEL POLISHED
ELECTRODE-TO-SHEET RESISTANCE
TIME IN CYCLES FROM START OF WELD

Fig. 5—Electrode-to-Sheet Resistance Characteristics for Welds Made Under Different Surface Conditions

Unless otherwise noted all of the material used in this investigation was degreased in a boiling tri-sodium phosphate solution, rinsed in boiling water, quickly dried and stored in a desiccator until welded.

The pressure for these welds was controlled by an air-thrust cushion on a motor-driven spot welder. Time of current flow was controlled by thyratron equipment and the current magnitude was varied by changing the primary voltage.

STRENGTH CHARACTERISTICS

The strength characteristics were determined in a hydraulic testing machine, using completely self-aligning Templin grips. The specimens used were $^{15}/_{16}$ inch wide by three inches long. The two pieces to be welded were held in a jig so that the weld was made at the center of a one-inch overlap.

Previous experience with plain carbon steel suggested the use of six and twelve cycles for weld time in this work. Shorter times require abnormally high electrical inputs, with which flashing difficulties are multiplied, and full size, properly fused welds are difficult to obtain. Times longer than twelve cycles show no advantage. Experience also suggested the use of $^1/_4$-inch flat electrode tips.

Figure 1 shows strength and indentation characteristics as a function of current for six-cycle welds made at unit electrode pressures of 10,000, 15,000 and 20,000 psi. A pressure of 10,000 psi is more likely to permit erratic expulsion of metal due to slight surface irregularities, leave internal cavities and produce less consistent results. There is a marked increase in the satisfaction of working with 15,000 psi, as compared with 10,000 psi.

A pressure of 20,000 psi produces strong welds, but also causes undesirable distortion and separation of the sheets, due to extrusion of metal at this pressure. See photograph *C* of Fig. 15 of our other paper,[1] for an illustration of this phenomena. This distortion caused abandonment of work at 20,000 psi before expulsion of metal occurred, which accounts for the indentation curves being discontinued at comparatively low values.

Figure 2 shows similar characteristics to Fig. 1, except that the time is twelve cycles instead of six. The opti-

[1] "Further Studies of the Spot Welding of Low Carbon and Stainless Steels," W. F. Hess and R. A. Wyant, Oct. 1939 Welding Research Supplement, pp. 348-s–354-s.

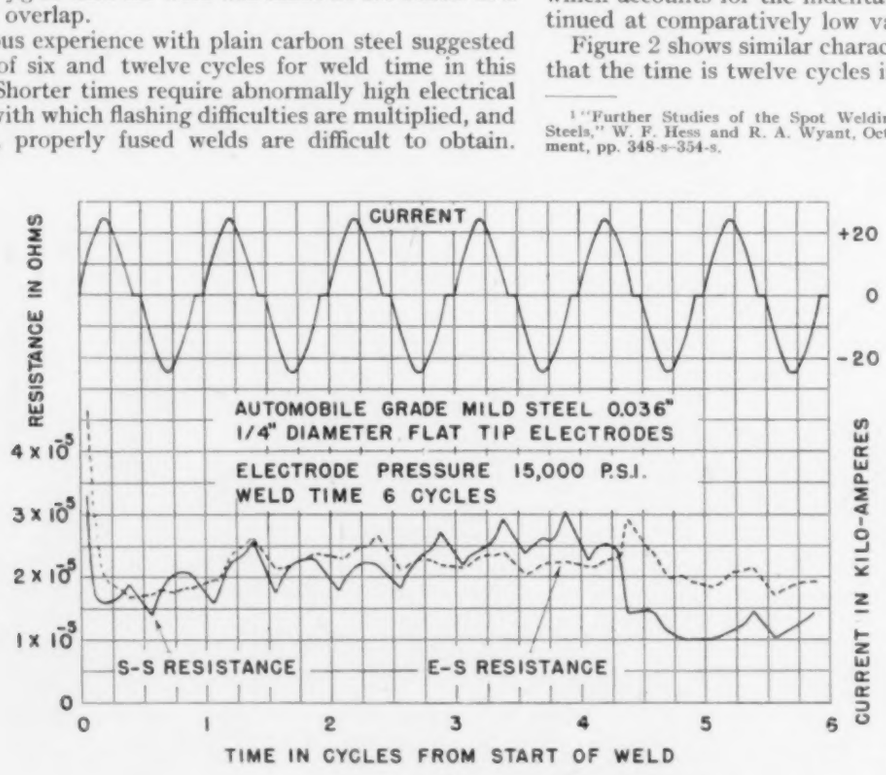


Fig. 6—Complete Resistance Characteristics for a Six-Cycle Weld

mum currents at 15,000 psi, for twelve and six cycles, are 11,000 and 12,500 amperes, respectively.

Figure 3 shows the comparison between welds made with four-inch radius, dome tips and 1/4-inch diameter flat tips. The 750-pound pressure used with the dome tips was approximately the same as used with the flat tips to produce a unit pressure of 15,000 psi. Dome-shaped tips are preferred by some, in practice, because electrode alignment difficulties are greatly reduced. Others prefer the simplicity of machining flat tips. Although our experience with domed tips has as yet been somewhat limited, it seems possible that with the proper radius, and on comparatively soft materials, a domed shape may lengthen the life of electrodes between dressings. It has been our very definite experience that, for consistent and uniform welds, electrodes must be maintained, within close limits, to a predetermined shape. This holds with equal, or perhaps even greater force, for flat electrodes, where both shape and alignment must be preserved.

It is apparent from Fig. 3 that the dome-shaped tips possess some advantages over the flat. One of these is that optimum welds are produced with almost 1000 amperes less current, or approximately 10% less current and only 5% less strength. Another advantage is that the dome-tip characteristic is a little flatter at the top. This is probably due to the fact that the dome tips concentrate the current to such an extent that fused welds are formed at much lower currents. Microscopic examination confirms the fact that small columnar welds are formed at lower currents. With the domed electrodes, a small columnar weld, 0.087 inch in diameter, was formed with 8200 amperes. With the 1/4-inch flat electrodes, a columnar weld, 0.092 inch in diameter, required 9850 amperes. The welds just mentioned furnished the first points of the diameter curves in the upper part of Fig. 3. A further interesting comparison between these two types of electrode is that welds made with the domed tips all failed with a ductile tear for currents above about 8500 amperes, whereas with the flat tips, tearing did not commence until about 10,250 amperes. It is interesting to note that the spot diameters for the above currents are approximately the same, and average a little less than 3 1/2 times the sheet thickness. Our experience with regard to the diameter of welds made with the 1/4-inch flat tips, in the automobile grade steel differed from that with the other low carbon steel which was annealed and pickled. Whereas in the latter, the diameters of the best welds approached very closely that of the electrode tips, in the automobile steel the diameters of the best welds were almost 20 per cent less than that of the electrode tips. Notice that the diameters of the welds made with the domed tips tend to increase more rapidly when expulsion of metal occurs. The expulsion allows the tips to sink into the sheets, as indicated in the indentation curve, and thus establish a larger area of contact.

In comparing the strength of welds made without removing the protective oil film with that of welds made in completely degreased stock, very slight differences in strength were found. These differences were of the order of only two or three per cent, but since they always favored the degreased stock, they do indicate a tendency. The magnitude of this effect is such that it can be said to be of no practical significance. The danger of welding oily stock, therefore, would not be due to any action of the oil, but to the dirt which it may have picked up, due to careless handling or standing too long exposed in the shop.

Studies of the effect of short time of pressure dwell after the cessation of current flow, were made on twelve-cycle welds. Two series of tests showed a definite tendency to weakness if the pressure was removed in from four to four and one-half cycles after cutting off the current. This time appears to be critical, since welds made with less than four cycles dwell time were definitely stronger, and welds made with more than five cycles were the strongest. With dwell times longer than six cycles there seems to be no further improvement. The magnitude of the decrease in strength with critical dwell time averaged between six and seven per cent for each series of tests. Photomicrographs, submitted in this paper, shed some light on the cause for the low strength at the short dwell times. It is possible that if pressure is released during the transformation from gamma to alpha iron, the volume increase which accompanies cooling through this range might cause the sheets to buckle outward at the center of the spot, thus causing separation at the incompletely solidified center of the weld.

When it was discovered that the automobile steel available for this work was assorted as to grain size, some

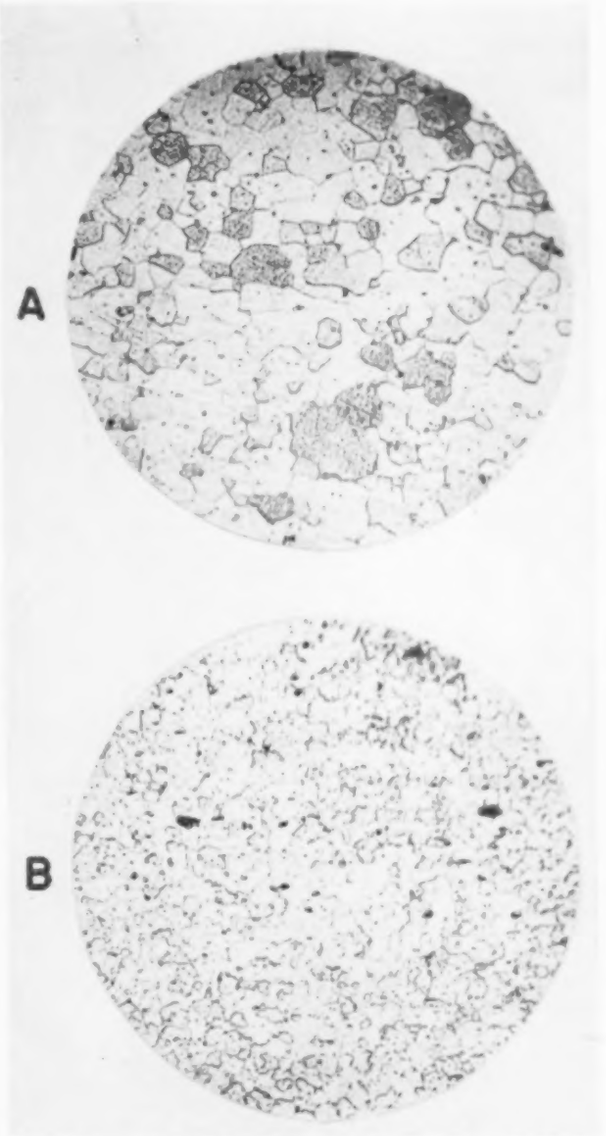


Fig. 7—Range of Grain Sizes Encountered in the Automobile Grade Mild Steel

A—A. S. T. M. Grain Size No. 5, at 100×
B—A. S. T. M. Grain Size No. 7, at 100×

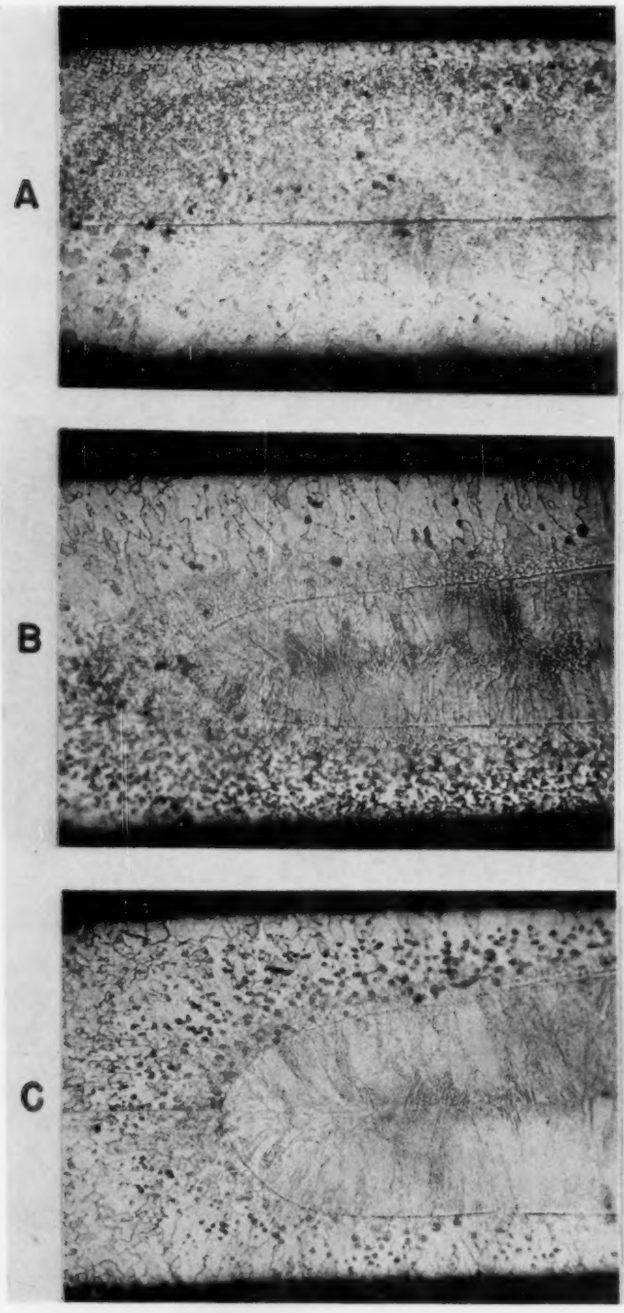


Fig. 8—Welds in Specimens of Various Grain Sizes, at 35 X

A—Pronounced heat effect in the fine grain sheet produced by a current of 9500 amperes for six-cycles which was insufficient to fuse the metal
B—Same effect in fully welded sheets produced by a current of 9500 amperes for twelve-cycles
C—Specimens of uniform grain size, twelve-cycle weld

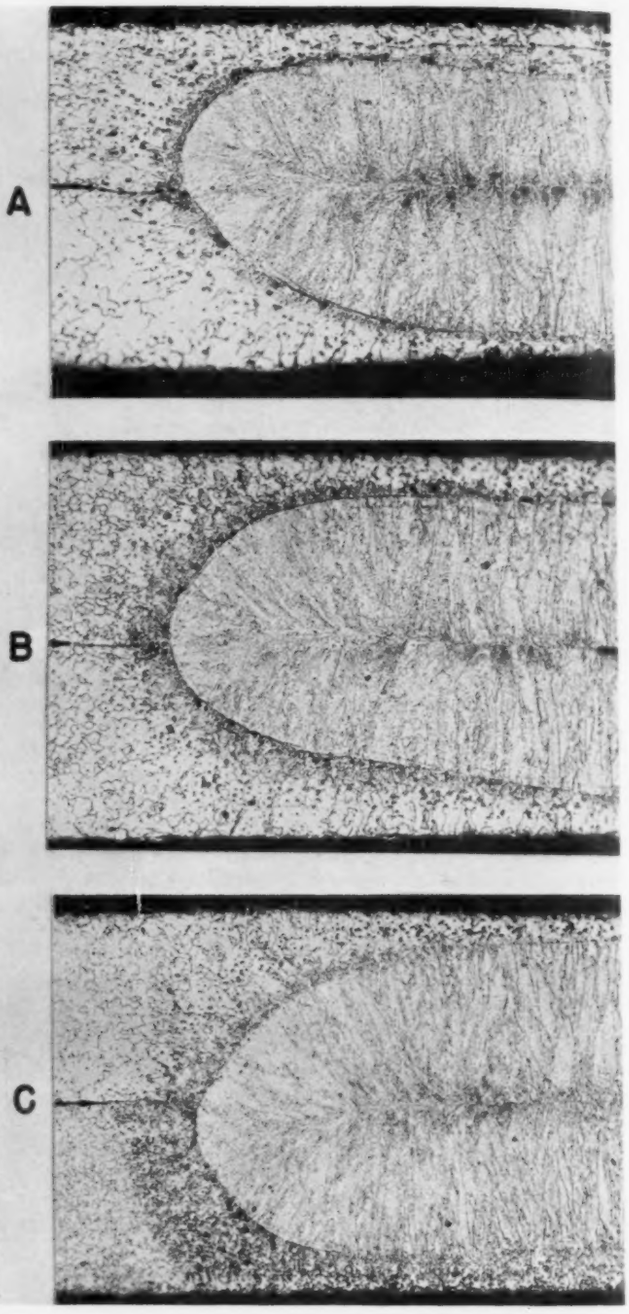


Fig. 9—Six-Cycle Welds in Specimens Selected for Grain Size, at 35 X

A—Coarse to very coarse
B—Fine to fine
C—Fine to very fine

of the coarse and some of the fine were selected for comparison of weld strengths. It was found that the fine-grain steel gave welds which were ten per cent stronger than those made in the coarse-grain material. However, enough of the material for complete strength-current characteristics was not available in the coarsest and finest grain sizes. When selected fine-grain material was welded to selected coarse-grain sheet, in every case failure occurred in the coarse-grain sheet. This is to be expected from our knowledge of the effect of grain size on the strength of metals. The method of selecting material for these welds, for grain size, was by surface hardness measurements, using a Vickers hardness testing machine. This method of selection is based on the metallurgical principle that hardness is in part a function of grain size, the finer grain being associated with the greatest hardness.

RESISTANCE CHARACTERISTICS

The method of measuring the sheet-to-sheet and electrode-to-sheet resistances has been discussed in de-

tail in a previous paper[1] and is also discussed in our contemporary paper.[2] Briefly the method consists of recording simultaneously, by means of an oscillograph, the potentials between the sheets, between one sheet and an electrode, and across a manganin shunt mounted in one of the electrode holders. The oscillograms are measured and the resistances calculated at different points in the weld cycle. In our contemporary paper mentioned above, the oscillogram shown for a mild steel weld (see Fig. 8),[2] is very similar to the oscillograms obtained from welds in the automobile grade steel. In the latter material, however, the "film effect" was more pronounced between the electrode and sheet. This was probably due to the fact that the automobile grade steel was much more oily when received and it was very difficult to remove all traces of the oil. In the same paper the resistance characteristics shown for the mild steel (Figs. 9 and 10) are comparable to the characteristics obtained from welds in the automobile grade steel.

[1] "Studies of the Spot Welding of Low Carbon and Stainless Steels," W. F. Hess and R. L. Ringer, Jr., Welding Research Supplement, October 1938, pp. 39–48.
[2] "Further Studies of the Spot Welding of Low Carbon and Stainless Steels," loc. cit.

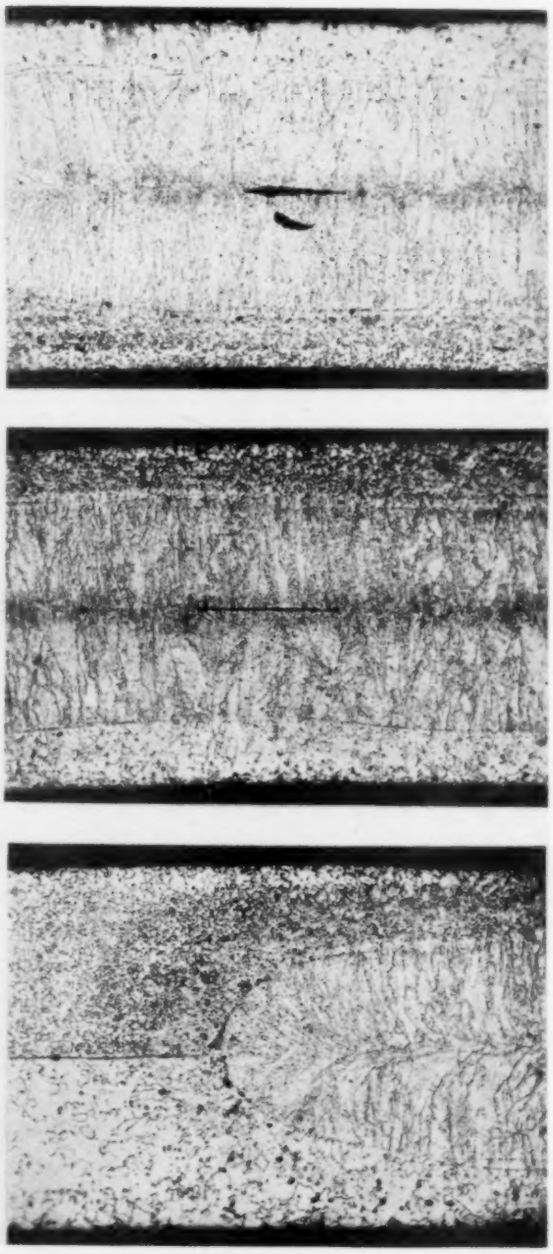


Fig. 10—Welds Made with Different Values of Electrode-Pressure-Dwell Time, at 35 X

A—Two-cycle dwell after twelve-cycle weld
B—Four and one-half cycle dwell
C—Six-cycle dwell

Figures 4 and 5 show resistance characteristics for the first two cycles of six-cycle welds made under different surface conditions. Characteristic *A* was for steel that was used as received, heavily coated with oil. Characteristic *B* was for steel that had been washed in a boiling solution of tri-sodium phosphate and rinsed in boiling water. Characteristic *C* was for steel that had been polished with fine emery paper and then washed in carbon tetrachloride. A very decided film effect is found at the beginning of the *A* and *B* characteristics. While these differences in surface conditions do change the sheet-to-sheet and electrode-to-sheet resistances early in the weld, the weld strength is affected very little, which is possibly due to compensating factors such as a slight reduction in current by an increase in resistance and a loss of energy in vaporizing the oil film. The location of the *B* characteristic with respect to *A* and *C* bears out our observation that after the degreasing treatment a very light oil film still adheres to the steel. It was noticed that after making a large number of welds in supposedly clean steel, a small trace of oil had condensed on the cold electrode tips. The irregularity in the *A* and *B* characteristics early in the second half cycle probably means that the film has not been entirely eliminated when that point is reached in the weld cycle. The drop in the sheet-to-sheet resistance that occurs when the current passes through zero indicates a cooling action during those intervals. It will be noticed that the corresponding changes in the electrode-to-sheet resistance during those intervals are much smaller in magnitude and are sometimes increases, rather than decreases. This indicates that heat may be flowing to the cold electrode from the center of the weld, thus preventing a drop in the electrode-to-sheet resistance by maintaining a more constant temperature in that area. While the phase control was adjusted to give a little less than the maximum heat input for these welds, thus prolonging the zero-current intervals, a similar drop in resistance is found in welds made with a maximum heat input.

Figure 6 shows the resistance characteristics of a complete six-cycle weld. Again the typical film effect is found in both the sheet-to-sheet and the electrode-to-sheet characteristics at the beginning of the weld. The abrupt drop in the sheet-to-sheet resistance during the fifth cycle indicates that metal was expelled from the weld at that point. At the same point in the weld the electrode-to-sheet resistance increased rapidly, indicating that the electrode pressure was momentarily relieved by the expulsion of metal, the inertia of the moving parts preventing the electrode from following through instantaneously. This observation substantiates the demand for a study to determine the effects of inertia in spot welding and may possibly serve as an approach to such a study.

MICROSCOPIC EXAMINATION

The most interesting photomicrographs obtained in the work on automobile grade mild steel are those illustrating the comparative effect of welding on fine-grain and coarse-grain sheet. Figure 7 shows at 100× the range of grain sizes encountered in this work. Photo-

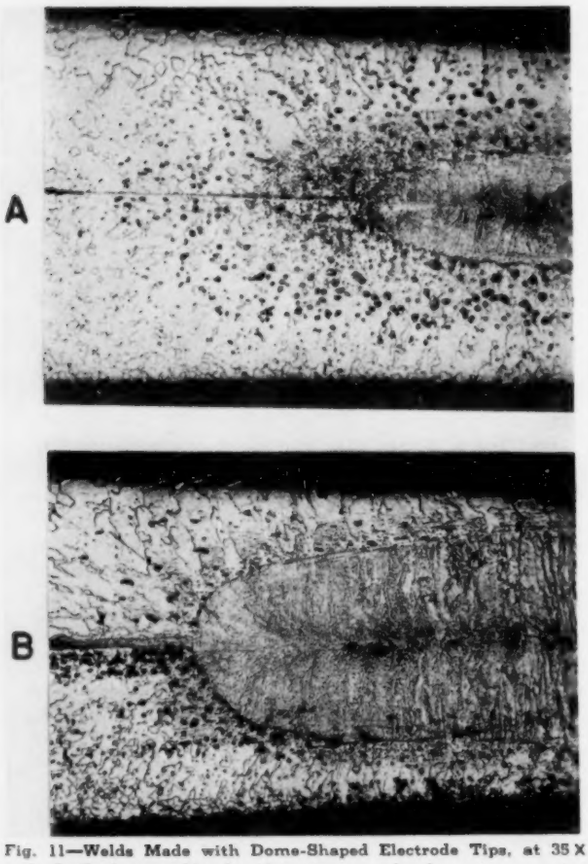


Fig. 11—Welds Made with Dome-Shaped Electrode Tips, at 35 X
A—Small columnar weld made with twenty per cent less than optimum current
B—Weld made with twenty per cent excessive current

graph *A* corresponds to A. S. T. M. grain size No. 5, while *B* corresponds to No. 7 on the same scale. The Vickers Pyramid Numerals, for the grain sizes shown in Fig. 7, are 99 for the steel shown in photograph *A*, and 129 for that in *B*.

Figure 8 is a series of welds in specimens of various grain sizes. Photograph *A* was selected to show the markedly greater heat effect in the fine-grain than in the coarse-grain steel. The upper half, which has a fine grain, shows a pronounced heat effect, while the lower half shows almost no effect in the coarse-grain sheet. This weld, which was very weak in strength, shows only surface sticking and a slight amount of incipient fusion. Photograph *B* shows a regularly fused weld, made in twelve cycles, between sheets of very different grain size. The much greater heat effect on the fine-grain side is again apparent. Photograph *C* of Fig. 8 shows a twelve-cycle weld made between sheets of nearly uniform grain size.

A good reason for the greater heat effect in the fine-grain material is that the same current should generate greater heat in this material due to its greater resistivity. For the coarse-grain sheet shown in Fig. 7, the resistivity measured 12.4 microhm-centimeters, while for the fine-grain material the resistivity measured 13.4 microhm-centimeters.

Figure 9 shows a series of grain size effects in six-cycle welds. The sheets for weld *A* were selected as coarse, and those for welds *B* and *C* were selected by the hardness measurements as fine. As seen in the photographs, weld *A* was actually coarse to very coarse, *B* was fine to fine and *C* was fine to very fine. It was difficult to find material like that in the lower sheet of weld *C*, that is, with such fine grain. Notice in Fig. 9, as in Fig. 8, the more pronounced heat zones in the finer of the grain sizes in each weld. Notice also that the fused section of each weld is more extensive in the finer grained sheet, where more heat has been developed.

Figure 10 illustrates a series of welds made in the study of the effect of short time dwell of electrode pressure. Each of these welds was made by welding a fine-grain sheet to one of coarse grain. In photograph *A* the pressure remained on for two cycles after the end of the current cycle. Weld *B* had a four and one-half cycle pressure dwell, and weld *C* had a six-cycle pressure dwell. The four and one-half cycle dwell of weld *B* was that which gave the weakest welds in the strength tests. The separation, along the line of interference of the last portions of the columnar grains to solidify, is probably responsible for the weakness of these welds. A reason for this separation was suggested above, in the discussion of dwell time under "Strength Characteristics." Weld *A* also shows some separation and a cavity, but these welds were slightly stronger as a group than those represented by weld *B*. Weld *C* was completely sound and represents welds of full strength.

Dome-shaped tips were used to make the welds of Fig. 11. Photograph *A*, at 35×, shows the very small columnar weld, which was measured for the first point on the diameter curve for these welds, shown and discussed above. The concentration of current by the dome tips has here produced a small fused weld, with a current of 8200 amperes, and having a strength of 720 pounds. Such a current would have produced no weld at all with $^1/_4$-inch flat tips. Photograph *B* shows the other extreme of a weld made with excessive current. Note the trail of expelled metal leaving the weld at the left and passing out between the sheets. The much coarser columnar grains at the right, which is the center of the spot, are also characteristic of excessive current.

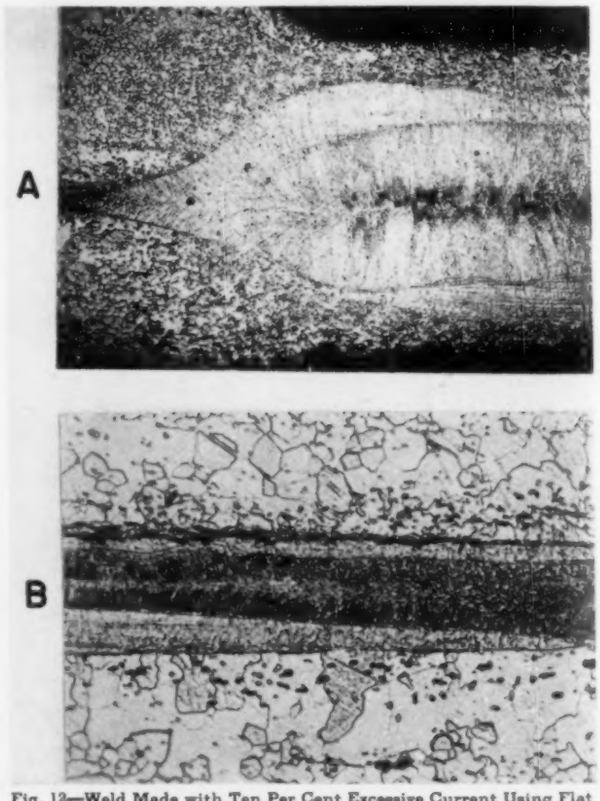


Fig. 12—Weld Made with Ten Per Cent Excessive Current Using Flat Electrode Tips
A—End of weld showing indentation, banding and expulsion of metal, at 35×
B—Section of trail of double expulsion at 100×

Figure 12 is included as a good example of a weld made with excessive current, using 12 cycles, 15,000 psi, 1/4-inch flat tips and 12,700 amperes, which was 1200 amperes too much. This weld is somewhat of a curiosity in that the 10 per cent excess current has caused a double expulsion of metal. Photograph *A*, at 35×, shows almost all the common evidences of excessive current: excessive indentation, amounting to twenty per cent; banding, the appearance of two or more welds within each other; and a trail of expelled metal leaving the weld. Cavities cannot be expected in this weld, since for this material the pressure used was sufficient to reweld all voids left by the expelled metal. Photograph *B*, at 100×, shows a portion of the trail of double expulsion. Note the columnar structure of the inner portion, last to solidify.

RESULTS AND CONCLUSIONS

As a brief summary of the results discussed above, together with the conclusions reached, the following points should be noted:

1. Single spot lap welds having a tensile shear strength of 1100 pounds are obtainable in this material, when using 1/4-inch flat tips, and 15,000 psi electrode pressure.
2. The optimum pressure for flat tips is very close to 15,000 psi, pressures of 10,000 and 20,000 psi each being less satisfactory.
3. The optimum current for six-cycle welds under the above conditions is 12,500 amperes; and for twelve-cycle welds is 11,000 amperes. Times between six and twelve cycles are considered, by experience, to be best for this work.
4. Only five per cent less strength than above is obtainable with four-inch radius dome-shaped tips, using 11,500 amperes, six cycles and 750 pounds electrode pressure. This current is 1000 amperes or about ten per cent less than for the 1/4-inch flat tips.
5. The welding of this material, at 15,000 psi electrode pressure, in the oily condition, as received, produced such a slight decrease in strength, as to be of no practical significance. Caution is suggested, if the oil is very dirty.
6. The time of electrode-pressure dwell was found to have a critical value at which welds of somewhat lower strength were produced. For twelve-cycle welds, six to seven per cent less strength resulted when the pressure dwell time was from four to four and one-half cycles.
7. Differences in grain size affected the strength of spot welds in the automobile grade steel by about ten per cent. This difference is important in a research program, where small effects are being studied, but may or may not be important in shop practice.

ACKNOWLEDGMENTS

The generous cooperation of Mr. M. B. Gathman and the Fisher Body Corporation, in supplying the material used in these studies, is deeply appreciated.

Thanks are likewise due Mr. G. S. Mikhalapov, Chairman of the Resistance Welding Section of the Industrial Research Division of the Welding Research Committee, for his assistance and that of his committee.

REACTIONS IN ARC WELDING*

The Effect of Graphite in the Electrode Coating

By R. DAVID THOMAS, JR.,† and F. H. RHODES‡

A SCIENTIFIC development of a flux coating for electrodes involves a knowledge of fundamental reactions which take place during arc welding. Very little is known about these fundamental reactions; consequently the development of electrode coatings has been largely by trial and error. Many years of research on this problem will be required before a complete picture of arc-welding reactions will become apparent. As a start on this problem, the authors decided to study the influence of graphite in a typical electrode coating on the slag and weld deposit.

The coatings on rods for electric arc welding serve several purposes: to assist in maintaining a stable arc; to permit greater variation in the intensity and the distribution of the heat; to form a protective gaseous envelope around the arc, thus minimizing the oxidation of the deposited metal; to form on the deposit a covering of slag under which the metallurgical reactions can be controlled; and to introduce deoxidizers or desired alloying elements into the deposit. Despite the importance of the coating in determining the composition and characteristics of the weld deposit, there is but little published information as to the relationship between the composition of the coating and the metallurgical reactions involved in welding.

By direct analysis of the deposited metal it is difficult to determine the relative amounts of the alloying elements in the coating that are carried into the deposit. We can, however, obtain this information by observing the change in the ratios of the various elements to some "indicator element" in the coating that may be assumed to be carried entirely into the slag. The indicator element used was calcium. It is a reasonable assumption that the reduction of any of the calcium oxide in the coating to metallic calcium or any compound that would be retained in the metal does not occur to a measurable extent, and that the loss of calcium by volatilization is negligible.

The electrode coating contained graphite, pyrolusite, ilmenite, limestone, Newburgh chalk, ferrosilicon, potassium silicate and potassium chromate. The powdered dry ingredients, with the exception of the potassium chromate, were mixed. The potassium chromate, dissolved in a little water, was added to the solution of

* To be presented at Annual Meeting, A. W. S., Chicago, Oct. 23-27, 1939. Contribution to Fundamental Research Division of Welding Research Committee.

† McMullen Fellow, Cornell University. Now with the Arcos Corporation, Philadelphia.

‡ Director, School of Chemical Engineering, Cornell University, Ithaca, New York.

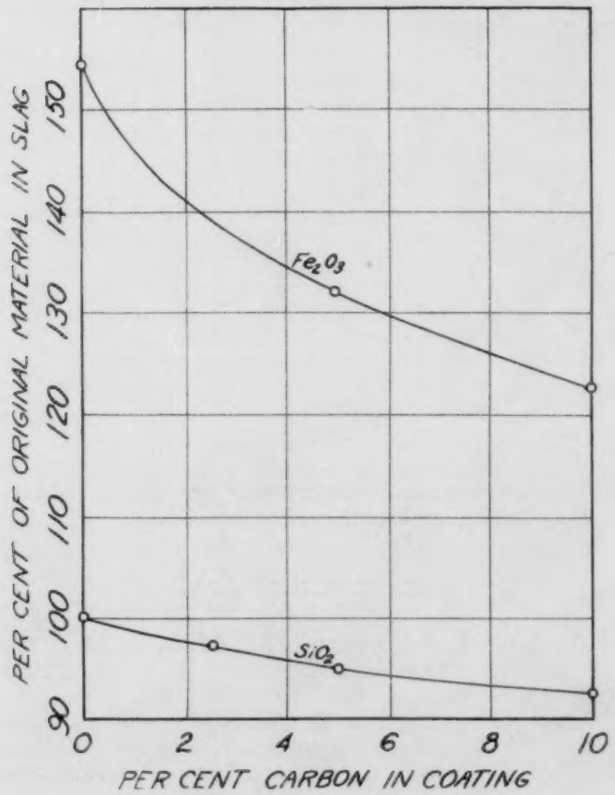


Fig. 1

potassium silicate, this liquid was then added to the dry materials in small portions and with constant mixing, until a smooth paste was obtained. This was diluted to the proper consistency with potassium silicate solution.

In preparing the coatings, an attempt was made to keep a constant ratio among all of the dry ingredients except the graphite. The silica content varied, because with changes in the dry materials different amounts of silicate solution were needed to give pastes of proper consistency.

The pastes were prepared as follows:

Table 1—Pastes for Electrode Coatings

Coating no.	I	II	III	IV
Graphite, g.	0.0	14.0	28.0	56.0
Newburg chalk, g.	54.0	52.0	50.0	46.0
Limestone, g.	45.2	43.6	42.0	38.8
Ilmenite, g.	103.2	99.6	96.0	88.8
Pyrolusite, g.	133.6	128.4	124.0	114.8
Ferrosilicon, g.	64.0	62.4	60.0	55.6
Potassium silicate 28° Be., cc.	250	250	350	350
Water, cc.	37.5	37.5	37.5	37.5
Potassium chromate, g. (dissolved in water)	12.5	12.8	12.8	12.8

The core rods were nine-inch lengths of $^3/_{32}$-inch steel of the following analysis:

C	0.012 per cent
Mn	0.017
S	0.025
Si	Trace

The rods, held in a clamp that kept them parallel and vertical, were dipped into the paste, withdrawn slowly and allowed to dry. This process was repeated until the outer diameter of the coating reached 0.134 ± 0.003 in. After the last dipping the coating at the tip was ground off until the metal core appeared. Finally, the coated rods were baked for one hour at about 175° C.

The base-plates were strips of $^1/_{16}$-inch low carbon steel. The welding was done with a "model 300" machine. After cooling, the slag was chipped off for analysis. The plate was cut transversely to the bead, polished, etched with 2 per cent "nital" solution and examined under the metallograph. The entire sample of slag was pulverized to pass a 100-mesh sieve. Samples were analyzed by the usual methods of quantitative analysis.

It was not possible to maintain absolutely identical conditions of welding with the various electrodes. With the smaller amounts of carbon in the coating, the welding could be done with 75 amperes; with the highest concentration of carbon it was necessary to reduce the current to 40 amperes. In all cases, an attempt was made to keep the length of the arc constant.

The analyses of the coatings and the slags are shown in Table 2. In no case were sodium and potassium oxides determined.

DISCUSSION

In Table 3 are shown the "lime ratios" in the slags—that is, the ratios of SiO_2, Fe_2O_3, TiO_2, "Al_2O_3" and MnO to CaO. This table also shows the percentage ratios of the total amounts of these oxides in the slags to the total amounts originally present in the coatings. In Run II, analyses were made only for silica and manganese. The results for Fe_2O_3 and SiO_2 are shown graphically in Fig. 1.

With an increase in the carbon content of the coating there is a striking uniform decrease in the difference between the silica–lime ratio of the coating and that of the slag. The analyzed SiO_2 content of the coating results from the sum of the silicon existing in the form of the oxide and that existing as ferrosilicon. When no graphite is present, the silica–lime ratios of the slag and of the coating are essentially the same, indicating that all of the ferrosilicon of the coating has been oxidized and appears as silica in the slag. With increasing amounts of graphite in the coating, more protection is offered

Table 2—Analyses of Coatings and Slags

	Sample	C (calc.)	SiO_2	Fe_2O_3	TiO_2	"Al_2O_3"*	MnO	CaO
I	Coating	0	32.14	18.19	11.40	2.70	18.23	5.63
I	Slag	..	32.34	28.19	11.80	2.98	17.47	5.63
II	Coating	2.5	31.29	....	....	..	16.90	5.35
II	Slag	..	33.82	....	....	..	18.01	5.97
III	Coating	5	34.22	13.99	9.90	3.00	17.55	4.74
III	Slag	..	37.68	21.07	10.97	3.21	19.94	5.40
IV	Coating	10	30.95	12.69	10.64	1.69	13.56	4.62
IV	Slag	..	36.14	19.61	13.37	1.78	16.07	5.84

* "Al_2O_3" consists chiefly of aluminum oxide, but contains also appreciable quantities of Cr_2O_3.

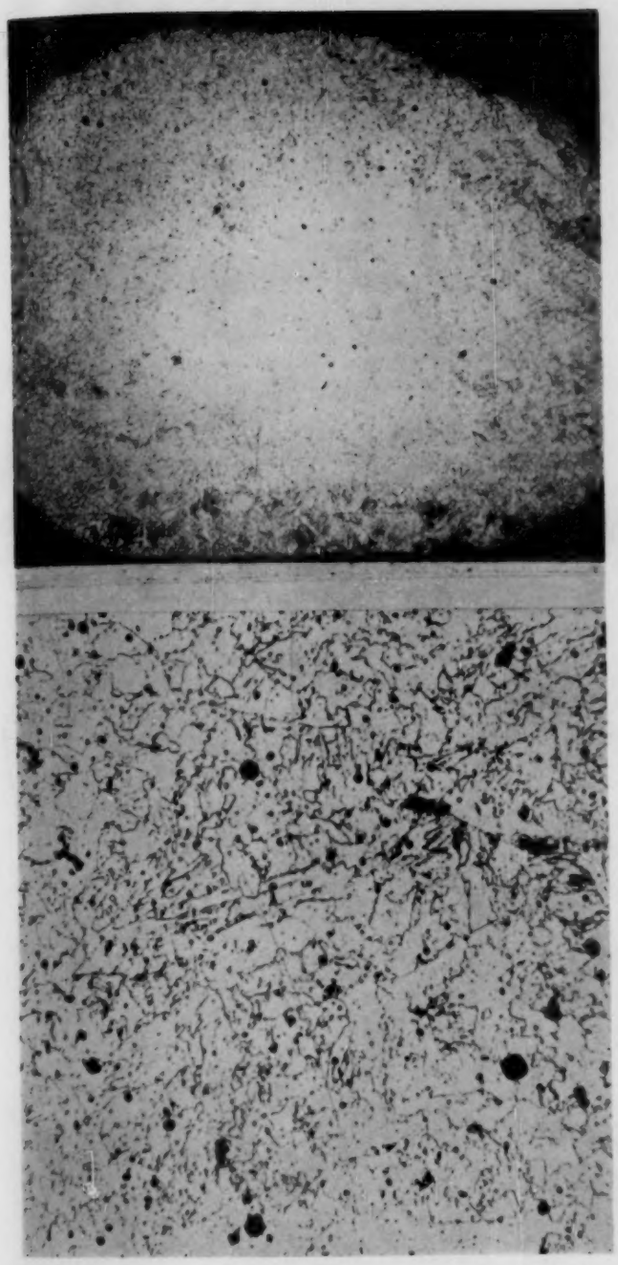


Fig. 2—Run I

(a) (Top) 25 × (b) (Bottom) 100 ×

against the oxidation of ferrosilicon and some of the silicon finds its way into the weld deposit.

The slag always carries more iron oxide than is present in the coating because of partial oxidation of the iron core wire in the arc. As in the case of silicon, less oxidation of iron occurs when the carbon content of the coating is higher.

The titanium and aluminum oxides (the latter including Cr_2O_3) are transferred quantitatively from coating to slag. The apparent variations can be accounted for by unavoidable errors in analysis. For instance, the possible error in reporting 3 per cent "Al_2O_3" (which is obtained by difference) is ±0.3. This would cause a possible error in the alumina–lime ratio of ±0.07. By similar reasoning, the possible error in the titania–lime ratio is ±0.08. The difference between the lime ratios of these oxides in the coating and in the slag never exceeds the possible error caused by analysis.

A small amount of manganese is always carried into the weld deposit. In Run III the amount not reappearing in the slag is small; this run contains the largest manganese–lime ratio. In Run IV the maximum loss in manganese between coating and slag is observed, corresponding to a minimum manganese–lime ratio in the coating. It is possible that for some unexplainable reason the manganese reaction is a more important function of the manganese oxide concentration than that of the carbon concentration in the coating. This can only be definitely established by another series of similar experiments in which the manganese oxide–ratio of the coating is purposely varied while all other ratios are held constant.

The photomicrographs of the weld deposits show that the structure consists of ferrite (white) and pearlite (black). By increasing the carbon content of the

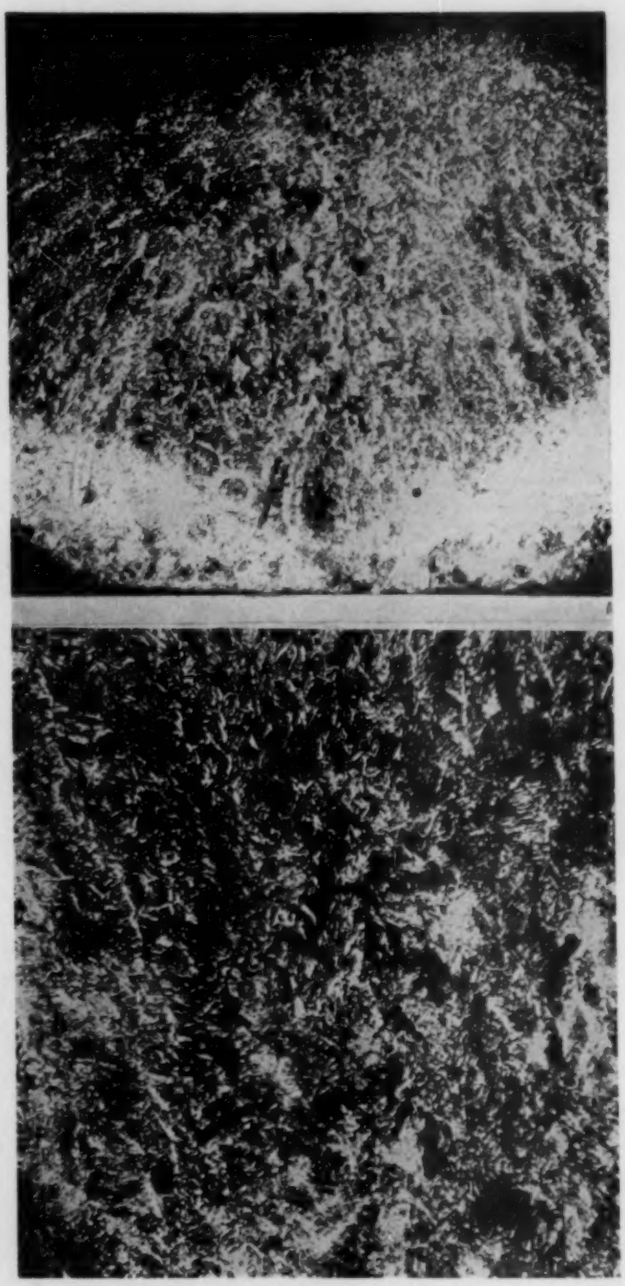


Fig. 3—Run II

(a) (Top) 25 × (b) (Bottom) 100 ×

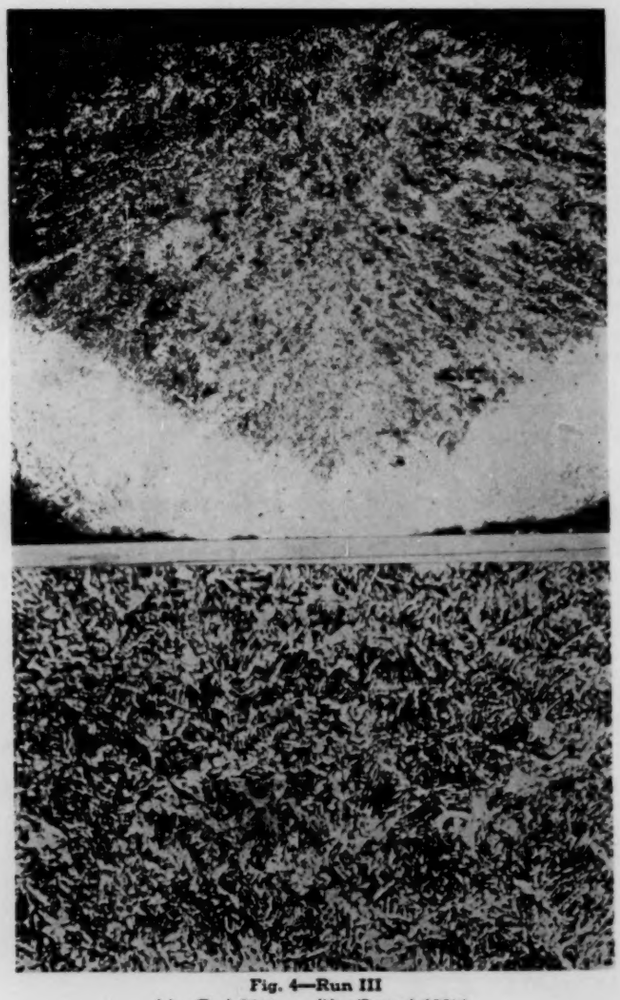


Fig. 4—Run III
(a) (Top) 25× (b) (Bottom) 100×

Table 3—Lime Ratios of Coatings and Slags

Constituent	Exp. No.	Slag	Coating	Difference	Per Cent of Original Material in Slag
SiO_2	I	5.74	5.70	+0.04	100.7
	II	5.66	5.84	−0.18	97.0
	III	6.85	7.22	−0.37	94.9
	IV	6.18	6.70	−0.52	92.3
Fe_2O_3	I	5.00	3.24	+1.76	154.2
	III	3.90	2.95	+0.95	132.2
	IV	3.36	2.74	+0.62	122.7
TiO_2	I	2.09	2.02	+0.07	103.5
	III	2.04	2.08	−0.04	98.2
	IV	2.29	2.30	−0.01	99.5
Al_2O_3	I	0.53	0.48	+0.05	110.3
	III	0.59	0.63	−0.04	93.6
	IV	0.30	0.37	−0.07	81.1
MnO	I	3.10	3.24	−0.14	95.7
	II	3.02	3.16	−0.14	95.6
	III	3.68	3.70	−0.02	99.5
	IV	2.75	2.94	−0.19	93.6

electrode coating, more carburization of the weld deposit is effected (see Figs. II, III, IV and V).

The weld deposit of Run I is found by microscopic observation to contain a large number of inclusions. This may be expected since this deposit contains little or no silicon to act as a scavenger to remove these oxide inclusions.

That we get appreciable quantities of carbon, silicon and manganese in a weld deposit under a slag containing as much as 20 per cent iron oxide indicates that the metallurgical reactions between metal and slag are very incomplete. The time during which the material is molten is so short that equilibrium is not attained. This is particularly true in these experiments in which small diameter welding wire and low welding currents were used. The data herein presented indicate that the metallurgical reactions in welding take place chiefly as the material passes through the arc.

CONCLUSIONS

Increase in the graphite content of the coating for welding rods for electric welding normally results in an increase in carbon content of the deposit. With rods of too low carbon content the deposit carries appreciable amounts of oxide inclusions.

With higher percentages of graphite in the coating, more silicon is carried into the weld metal. The manganese content of the metal may also increase slightly. The amount of iron oxidized diminishes. In all cases, practically all of the titanium and aluminum oxides in the coating reappear in the slag.

ACKNOWLEDGMENT

The authors gratefully make acknowledgment to the Arcos Corporation for the permission to use coating formulas developed in its laboratory.

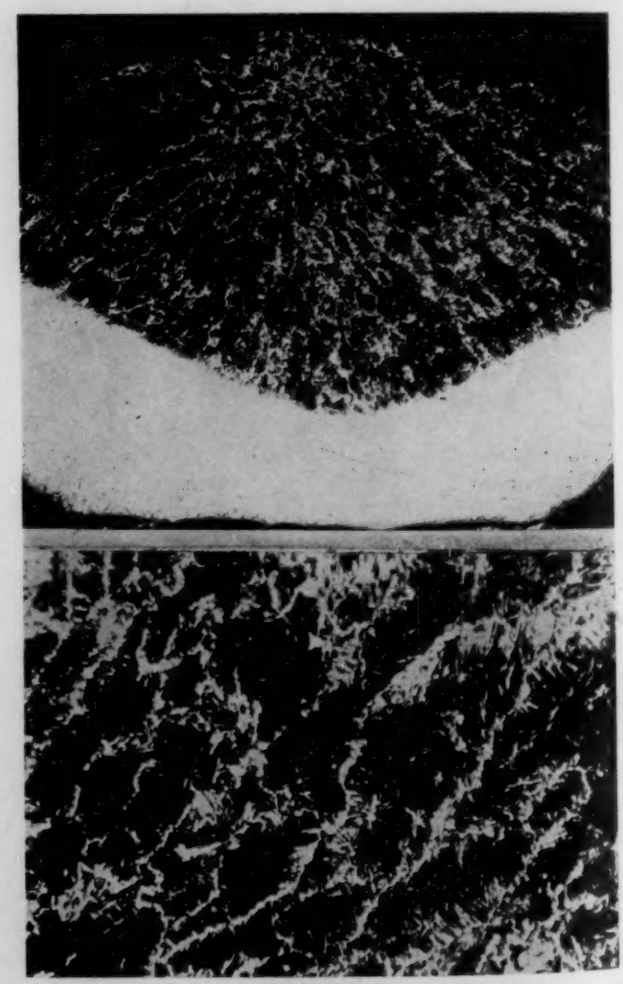


Fig. 5—Run IV
(a) (Top) 25× (b) (Bottom) 100×

WELD HARDENING OF CARBON and of Alloy Steels*

By H. J. FRENCH† and T. N. ARMSTRONG†

INTRODUCTION

IN THE early applications of metallic arc welding to alloy steels, cracking difficulties not infrequently were encountered. These were sometimes traced, in part, to the conditions of welding, involving such variables as speed of arc travel, size of electrode, types of rod coatings, etc. But, also, it became apparent that some of the commercial alloy steels most widely used for heat treated machinery parts were especially sensitive to variations of this sort and developed hard brittle areas adjacent to weld deposits, which reduced the toughness of the welded joints and often caused cracking during or after cooling.

From such early experiences, weld hardening has become well recognized by the welding industry as a factor of importance in the selection of steels, in establishing the details of processing and in the utilization of these metals in welded structures.

It is not to be assumed that absence of weld hardening is all that is needed to provide a satisfactory welding steel but, on the other hand, a high weld hardening capacity means that the steel will be weldable only under restricted conditions.

As is well known, zones of high hardness in welded joints result from the chilling of the highly heated zones, in welding, by the adjacent colder metal areas. The time-temperature relations in both heating and cooling comprise, for practical purposes, a heat treatment and, if the steel to be welded has a high hardening capacity and hardening tendencies, it may develop undesirable highly hardened zones adjacent to deposited metal. These not only are areas low in ductility and toughness, but it is suspected that they should be avoided for other reasons. They create a lack of uniformity in structure and properties which, under applied loads, probably create secondary multi-directional stresses which, in turn, may cause the joint as a whole to react as if made of non-ductile material.

It will be shown that where high elastic properties and strength are needed, along with good welding qualities (low weld hardening), low carbon alloy steels can offer distinct advantages over higher carbon steels, either plain carbon or alloy. The low carbon alloy steels can have added advantages of improved resistance to corrosion, better retention of toughness at low temperatures, better creep resistance at elevated temperatures, better forming qualities, etc.

* To be presented at Annual Meeting, A. W. S., Chicago, Oct. 23 to 27, 1939. A contribution to the Industrial Research Division, Welding Research Committee.

† Metallurgists, The International Nickel Co., Inc., New York, N. Y.

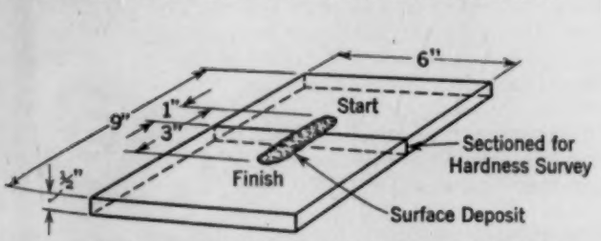


Fig. 1—Sketch Showing Methods Used in Determining Weld Hardenability

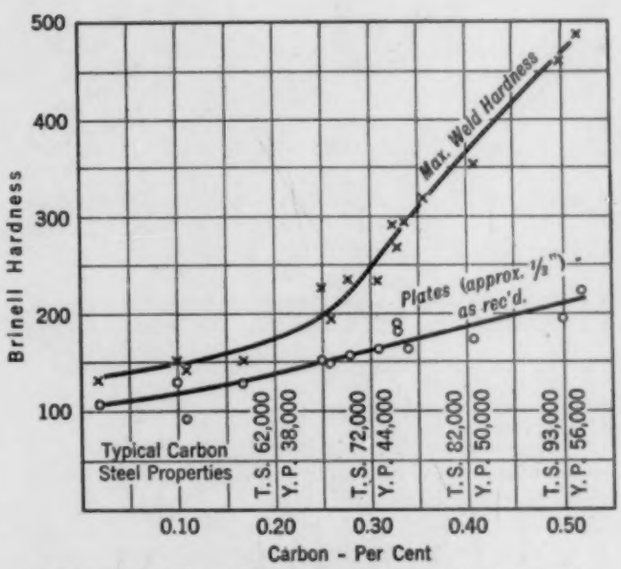


Fig. 2—Effect of Carbon Content in Plain Carbon Steels on Weld Hardening

Lower curve shows plate hardness, as received. Typical tensile strength (T.S.) and yield point (Y.P.) values for normalized plates also given

TEST METHODS

Different methods considered applicable to welding conditions have been used in the study of hardenability of steels. Some of the methods of test involve heating to high temperatures close to the melting point and cooling the steels rapidly in water, iced brines, etc. The sample bars may be heated all over, or one end may be introduced into a furnace which is at the maximum temperature desired and left there long enough for the heat to diffuse so that when the steel is quenched, some portion of the bar is cooled rapidly from all temperatures between the maximum and temperatures below the thermal critical range. Subsequent to quenching, when using any one of these methods, metallographic examination, hardness tests and sometimes impact tests have been made to gain an idea of the effects of the high temperature heating and subsequent rapid cooling on the structure and properties of the steels.

Useful, comparative information can be developed in such tests which, however, are open to the objection that they do not involve the welding operation itself. On this account, it is not always clear how the results may be applied to welding technique.

Among the direct methods of studying weld hardening

Table 1—Properties and Weld Hardening of Carbon Steels

Compositions, % C	Mn	Si	Yield Point, Psi	Tensile Strength, Psi	Plate Thickness, In.	Section	Weld Speed, In./Min.	Rod	Plate Hard. Brinell	Max. Weld Hardness Vic. Brinell	Source of Data
0.02	...	...	26/32,000 (7)	42/48,000 (7)	0.59	.....	7	.....	107	130	(2)
0.10	0.61	0.10	39,600	57,000	0.59	.....	7	.....	130	152	(2)
0.11	0.47	0.12	31,300	56,900	0.47	.....	...	Coated	91	142	(3)
0.17	0.55	0.31	39,800	68,300	0.47	.....	...	Coated	130	152	(3)
0.25	0.43	0.20	.....	.....	0.5	3 x 6 In.	6	Heavy Coated 3/16 In.	151	227	(4)
0.26	0.91	0.07	46,400	74,500	0.59	.....	7	.....	150	197	(2)
0.28	0.54	0.06	.....	.....	0.5	3 x 6 In.	6	Heavy Coated 3/16 In.	157	237	(4)
0.31	0.45	...	38,300	66,000	0.59	.....	7	.....	165	236	(2)
0.33	0.86	0.19	55,000	87,500	0.59	.....	7	.....	182	270	(2)
0.33	0.78	0.33	50,100	84,600	0.5	Large	...	Coated	190	295	(5)
0.34	0.65	0.07	.....	.....	0.5	6 x 9 In.	4/6	Heavy Coated 3/16 In.	165	295	(6)
0.36	0.76	0.26	.....	.....	0.5	6 x 9 In.	4/6	"	...	320	(6)
0.41	0.75	...	.....	.....	0.5	6 x 9 In.	4/6	"	175	355	(6)
0.50	0.63	0.22	.....	.....	0.5	6 x 9 In.	4/6	"	195	460	(6)
0.52	0.60	...	.....	.....	0.5	Large	6	Heavy Coated 5/32 In.	225	487	(1)

(1) T. N. Armstrong: Arc Welding of High Carbon Steels. *Trans. A. S. M.*, 1935, p. 567.
(2) F. Meunier and D. Rosenthal: See Spraragen & Claussen, Welding Research Supplement, Sept. 1937, to THE WELDING JOURNAL, p. 23.
(3) K. L. Zeyen: The Welding of High-Strength Steels. *Stahl u. Eisen* **34** (1935), p. 901.
(4) W. H. Bruckner: The Use of Charpy Tests, etc. *Proc. A. S. T. M.* **38** (1938), p. 71.
(5) Private communication.
(6) Authors' tests.
(7) Typical properties. Not given in original source

is the deposition of a bead on the surface of a steel plate. The welding conditions are standardized with respect to electrode size and type, rate of arc travel, and the dimensions of the plate, as well as the length of the bead laid down. The plate then is sectioned across the weld, one section face is ground or polished, and hardness tests are made over the section. Figure 1 illustrates the method described.

While this has the advantage of utilizing welding technique, it, too, can give only comparative data. The hardness test indentations must be close together, if the zones of maximum hardness are not to be missed. The numerical values of hardness will be dependent, in any one steel, upon the conditions under which the bead is laid on the surface of the plate, the length of the bead laid down, and the position of the test section with respect to start and finish of the weld. Also, they depend upon other variables such as the dimensions of the plate and especially its thickness.

The simplicity of this test recommends it for general surveys, and it is flexible, permitting modifications, such as preheating or precooling of the plates, variations in arc travel speed, plate thickness, etc. It is the method which is the basis of most of the test data and discussions which follow.

Unless otherwise indicated, the conditions of test selected were as follows:

Electrode: 3/16 inch diameter, heavily coated
Arc Travel (Welding Speed): 4 to 6 inch per minute
Plate Dimensions: 1/2 x 6 x 9 inch
Surface Bead: Laid at center line of 6 inch wide plate
Start of Bead: 3 inches from one end of plate
End of Bead: 3 inches from other end of plate
Section for Hardness Surveys: 1 inch from start of deposit.

It should be recognized that single surface beads produce the maximum weld hardening for fixed conditions of welding and given plate dimensions. In multiple layer welds, each bead "heat treats" the weld affected zones produced by the preceding deposit, and generally a multiple bead weld will produce a joint with lower weld hardnesses than are found after only one layer.

If the single bead produces high weld hardness, damage may occur which is not repaired by the subsequent layers. Therefore, the single surface bead test has a definite value in the study of steels for welding.

CARBON STEELS

The results of weld hardening tests on a few plain carbon steels are combined in Fig. 2 with weld hardening data, for somewhat similar welding conditions, taken from different sources. The two lines drawn through the scatter bands of points represent as-rolled and maximum weld hardness of steels containing different carbon contents and illustrate the effects of carbon on the magnitude of weld hardening.

As is shown in Table 1, the steels considered are not all alike in manganese and silicon contents, nor were the conditions of test exactly the same for all the data taken from technical literature. The scatter bands are not wide, however, and it may be assumed that the lines represent typical values for carbon steels with about 0.70% Mn and 0.20% Si, under the selected welding conditions already described.

The plain carbon steels showed no marked hardening with carbon contents below about 0.25%, but with increase in carbon above about 0.25%, the maximum weld hardness increased rapidly, reaching Vickers Brinell values above 450 at 0.50% carbon. These characteristics are already well known, and Fig. 2 and Table 1

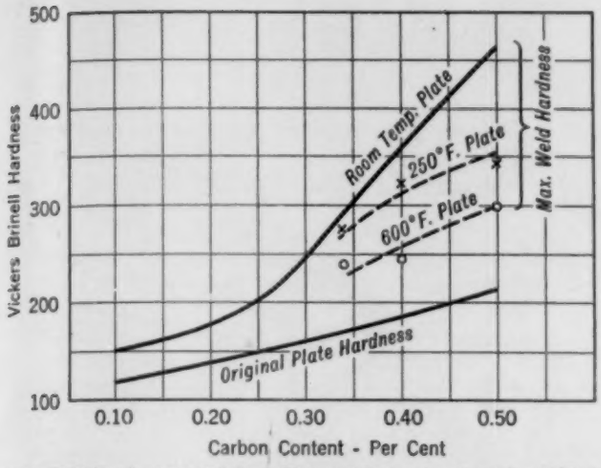


Fig. 3—Weld Hardening of 1/2-Inch Plates of Carbon Steels with and Without Preheating

were prepared mainly as a basis for comparisons with different alloy steels.

The yield point and tensile strength values, at ten point carbon levels between 0.20 and 0.50% C, placed at the bottom of Fig. 2, are representative values for commercial carbon steels normalized in small sections around 1/2 to 1 inch. When considered with the weld hardening data, they become of interest because of the view, still held in some quarters, that it is easier to weld strong carbon steels than alloy steels.

In cases of mobile equipment, for example, railroad rolling stock, where weight reductions may be desired, steels with higher strength and elastic properties than are provided by readily weldable soft carbon steels are indicated. If plain carbon steels are to be employed to provide 50,000 psi yield points, which is a minimum value now in commercial use, the carbon content would have to be kept above about 0.40%, the exact level being dependent upon the thickness of the plates or shapes, etc.

At such carbon levels appreciable weld hardening may occur unless, of course, special technique such as preheating is practicable. While weldable, these higher carbon, plain carbon steels clearly must be handled under restricted conditions, if the weld hardness is to be kept down.

The effects of preheating 1/2-inch plates of carbon steels, welded under the test conditions described, are summarized in Fig. 3. Preheating at 250° F. reduced the maximum weld hardness somewhat more in the steel containing 0.50% carbon than in steels with lower carbon contents, but the maximum hardness of the welded 0.40 to 0.50% carbon steels remained above 300 Brinell.

Preheating at 600° F. reduced the maximum weld hardness 50 to 75 points Brinell below the values observed with a 250° F. preheat, but the maximum hardness of the welded 0.50% carbon steel remained around 300 Brinell.

It should not be assumed that satisfactory welded joints will be insured in the higher carbon, plain carbon steels merely by preheating the parent metal. Preheating can reduce the maximum weld hardness produced from single bead deposits, but it may at the same time stimulate grain growth in portions of the joint, or, if the parent metal previously has been strained, it may promote recrystallization or aging effects, all of which invite a tendency toward brittleness or loss of ductility in the joint.

TERNARY ALLOY STEELS

The weld hardenability observed under the selected test conditions in the ternary steels containing manganese, nickel, chromium and molybdenum is shown graphically in Fig. 4, while the compositions and properties of these steels are given in Tables 2 to 4, inclusive.

Individual additions of 1 1/4 to 1 1/2% Mn, 2 or 3 1/2% Ni, 1% Cr or 0.35% Mo, raise the maximum weld hardness at all carbon levels, the amount of increase being dependent upon the alloy and the proportions added.

Commercial carbon steels generally contain 0.45 to 0.75% Mn, with the higher carbon grades near the upper

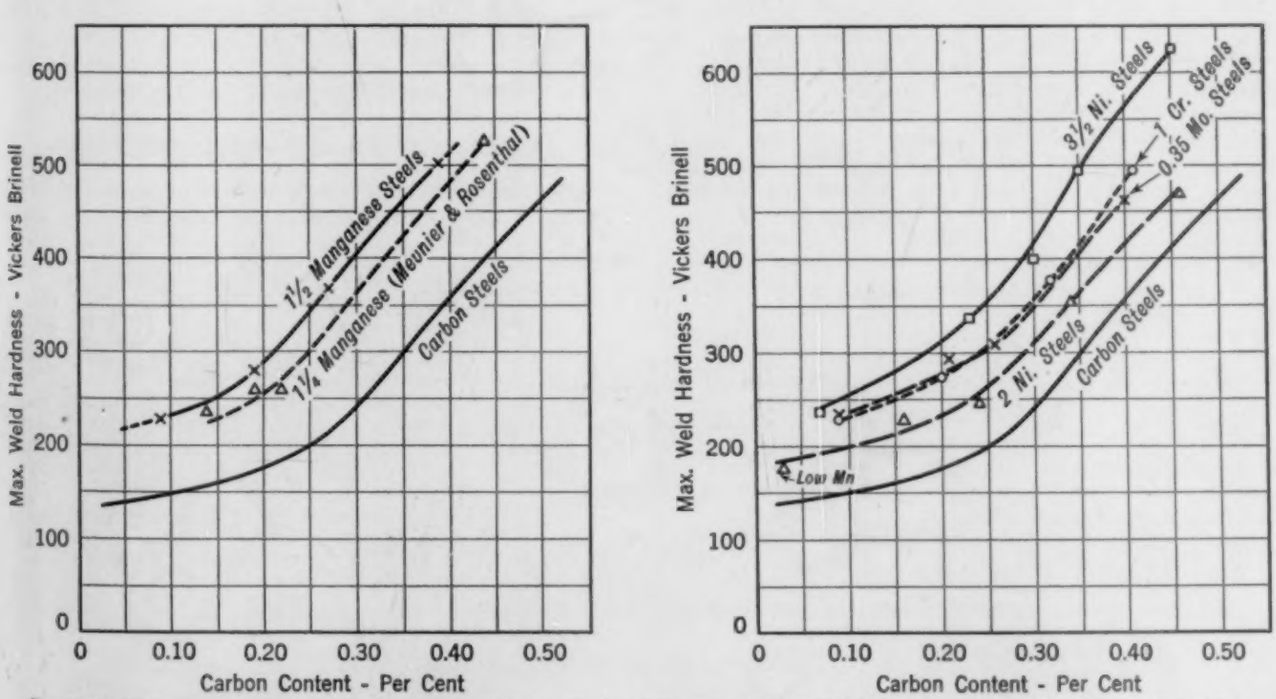


Fig. 4—Maximum Weld Hardness in 1/2-Inch Plates of Carbon Steels and Ternary Steels of Manganese, Chromium, Molybdenum and Nickel
Surface beads deposited at about 5 inches per minute. Heavily coated 5/16-inch electrodes used

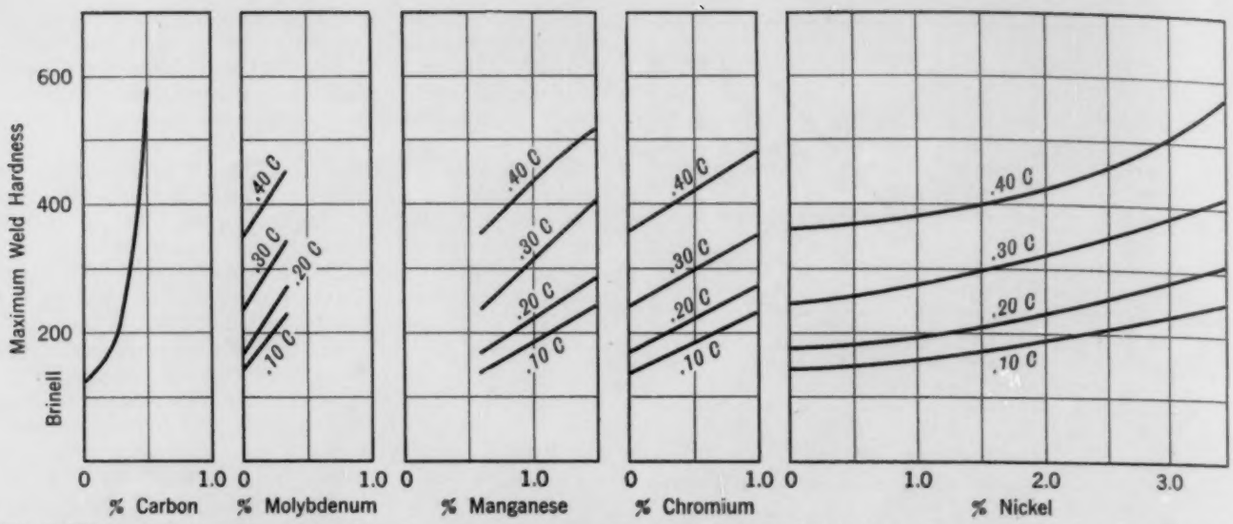


Fig. 5—Effects of Carbon, Manganese, Chromium, Molybdenum and Nickel on Weld Hardenability of Steels Containing Different Carbon Contents

These curves derived from those in Fig. 4

value, so that the increases in maximum weld hardness shown in Fig. 4 from additions of 1½% manganese are produced by increasing the manganese by only about 0.8%. This makes manganese the most potent of the elements considered, next to carbon, in increasing weld hardenability.

The effects of manganese, in the tests under discussion, are confirmed by data published by Meunier and Rosenthal, included in Fig. 4 and Table 2.

Nickel at 2% had a relatively feeble influence on the weld hardenability, the maximum weld hardness being less at any carbon content over the range 0.10 to 0.45% than that produced by individual additions of 1¼% Mn, 1% Cr or 0.35% Mo. The weld hardenability of the 3½% nickel steels was about the same as that of the 1½% manganese steels at carbon contents of 0.30% or less, but somewhat greater at higher carbon contents.

The effects of the several alloying elements on weld hardening are compared on a weight percentage basis with carbon in Fig. 5. The curves were derived from the data in Fig. 4, and show that carbon is, by far, the most potent of the elements considered in promoting high weld hardness. The amount that can be tolerated in the ternary alloy steels is less than that in the plain carbon steels on the basis of any allowable maximum weld hardness.

The slope of the curves showing maximum weld hardness in relation to alloy composition is greater at all carbon contents between 0.10 and 0.40% for the elements manganese and molybdenum than it is for chromium; it is greater for chromium than it is for nickel.

This means that the tolerance for carbon, on the basis of any allowable maximum weld hardness, is least in the ternary manganese or molybdenum steels, somewhat greater in the chronium steels (to 1% chromium) and still greater in the ternary nickel steels, especially within the range up to 2%.

If appreciable proportions of alloys are to be added or if low hardenability over the broadest range of shop welding conditions is desired in alloy steels, carbon contents should be below about 0.10%. This should not be interpreted to indicate that higher carbon alloy steels are not weldable. They are, and many are being welded regularly in commercial production over a broad range of conditions. The trends shown, however, indicate that freedom from objectionable weld hardening over the broadest range of conditions is obtained only with low carbon contents and the lower the better.

WELDING SPEED AND PLATE THICKNESS

In practical welding, the conditions encountered may represent more drastic "treatment" of the parent metal

Table 2—Properties and Weld Hardening of Manganese Steels (1¼ to 1½ Mn)

Composition, % C	Mn	Si	McQuaid-Ehn Grain Size	Yield Point, Psi	Tensile Strength, Psi	Elongation 2 Inch, %	R. A., %	Plate Thickness Inch	Weld Speed, In./Min.	Rod	Plate Hard Brinell (5)	Max. Weld Hardness Vic. Brinell	Source of Data
0.09	1.52	0.20	7–8	44,500	64,600	39	75.1	0.5	5	(1)	124	225	(2)
0.19	1.59	0.20	8	58,200	79,800	33	66.4	0.5	5	(1)	154	284	(2)
0.27	1.42	0.15	7–8	63,000	87,400	31	60.1	0.5	5	(1)	169	370	(2)
0.39	1.42	0.20	8	68,900	98,800	24	43.4	0.5	5	(1)	192	505	(2)
0.14	1.32	0.33	...	.....	.....	..	....	0.59	7	...	165	235	(4)
0.19	1.33	0.58	...	51,000	74,000	20 (3)	....	0.59	7	...	176	260	(4)
0.22	1.03	0.34	...	.....	.....	..	....	0.59	7	...	180	260	(4)
0.44	1.16	0.10	...	64,000	104,000	18 (3)	....	0.59	7	...	200	530	(4)

(1) ½-inch plates, 6 x 9 inches, welded with heavy coated 5/16-inch electrodes at 5 in./min.
(2) Authors' tests.
(3) In length of 7 times diameter.
(4) F. Meunier and D. Rosenthal. See Spraragen & Claussen, Welding Research Supplement, Sept. 1937, to THE WELDING JOURNAL, p. 23.
(5) Authors' tensile tests on normalized plates; data from other sources on plates "as received" and welded.

than that obtained under the selected tests conditions, and higher hardness may be developed in weld affected zones than was shown for many of the steels. On the other hand, more moderate conditions and lower weld hardness may obtain. Consideration of some of the variables, other than steel composition, which may affect the weld hardness produced in the parent metal will help to define the relation of the test data to the range of variations which might be encountered in practice.

Increase in the rate of heat input resulting from increase in arc travel (welding) speed, or factors like decreased plate temperatures or increase in plate thickness, which raise the speeds of cooling, can raise the maximum weld hardness and can change the position of the zones of maximum hardness.

In Fig. 6 the effects of arc travel on 2% nickel steels with from 0.22 to 0.45% carbon are shown along with the effects of plate thickness on the 0.22% carbon grade and, for direct comparison, the effects of carbon content on the 2% nickel steel with an arc travel of around 5 inches per minute.

Increase in plate thickness from $^1/_2$ to $1^1/_2$ inches raised the maximum weld hardness in the 0.22% C, 2% Ni steel about the same amount as an increase in carbon from 0.20 to 0.40% in $^1/_2$-inch plate, namely, from 225 to 400 Brinell. This increase was equivalent (by extrapolation) to that produced by raising the welding speed from 5 to 14 inches per minute, in the 0.22% C grade in the $^1/_2$-inch plate.

The effects shown in Fig. 6 are typical of the trends to be found in many constructional alloy and carbon steels. The single bead fillet weld laid on the surface at relatively high speed is a far more drastic "treatment" of the parent metal than is necessary under many commercial conditions, and the welder has at his command a degree of control of the maximum weld hardening which will occur.

If, instead of the selected test conditions, representative of a single bead fillet weld, multiple layer butt joints are examined, there may be no appreciable weld hardening in the finished joint. This will be apparent from the hardness surveys on butt welded $^5/_8$- and 1-inch plate of 2% nickel steel with approximately 0.20% carbon, in Fig. 7.

The maximum hardness obtained from the single surface beads and in the multiple layer butt welds shown are as follows:

(0.19 to 0.22% C, 2% Ni Steel)

Single surface bead at 5 inches per minute on $^1/_2$-inch plate	235 Vickers Brinell
Single surface bead at 5 inches per minute on $1^1/_2$-inch plate	395 Vickers Brinell
Multiple layer butt weld in $^5/_8$-inch plate	228 Vickers Brinell
Multiple layer butt weld in 1-inch plate	273 Vickers Brinell

PRECOOLING

The plate temperature may exert an appreciable effect on the magnitude of weld hardening, especially with those steels which have a relatively high hardening capacity. Maximum weld hardness obtained under the selected test conditions already described, with plates at room temperature and plates precooled to +5 to +10° F., are summarized in Fig. 8 for both carbon steels and a number of ternary alloy steels.

Precooling the plate increases the temperature gradients, tends to cause somewhat faster cooling of the weld affected zones and clearly results in increases in maximum weld hardness. In other work, not here reported, at temperatures around 32° F., at which outdoor welding in the winter would be stopped, hardening was about as great as at 10° F. used in the tests under discussion.

In some of the tests, notably those on carbon steels and $3^1/_4$% nickel steels, the precooling was carried out in snow in the winter-time and the welding done outdoors. In others, the test plates were cooled in refrig-

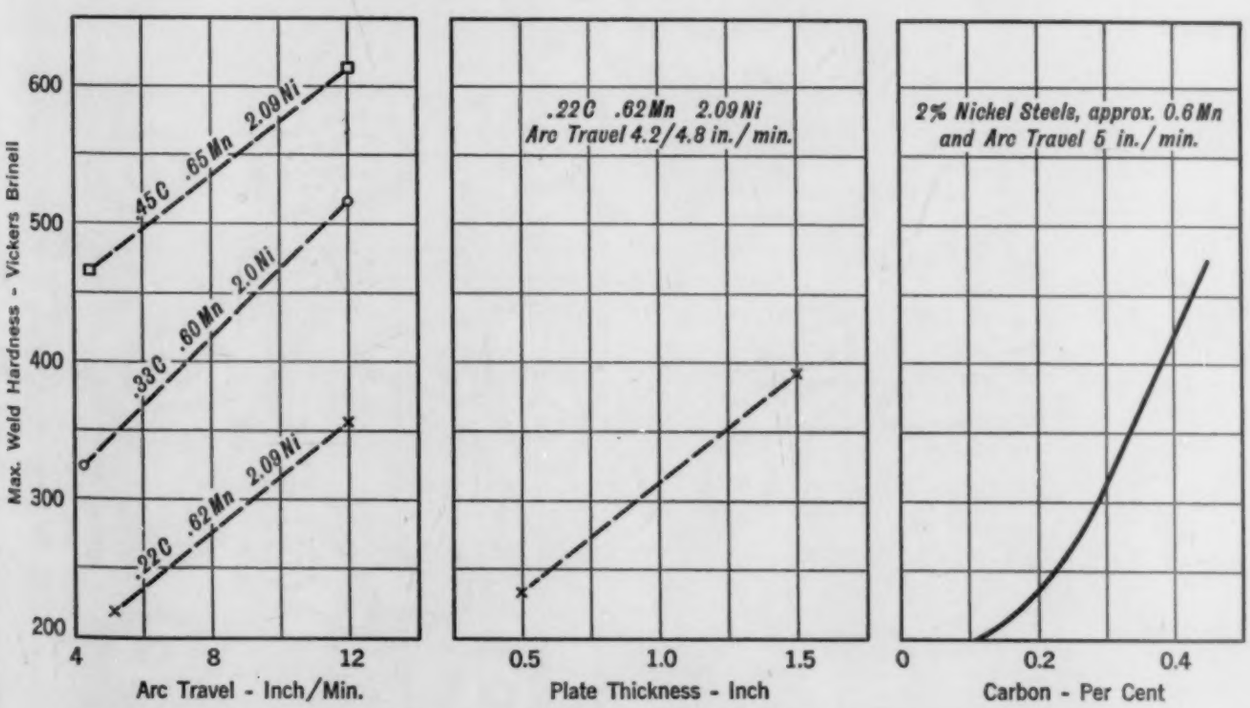


Fig. 6—Maximum Weld Hardness of 2% Nickel Steels as Affected by Arc Travel Speed, Plate Thickness and Carbon Contents

erated solutions, from which they were removed for welding in the laboratory. Under such conditions, a certain scatter of values is to be expected in curves such as those shown in Fig. 8, but, even so, there are indications once again that the low carbon steels are less susceptible to variations in welding conditions which promote weld hardening than are the higher carbon steels. This is shown especially well in the plain carbon steel series, the 1% chromium steels and the 0.35% molybdenum steels.

MAXIMUM HARDNESS OBTAINABLE

The results of the tests already described show that many variables associated with the steel and the conditions of welding can affect the magnitude of weld hardening. This means that no one set of conditions comprising a weld hardenability test can give an adequate picture for general purposes. Certainly it would be hazardous to conclude from any one set of welding conditions that a steel could or could not be welded commercially. Tests of the type utilized as the basis for this discussion can give comparative values and show trends, and such information is of value in the selection of steels and setting the control for welding technique.

Perhaps a better basis for interpretation of weld hardening tests can be obtained by comparing some of the results already described with the maximum hardness obtainable by rapid cooling of steels from high temperatures. In Fig. 9, the curve marked "Maximum Hardness Obtainable by Quenching Carbon Steel," is a reproduction of the curve prepared by Burns, Moore and Archer.[1] The original was given in Rockwell hardness, which is not directly proportional, numerically, to Brinell hardness numbers. Approximate relations are

[1] J. L. Burns, T. L. Moore and R. S. Archer, "Quantitative Hardenability," *Trans. A. S. M.*, **26**, No. 1 (March 1938), page 1.

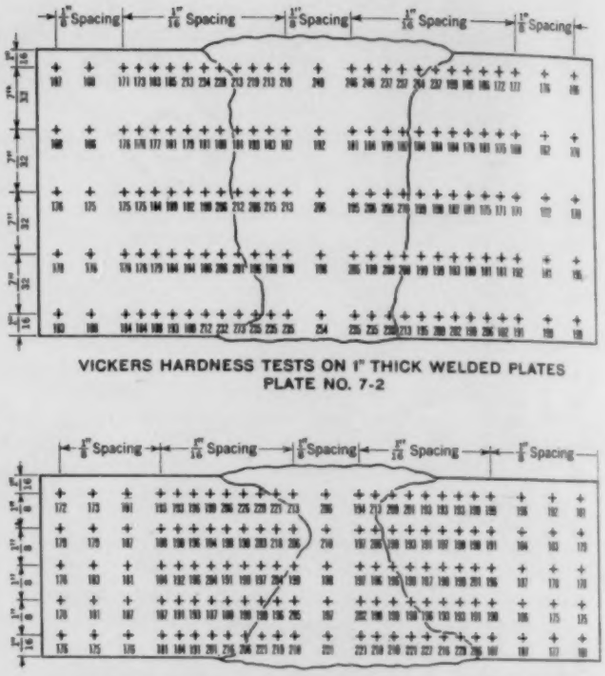


Fig. 7—Hardness Surveys on Multiple Bead Butt Welded Plates of 0.20% C, 2% Ni Steel

Deposits made with heavily coated electrodes depositing 2% nickel steel

Table 3—Properties and Weld Hardening of 2 and 3½% Nickel Steels

Composition, %				Yield Point,	Tensile Strength,	Plate Thick-		Weld Speed,		Plate Hard.,	Max. Weld Hardness, Vic.	Source of
C	Mn	Si	Ni	Psi (5)	Psi (5)	ness, In.	Section	In./Min.	Rod	Brinell	Brinell	Data
0.028	0.19	....	2.08	34,000	50,000	0.5	3 x 6 In.	5	Heavy Coated 3/16 In.	125	175	(3)
0.16	0.62	....	2.0	43,000	67,000	0.5	Large	.	Heavy Coated	160	228	(1)
0.24	0.62	0.22	1.97	49,000	79,000	0.5	6 x 9 In.	5	Heavy Coated 3/16 In.	170	245	(2)
0.34	0.61	0.22	1.99	55,000	92,000	0.5	6 x 9 In.	5	Heavy Coated 3/16 In.	215	355	(2)
0.46	0.66	0.23	2.01	64,000	110,000	0.5	6 x 9 In.	5	Heavy Coated 3/16 In.	235	470	(2)
0.07	0.50	....	3.25	45,000	59,000	0.5	6 x 9 In.	5	Heavy Coated 3/16 In.	155	235	(2)
0.23	0.64	0.18	3.27	62,000	89,000	0.5	6 x 9 In.	5	Heavy Coated 3/16 In.	185	335	(2)
0.30	....	....	3.62	..	..	...	1½ x 5 In.		Heavy Coated 1/8 In.	220	400	(4)
0.35	0.52	0.06	3.23	70,000	108,000	0.5	6 x 9 In.	5	Heavy Coated 3/16 In.	205	495	(2)
0.45	0.51	0.20	3.41	78,000	125,000	0.5	6 x 9 In.	5	Heavy Coated 3/16 In.	255	625	(2)

(1) Private communication.
(2) Tests by W. L. Warner for International Nickel.
(3) W. H. Bruckner: The Use of Charpy Tests, etc. *Proc. A. S. T. M.* **38** (1938), p. 71.
(4) W. L. Warner: AMERICAN WELDING SOCIETY JOURNAL, October 1933, p. 55.
(5) Representative properties, taken from chart representing normalized steels in small sections in *Nickel Steel Topics*, June 1935, p. 10, published by International Nickel Co., Inc., New York.

Table 4—Properties and Weld Hardening of 1% Cr Steels and 0.35% Mo Steels (1)

Composition, %					McQuaid-Ehn, Grain Size	Yield Point, Psi	Tensile Strength, Psi	Elongation 2 Inch, %	R. A., %	Plate, Hard. Brinell	Max. Weld Hardness, Vic. Brinell
C	Mn	Si	Cr	Mo							
0.09	0.62	0.17	0.99	....	5–7	40,800	57,800	41	70.9	111	227
0.20	0.54	0.20	1.00	....	4–5	46,200	71,200	32	57.3	141	272
0.32	0.53	0.20	1.03	....	8	57,500	86,200	25	39.7	168	375
0.41	0.38	0.20	1.00	....	6	67,200	98,000	16	34.1	192	494
0.09	0.88	0.20	....	0.32	6	38,100	61,200	40	74.5	115	231
0.21	0.92	....	....	0.35	7	50,600	75,000	32	53.1	145	293
0.26	0.76	0.20	....	0.32	5–7	50,400	76,600	26	45.1	155	308
0.40	0.64	0.19	....	0.33	8	66,000	96,800	18	37.2	202	463

(1) Welding carried out at 5 inches per minute welding speed, with $^{3}/_{16}$-inch heavily coated electrodes on $^{1}/_{2}$-inch plates, 6 x 9 inches. The tensile properties were obtained on normalized $^{1}/_{2}$-inch plates.

shown in Fig. 9, since all the weld hardness values were obtained directly from Vickers Brinell measurements.

Burns, Moore and Archer state that the maximum hardness obtainable "is shown to depend solely on carbon content, regardless of alloys in amounts present in the common S. A. E. steels and regardless of austenite grain size."

If these conclusions with respect to liquid bath quenching in heat treatment are assumed to be applicable to hardening on welding, the maximum hardness which can be developed in the pearlitic steels is indicated by the upper curve in Fig. 9, and the maximum value obtainable is dependent solely upon the carbon content and not upon the presence of one or more alloying elements in the steel. This, of itself, supports the view that carbon contents in steels for welded structures should be kept at the lowest practicable values.

While maximum attainable hardness may be fixed by the carbon content, the alloys in the steel and the proportions present do exert an important influence upon the hardness numbers developed under fixed welding conditions.

In other words, while the maximum attainable is dependent upon carbon content, the degree to which this maximum is approached under any set of conditions will depend not only upon the carbon content but also upon the alloys present. This is shown by comparison in Fig. 9 of the highest attainable hardness in steels containing different carbon contents with the weld hardness

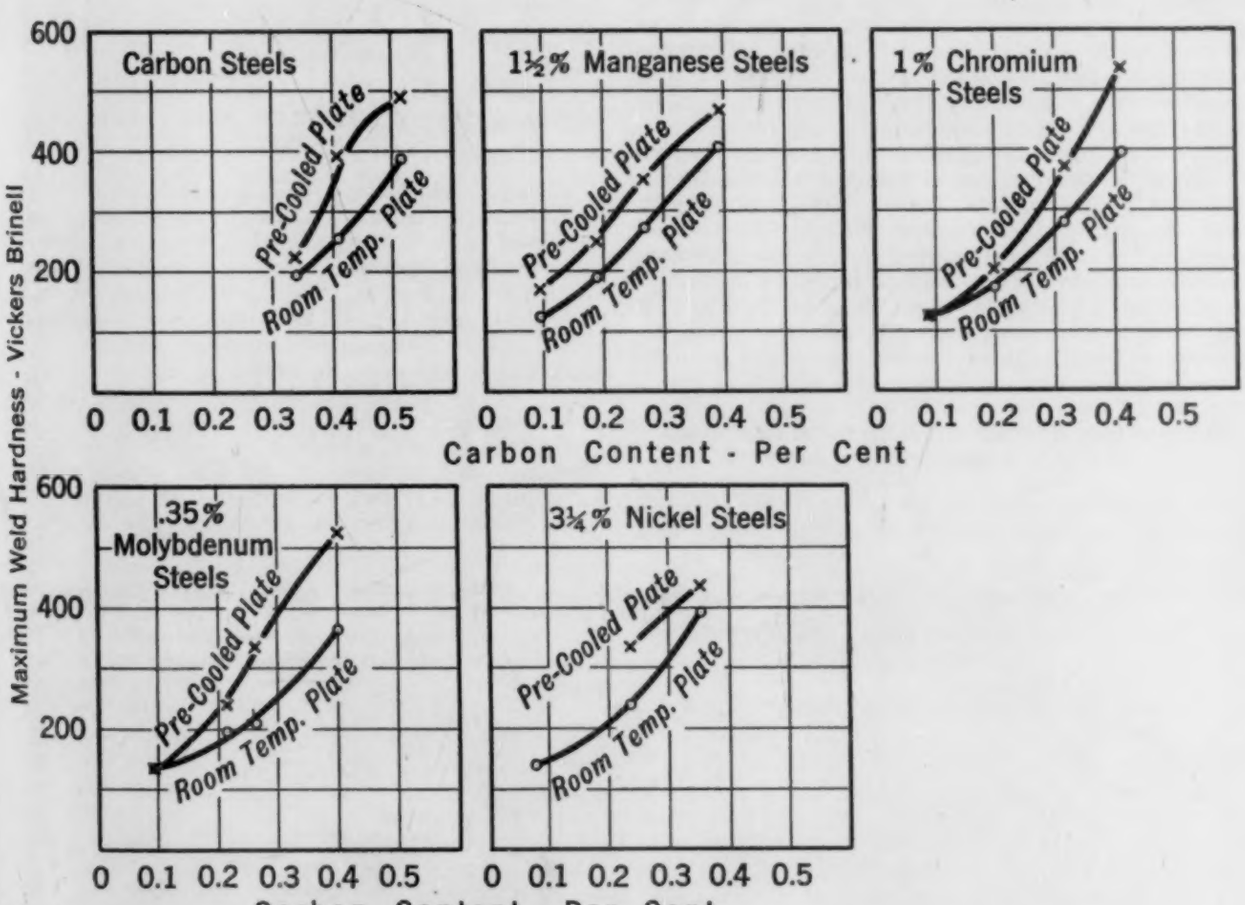


Fig. 9—Effect of Precooling to 5–10° F. on the Maximum Weld Hardness of Carbon Steels and Ternary Steels of Manganese, Chromium, Molybdenum and Nickel

Tests on $^{1}/_{2}$-inch plates welded at about 5 inches per minute arc travel

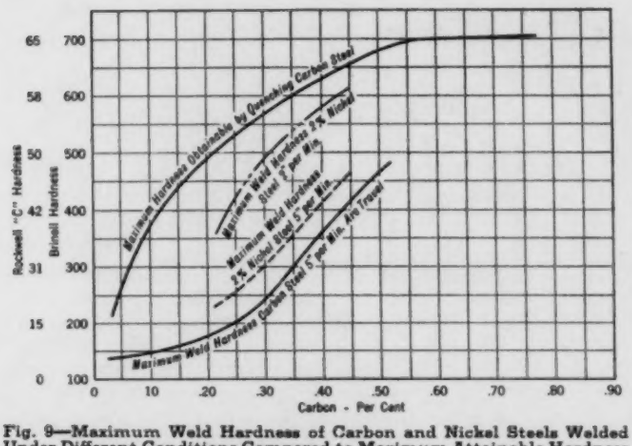

Rockwell "C" Hardness
Brinell Hardness
Carbon - Per Cent
Maximum Hardness Obtainable by Quenching Carbon Steel
Maximum Weld Hardness 2% Nickel Steel 5" per Min.
Maximum Weld Hardness 2% Nickel Steel 5" per Min. Arc Travel
Maximum Weld Hardness Carbon Steel 5" per Min. Arc Travel

Fig. 9—Maximum Weld Hardness of Carbon and Nickel Steels Welded Under Different Conditions Compared to Maximum Attainable Hardness by Rapid Cooling

of carbon and 2% nickel steels welded under different conditions.

For example, the selected weld hardening test conditions involving 5 inches per minute arc travel produced only mild weld hardening, far below the possible maximum which might be developed in both carbon and 2% nickel steels, over the entire range of carbon contents investigated. On the other hand, increase in speed of arc travel to 12 inches per minute on the 2% nickel steels raised the weld hardness close to the maximum obtainable.

The range between the upper and lower curves in Fig. 9 is in reality a range of hardenability over which the welder has at least a certain degree of control. Obviously, he cannot reduce the section thickness if the structure requires heavy plates which tend to promote hardening in fillet welding, but he can control such variables as the temperature of the parent metal by preheating, he can choose the size and type of electrodes and, within certain limits, the speed of arc travel, all of which can operate in the direction of reducing weld hardness.

Viewing Fig. 9 from the broadest point of view, it should be apparent that the freedom in welding is relatively great in steels containing 0.10% or less of carbon. A reasonable degree of control is available to the welder for many low alloy steels containing from 0.10 to perhaps 0.20% carbon, but above this carbon content the allowable welding conditions become more and more restricted as the carbon is increased.

PREHEATING STEELS WITH HIGH HARDENING CAPACITY

Performance requirements sometimes dictate the use of steels with marked hardening tendencies and high hardening capacity and, if welding is to be employed, some methods must be available for the procurement of sound and strong, yet ductile, joints. Much is still to be learned about special alloy weld rods and the technique of welding those alloy steels coming under this classification. But, within certain limits, preheating is useful with alloy steels as with carbon steels.

As a matter of general interest, there is given in Table 5 a summary of the effects of preheating and strain relieving treatments on the maximum weld hardness of a few steels of moderate to high hardening capacity.

The first steel in Table 5, a medium carbon, intermediate manganese-nickel steel, is one of the strong, low-cost types used for light section castings on which repair welding or attachments may be required. Under the selected weld hardenability test procedure, a high maximum weld hardness of 585 Vickers Brinell was secured. This was reduced to around 400 with preheating temperatures of 250° or 600° F. These high values suggest the desirability of strain relieving treatments following any welding operations on this steel.

The same may be said of the cast steels, Nos. 2 and 3, which are medium carbon-nickel-chromium or nickel-chromium-molybdenum steels sometimes used for wear resistance.

Steels containing 3¼% nickel and around 0.35% carbon, represented by the seventh steel in Table 5, have high hardening capacity but, unlike the first three steels, developed under 300 Vickers Brinell when preheated at 600° F. Welded without preheating and then strain relieved at 1110° F., the maximum Brinell hardness was around 250.

Data covering other steels meeting high strength specifications or other special requirements are included in Table 5 as a matter of record.

COMPARISONS OF COMPLEX ALLOY AND CARBON STEELS

The soft plain carbon steels occupy a preferred position over higher carbon steels or the alloy steels, from the viewpoint of weld hardening, but they are deficient in other qualities required in many engineering structures. In the rolled, normalized or annealed conditions, the alloy steels, as a class, can provide higher strength properties, better corrosion resistance in certain environments, improved creep resistance at elevated temperatures, less embrittlement at subatmospheric temperatures, etc., than the plain carbon steels.

Not infrequently, reduction in weight or bulk is desired from designs based on soft plain carbon steels as, for example, in transportation equipment, and, in such cases, two alternatives are available. The carbon con-

Table 5—Effects of Preheating and Strain Relieving on Weld Hardness of Alloy Steels with Appreciable Hardening Capacity in ½-Inch Plates

Composition, %						Cast or Wrt.	Plate Hardness, Brinell	Max. Weld Hardness—Vickers Brinell when Plate Welded at Designated Temperature, ° F.				
C	Mn	Si	Ni	Cr	Mo			70°	250°	600°	900°	70°, Then Stress Relieved at 1110° F.
0.35	1.28	0.39	1.57	..	...	Cast	230	585	415	395	...	...
0.29	0.83	0.24	2.20	0.68	0.25	Cast	255	575	480	415	...	...
0.40	0.76	0.39	2.10	0.79	...	Cast	265	610	570	455	515	...
0.44	0.45	0.24	3.50	1.48	...	Wrt.	340	735	625	670	530	363
0.46	0.66	0.23	2.01	..	...	Wrt.	235	470	...	...	...	300
0.23	0.64	0.18	3.27	..	...	Wrt.	185	335	315	240	...	...
0.35	0.52	0.06	3.23	..	...	Wrt.	205	495	370	290	...	254
0.45	0.51	0.20	3.41	..	...	Wrt.	255	625	...	...	...	297
0.21	1.00	0.28	2.56	0.13	0.27	Wrt.	270	475	415	330	...	...
0.35	0.70	0.26	..	0.72	0.19	Wrt.	265	440	355	315	...	...
0.34	0.56	....	..	1.03	0.18	Wrt.	201	502	426(1)	339	313	...

(1) Preheated at 300° F.

Table 6—Properties and Weld Hardening of Complex, Low Carbon Pearlitic Steels (1/2-Inch Plates)

Composition, %							Yield Point (1), Psi	Tensile Strength (1), Psi	Plate Hardness	Max. Weld Hardness
C	Mn	Si	Ni	Cu	Mo	V				
0.09	0.70	0.03	0.75	1.40	0.11	...	65,000	74,000	190	220
0.12	0.56	....	2.01	1.07	...	...	58,000	78,000	190	263
0.17	0.65	....	3.5	..	...	...	55,000	81,000	175	290
0.13	0.68	....	2.22	..	...	0.11	54,000	78,000	215	249
0.11	0.46	0.98	1.05	0.94	...	...	63,300	78,000	202	274
						Cr				
0.17	0.46	....	1.29	..	...	0.60	54,000	75,000	146	278

(1) Typical properties, normalized.

tent may be raised in the plain carbon steel to secure increased elastic properties and strength, or alloy steels may be utilized. It is proposed to show that very low carbon alloy steels can provide strength properties equal to or better than the plain carbon steels and, at the same time, show lower weld hardenability. Any superiority in corrosion resistance, creep properties or other special characteristics, are added advantages with respect to performance and safety.

The stated objective will not be achieved merely by substituting for plain carbon steel of a given carbon content one or more alloy steels with equal carbon contents. This already has been shown in Fig. 4 where, under fixed welding conditions, all of the alloy steels developed higher weld hardness than plain carbon steels at equal levels of carbon. The most effective approach is through a reduced carbon content supplemented by additions of those alloys which will improve the strength properties of the ferrite and, at the same time, provide such secondary advantages as may be required.

Steel manufacturers, for some years now, have marketed as proprietary products steels which have high elastic properties and low weld hardenability along with improved resistance to corrosion. Typical properties and weld hardening data for some of these steels are combined in Table 6 with the properties and weld hardening of experimental alloy steels and some low carbon S. A. E. type steels.

In Table 6, all of the steels show 54,000 psi, or higher yield points, in the normalized condition in sections around 1/2 to 1 inch in diameter or thickness. The procurement of 54,000 psi yield point in normalized plain carbon steels would require about 0.45% or more carbon, as will be seen by reference to Fig. 2. When subjected to the selected weld hardenability test first described, a maximum weld hardness would be secured in this 0.45% C plain carbon steel of over 400 Vickers Brinell. Yet, all of the low carbon alloy steels listed in Table 6, with equal or higher yield points, show a maximum weld hardness under 300 in every case.

The advantages of the alloy steels are in reality greater than these quoted numerical values indicate for, as is shown in Fig. 9, the highest attainable hardness (under extreme welding conditions) in alloy steels with 0.15% C, or less, is far below that which may occur in plain carbon steels containing 0.45% or more carbon.

The low carbon, complex alloy steels retain their advantage even when the higher carbon, plain carbon steels are preheated. For example, the maximum weld hardness of the alloy steel No. 2, Table 6, was 263, and its yield point was 58,000 psi. The same yield point in plain carbon steel would require around 0.50% C, (Fig. 2). Such a carbon steel showed a weld hardness of 300 Vickers Brinell with a 600° F. preheat (Fig. 3), which is not only troublesome but out of the question for many structures encountered in practice. Alloy steel No. 2 and similar alloy steels have better forming qualities, because of their low carbon contents, and they have the added advantage of improved resistance to corrosion in the atmosphere and in certain other environments.

SUMMARY

It has been shown in tests on carbon steels and a variety of pearlitic alloy steels that weld hardness depends not only on the metallurgical characteristics of the steel being welded, but also upon many variables associated with the welding technique and upon the dimensions of the parts. Therefore, weld hardening tests must be made under exactly the conditions encountered in practice, if quantitative valuation of weld hardening effects is to be secured. Tests under only one set of conditions, which differ from those encountered in practice, can give only comparative data and must be interpreted with care.

A low order of weld hardening under the broadest range of conditions can be secured only in steels with low carbon contents, since these are the only ones which have a low hardening capacity.

Under fixed conditions of welding, the ternary steels of manganese, nickel, chromium and molybdenum, which were tested, as well as more complex pearlitic alloy steels, showed higher weld hardness than the plain carbon steels at equal carbon contents within the range 0.10 to 0.50% C. This may be ascribed to the greater hardening tendencies of the alloy steels.

When weight reductions are desired and steels of high elastic properties and strength are needed, low carbon, complex alloy steels offer distinct advantages over higher carbon, plain carbon steels with respect to weld hardening. At equal or higher yield points, such alloy steels show lower weld hardening than the plain carbon steels, and can have the added advantages of improved resistance to corrosion in the atmosphere and other environments, better retention of toughness at low temperatures, better creep resistance at elevated temperatures, etc. Due to their low carbon contents, they can be expected, likewise, to have better forming properties.

ACKNOWLEDGMENTS

Acknowledgment is made to W. L. Warner, Welding Engineer, Watertown Arsenal, under whose guidance a portion of the weld hardening tests were made; and to G. R. Brophy and his associates at the Research Laboratory, The International Nickel Company, Incorporated, Bayonne, New Jersey, for the remainder of the test data.

FURTHER STUDIES OF THE SPOT Welding of Low Carbon and Stainless Steels*

By WENDELL F. HESS, D.Eng.,† and ROBERT A. WYANT, E.E.‡

INTRODUCTION

THIS paper presents the results of studies made on hot rolled, annealed and pickled low carbon steel, 0.047 inch in thickness, and on 18–8 annealed stainless steel, 0.068 inch in thickness, in both the dull and polished finishes. It is a continuation of work on these materials[1] made on 0.029-inch hot rolled, annealed and pickled low carbon steel, and on 0.028-inch polished stainless steel.

In general, many of the results of the earlier papers were found to be true also for the gages of material discussed here. In addition some interesting effects were noted.

In the case of the low carbon steel, it was found that the best welds were made with a time of between six and twelve cycles, and with an electrode pressure on flat tips of 15,000 psi. This is in agreement with our previous papers. A pressure of 10,000 psi gave cavitation, early expulsion of metal and slightly lower strength. It was also noted that the best welds at this pressure suffered a very slight expulsion of metal, even though there was no violent expulsion. The cavitation which was observed upon sectioning welds at this pressure was evidently due to insufficient pressure to cause rewelding of the voids left by the expelled metal. When a pressure of 20,000 psi was used with the mild steel, undesirable distortion occurred. This was found to be due, as indicated in one of the following photomicrographs, to extrusion of metal adjacent to the weld, between the sheets, causing separation. The above facts clearly indicated that a pressure of about 15,000 psi was best for this material. At this pressure distortion is slight, cavitation is avoided and good strength is obtained.

In the case of stainless steel, 50,000 psi again seems to be the optimum pressure, although it appeared that very good results, with only slightly less strength, could be obtained at 40,000 psi. Six cycles was found preferable to two cycles and very slightly preferable to four cycles, since at the shorter times the rate of power input had to be so high that there was a considerable area of incipient fusion surrounding the weld. With the two-cycle welds, there was evidence of slight expulsion of metal at the edge of the weld zone, probably caused by the extremely rapid rate of heat development, due to the initial contact resistance and high current. This condition is frequently encountered also with insufficient pressure. The combination of high rate of energy input necessitated by short time welding, together with relatively high contact resistance, causes heat to be developed at the interface more rapidly than it can be absorbed by the body of the sheet, resulting in a miniature explosion.

It was interesting to note the two different causes for distortion of the sheets in the case of the mild and stainless steels. These will be discussed in connection with photomicrographs. The expulsion of metal was again found, as in the previous papers, to be highly undesirable, since it resulted in a very marked increase in distortion and surface indentation. Surface indentation was again found to be a sharp criterion of the optimum value of weld current. In the case of a low strength material, such as the low carbon steel, the cavities left by the expelled metal were rewelded, so that these welds suffered very little loss in strength, at values of current sufficient to cause expulsion of metal. In the case of stainless steel, however, which is typical of high strength material, the voids were not closed, with the result that these welds suffered a loss in strength.

The electrode material used in this investigation is the same as that used in the previous work, a copper alloy of approximately 80 Rockwell B hardness, 85 per cent electrical conductivity and elastic limit of 60,000 psi.

All of the material used in this investigation was degreased in a boiling tri-sodium phosphate solution, rinsed in boiling water, quickly dried and stored in a desiccator until welded. The effect of oily surfaces was investigated in connection with our work on automobile grade mild steel and is discussed in our paper on that subject.[2] The same paper also describes work to determine the effects of varying the pressure dwell time, effects of variations in grain size and also preliminary work on the use of dome-shaped electrode tips.

The pressure for these welds was controlled by an air thrust cushion on a motor-driven spot welder. Time of current flow was controlled by thyratron equipment and the current was varied by changing the primary voltage.

For the mild steel, flat electrode tips having diameters of $^3/_{16}$, $^1/_4$ and $^5/_{16}$ inch were used. In the selection of diameter for a flat-tipped electrode, the choice lies between a small tip which will always result in a shear type of failure, likely to be brittle, and a larger electrode with which welds can be made which fail by tearing at the stress concentration point, likely to be a ductile failure. The larger the diameter of the electrode, the wider can be the range of current between which a shear type of failure and expulsion of metal will be produced. With large electrodes the energy required to make a weld in-

* To be presented at Annual Meeting, A. W. S., Chicago, Oct. 23 to 27, 1939. A contribution to the Fundamental Research Division of Welding Research Committee.

† Associate Professor of Metallurgical Engineering, Head of Welding Laboratory, Rensselaer Polytechnic Institute, Troy, New York.

‡ Research Fellow, Department of Metallurgical Engineering, Rensselaer Polytechnic Institute, Troy, New York.

[1] "Studies of the Spot Welding of Low Carbon and Stainless Steels," W. F. Hess and R. L. Ringer, Jr., Welding Research Supplement, October 1938, pp. 39–48; "Data on the Spot Welding of Low Carbon Steel," W. F. Hess and R. L. Ringer, Jr., Welding Research Supplement, April 1939, pp. 113–115.

[2] "An Investigation of the Spot Welding of Automobile Grade Mild Steel," W. F. Hess and R. A. Wyant, October 1939, Welding Research Supplement, pp. 329-s–335-s.

Fig. 1—Data from Mild Steel Welds Made in 6 Cycles with 1/4 Inch Diameter Tips at Three Different Electrode Pressures

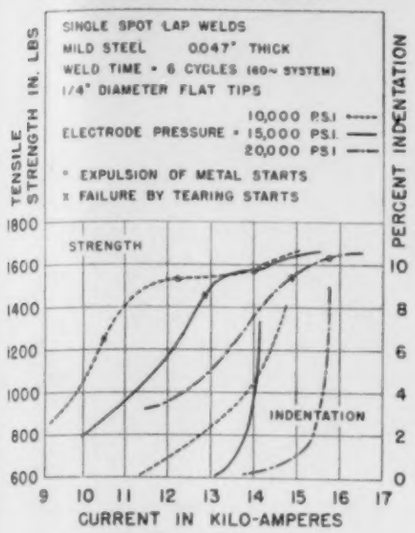


Fig. 2—Data from Mild Steel Welds Made in 12 Cycles with 1/4 Inch Diameter Tips at Three Different Electrode Pressures

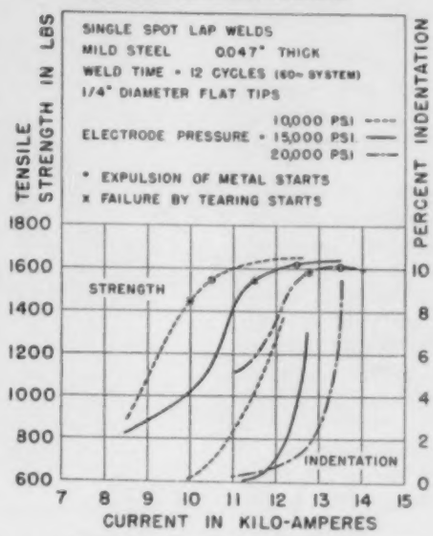
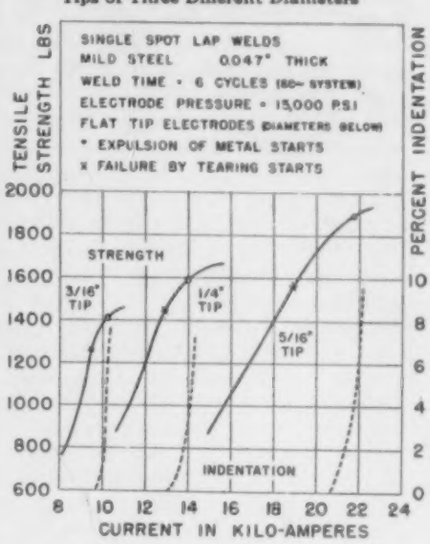


Fig. 3—Data from Mild Steel Welds Made in 6 Cycles at 15,000 Psi Electrode Pressure Using Tips of Three Different Diameters



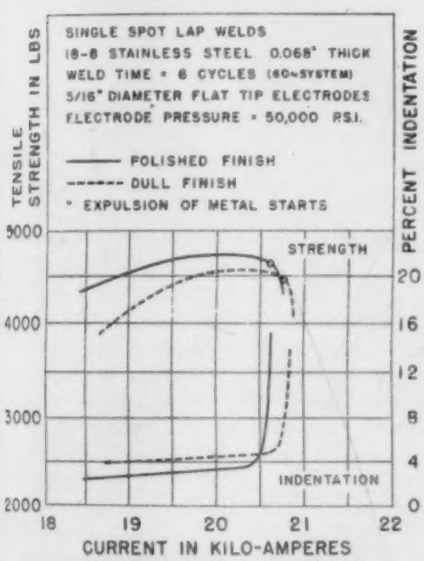


Fig. 4—Data from Stainless Steel Welds Made in 6 Cycles with 5/16 Inch Diameter Tips at 50,000 Psi Electrode Pressure

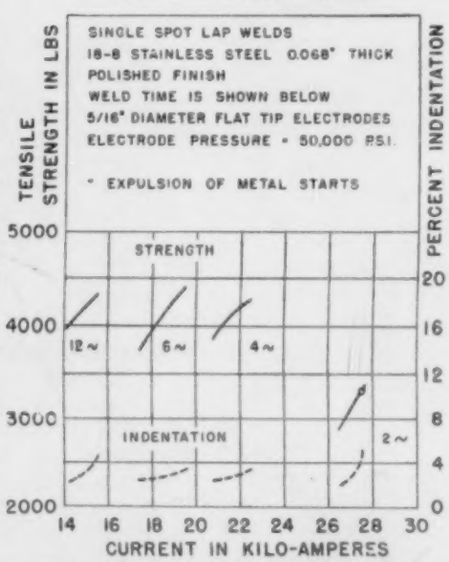


Fig. 5—Data from Stainless Steel Welds Made with 5/16 Inch Diameter Tips at 50,000 Psi Electrode Pressure in Four Different Times

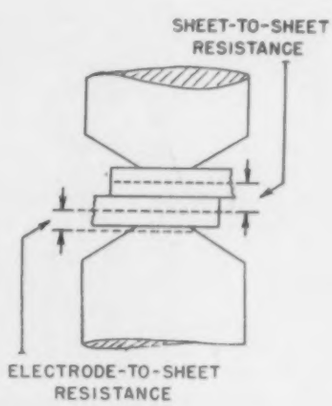


Fig. 6—Diagram Showing Planes Between Which Sheet-to-Sheet and Electrode-to-Sheet Resistances Are Measured

creases rapidly. A compromise has thus to be made between a small electrode, producing an undesirable type of failure, and too large an electrode, which is uneconomical. The best compromise for the mild steel described in this paper seems to be 1/4 inch diameter, although for comparison a size below and above this are shown. In the case of the stainless steel, a 5/16 inch diameter electrode tip was used, which is probably the minimum which would be satisfactory for this gage.

STRENGTH CHARACTERISTICS

Figure 1 shows tensile strengths of single spot lap welds using specimens 15/16 inch wide, with the weld made at the center of a one-inch overlap. During the course of these studies, it was found desirable to observe the point at which failure by tearing occurred and also, as in previous papers, the point at which expulsion of metal took place. Satisfactory currents for making welds at these various pressures will be obtained by selecting values between the point at which tearing starts and that at which expulsion of metal occurs. If an accurate measurement of current is not available, it will be noted that the indentation curves, plotted as a function of increasing current, show marked increase when expulsion of metal occurs. If, therefore, one were to make a series of welds using gradually increased heat until the indentation increased markedly, he could select a value just below that which gave the marked increase, and he would probably have the optimum value of current.

A pressure of 10,000 psi was found to give slightly lower strength, and less consistent and satisfactory operation, than could be obtained at higher pressure. Welds made at this pressure were frequently found to have internal cavities. Any slight surface irregularity might permit early expulsion of metal, and thus produce erratic results. Although the welds at 20,000 psi were slightly stronger than those made at 15,000 psi, this pressure was rejected because of undue surface distortion and separation of sheets produced by extrusion.

Figure 2 shows results for the same pressure conditions as shown for Fig. 1, the welds being made at 12 cycles rather than six. 15,000 psi was still the best choice for pressure for the reasons outlined above. It will be noted that the optimum currents are somewhat lower at the

longer time, being 13,500 and 12,000 amperes for the six- and twelve-cycle welds, respectively. Curiously enough these are the same values found optimum for welding the 0.029-inch mild steel described in the previous paper. This is probably accounted for by the greater proximity of the water-cooled electrodes in the case of the thinner gage, and consequent necessity for equal rate of heat input to offset the more effective cooling.

Figure 3 shows strength and indentation plotted as a function of current for three different sizes of flat electrode tips. The optimum currents, using six cycles and 15,000 psi, for the $^3/_{16}$, $^1/_4$ and $^5/_{16}$-inch tips will be approximately 10,000, 13,500 and 21,000 amperes, respectively. In the earlier papers it was noted that the optimum current using $^1/_4$-inch flat tips was double that required for $^1/_8$-inch, or proportional to the diameter. This was found true in the present work for the change between $^3/_{16}$ and $^1/_4$-inch tips, a 33% increase in diameter requiring a 35% increase in current. However, above $^1/_4$ inch the relation did not hold, current then being proportional to area, a 55% change in current accompanying the 56% increase in area. Looked at from another angle, the current density with the $^3/_{16}$-inch tips was a little over 360,000 amperes per square inch, whereas for the $^1/_4$- and $^5/_{16}$-inch electrode tips it was 275,000 amperes per square inch. It is logical to expect higher current density to be required for the smaller welds since the ratio of the area through which heat is dissipated to the volume being heated is greater for small welds than for larger ones.

From an economic viewpoint, in changing from $^3/_{16}$- to $^1/_4$-inch electrodes, we have used 35% more current and obtained 15% more strength. In changing from $^1/_4$- to $^5/_{16}$-inch electrode tips we have used 56% more current and obtained only 17% more strength. The calculated energies used between the electrode tips in the above cases are very closely proportional to the currents. It, therefore, does not appear economical to go to $^5/_{16}$-inch tips for the 0.047-inch mild steel. However, there is a very practical reason for the use of larger electrodes. Larger diameter electrodes widen the range of satisfactory current values, that is, the range within which the tear type of ductile failure will be obtained. For the $^3/_{16}$-inch welds, the range is 800 amperes, or 8.5%. For the $^1/_4$-inch welds, the range is 1150 amperes, or approximately 9%. For the $^5/_{16}$-inch welds, the range is 2850 amperes, or 15%. The diameter increase, for which there is a marked increase in the range of satisfactory operation, will be a function of the thickness, type and strength of the material, and of the strength of the welds obtainable therein. The practical significance of the above is that for the $^3/_{16}$- and $^1/_4$-inch electrode tips we could select an operating value which would permit a 5% drop in line voltage without producing any unsatisfactory welds. This situation would be obtained with a high-grade power supply. With the $^5/_{16}$-inch electrodes we could tolerate a 10% drop in line voltage.

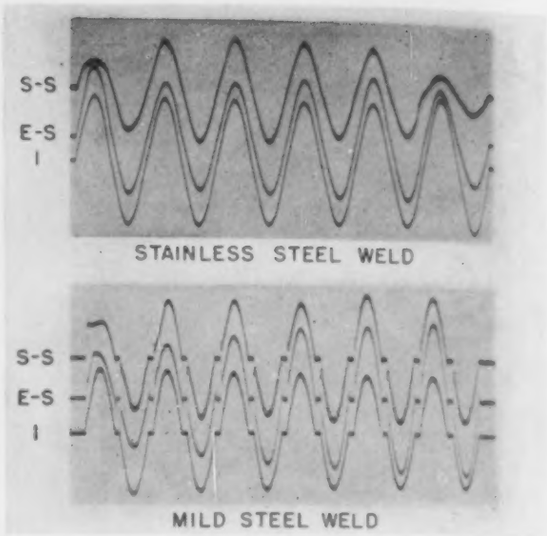


Fig. 8—Oscillograms Taken on Stainless and Mild Steel Welds

S-S—Sheet-to-sheet potential
E-S—Electrode-to-sheet potential
I —Potential across manganin shunt

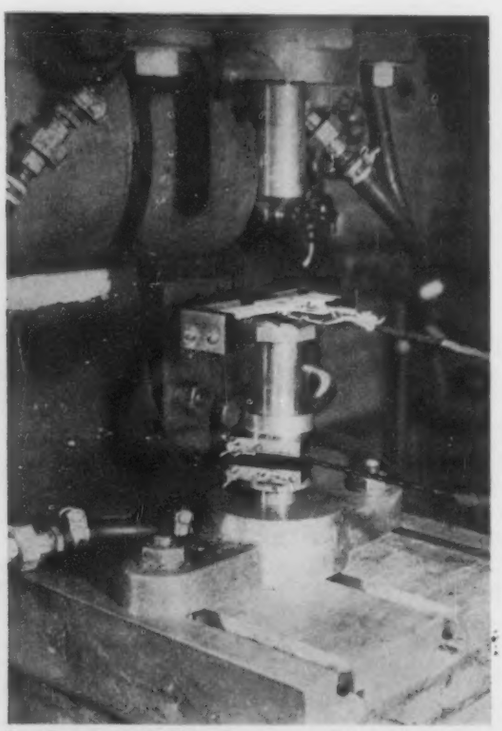


Fig. 7—Jig for Holding Specimens, Potential Connections to Sheets, Electrode and Manganin Shunt in Lower Electrode Holder

Figure 4 shows the tensile strength data for annealed 18–8 stainless steel, 0.068 inch thick, for both the dull and polished finish. The strength data for these curves were obtained with single spot lap welds, using two pieces of $1^1/_2$ x 5-inch stainless steel, welded in the center of a $1^1/_2$-inch overlap. The conditions shown in this curve are considered optimum, based on exploratory work done at 2, 4, 6 and 12 cycles, and using unit pressures of 30,000, 40,000 and 50,000 psi. Good work can also be done at 40,000 psi in this thickness with slightly less strength partially compensated by slightly less distortion when working at the highest values of current. In the case of stainless steel, expulsion of metal is accompanied by a definite loss in strength, which is probably accounted for by the serious cavitation and distortion which accompany expulsion of metal.

Figure 5 shows results of exploratory work at 50,000 psi using 2, 4, 6 and 12 cycles weld time. This exploratory work was done with $^{15}/_{16}$-inch wide specimens in order to save material, so that the strength values, while comparative, will be a few hundred pounds lower than if the $1^1/_2$-inch specimens had been used. The optimum currents will be the same in either case. The strength data indicate superiority for the six-cycle welds, and the photomicrographs, to be subsequently discussed, will show some of the reasons for the superiority.

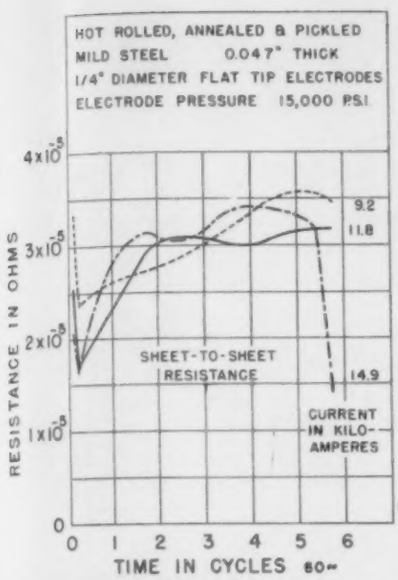


Fig. 9—Sheet-to-Sheet Resistance Characteristics for Mild Steel Welds

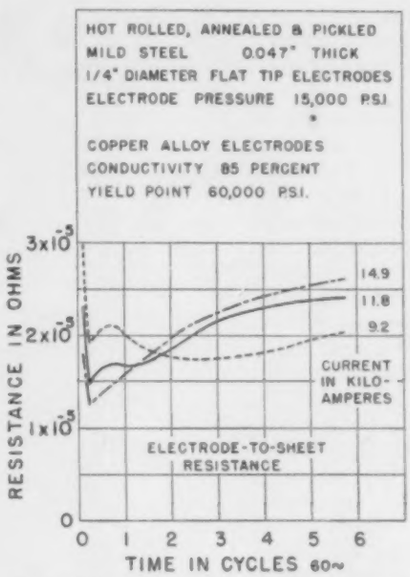


Fig. 10—Electrode-to-Sheet Resistance Characteristics for Mild Steel Welds

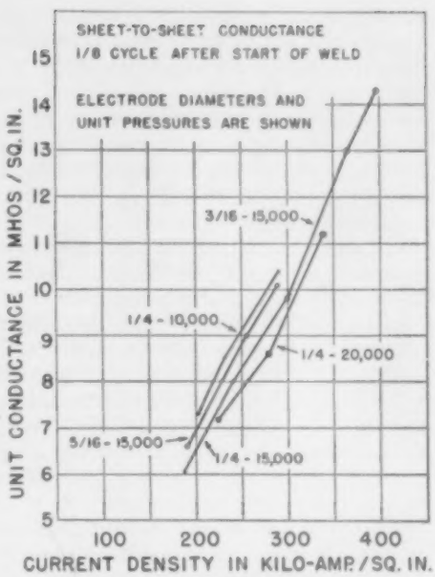


Fig. 11—Sheet-to-Sheet Conductance 1/8 Cycle After Start of Mild Steel Weld

RESISTANCE CHARACTERISTICS

The method of measuring sheet-to-sheet and electrode-to-sheet resistances has been discussed in a previous paper.[3] Attention should be called to the fact that the resistances measured are not simple contact resistances. The sheet-to-sheet resistance includes the contact resistance plus the resistance of one sheet of material. Likewise the electrode-to-sheet resistance includes the contact resistance plus the resistance of one-half sheet of material. This point is illustrated by Fig. 6. It is hoped that the component resistances can be determined in the near future. Figure 7 shows the latest method of supporting the specimen by means of a jig and also the latest method of connecting the oscillograph to the sheets, electrode and manganin shunt in the lower electrode holder.

Typical oscillograms of stainless and mild steel welds are shown in Fig. 8. For the mild steel weld the phase control was adjusted to give a little less than the maximum heat input. The abrupt rise of the sheet-to-sheet potential at the start of the mild steel weld is the typical effect of a surface film on the material being welded. Such an effect was not nearly so pronounced in the stainless steel weld. The abrupt drop in the sheet-to-sheet potential during the tenth half cycle of the stainless steel weld is typical of a weld in which metal is expelled.

Typical resistance characteristics for mild steel welds are shown in Figs. 9 and 10. Due to improvements in oscillographic practice, it has been possible to extend the characteristics further toward the start of the weld than was done in previous papers. Both the sheet-to-sheet and electrode-to-sheet resistances decrease very rapidly during the first quarter cycle and reach a minimum at about one quarter cycle after the start of the weld. It is in this period of the weld cycle that the intimate contacts are established. During the first quarter cycle of a weld, the resistance values tend to vary inversely with the r.m.s. current for a wide variety of welding conditions. This is well illustrated by the characteristics shown. The resistances rise as shown after the first quarter cycle and usually fall somewhat toward the end of the weld. The temporary rise in the electrode-to-

[3] "Studies of the Spot Welding of Low Carbon and Stainless Steels," loc. cit.

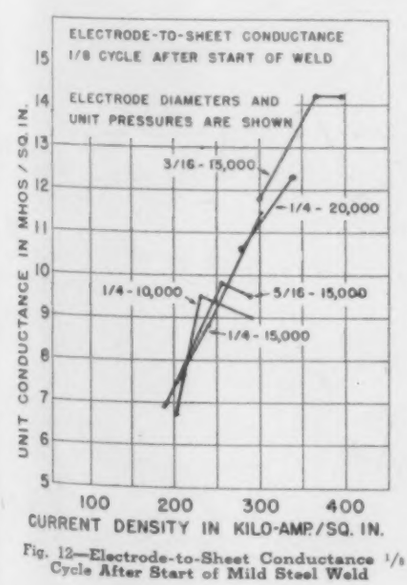


Fig. 12—Electrode-to-Sheet Conductance 1/8 Cycle After Start of Mild Steel Weld

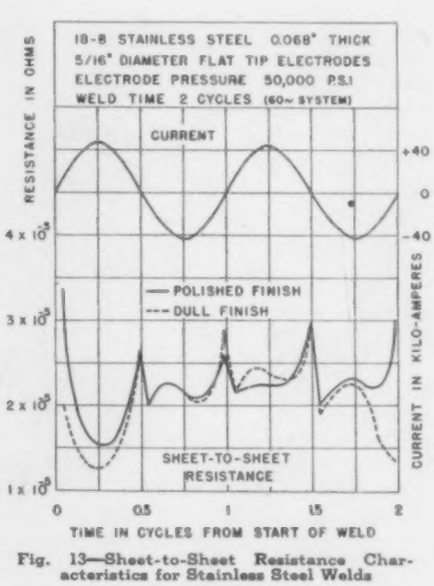


Fig. 13—Sheet-to-Sheet Resistance Characteristics for Stainless Steel Welds

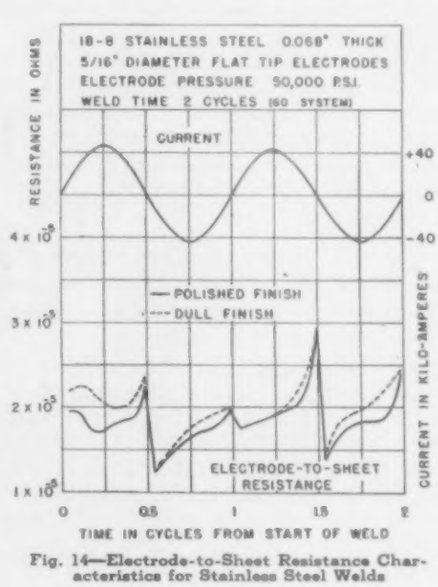


Fig. 14—Electrode-to-Sheet Resistance Characteristics for Stainless Steel Welds

sheet resistance during the second half cycle occurs frequently and is probably due to the fact that the electrode does not make its most intimate contact until then. The abrupt drop in the sheet-to-sheet resistance for the weld made at 14,900 amperes is typical of a strong weld from which metal was expelled. The weld made at 9200 amperes was very weak, while the weld at 11,800 amperes was intermediate in strength.

An initial attempt to analyze in detail several series of these characteristics for welds made under different electrode pressures and tip diameters reveals an interesting relationship. At a point one-eighth of a cycle after the start of the weld, both the sheet-to-sheet and the electrode-to-sheet conductance per unit of tip area tends to be a straight-line function of the r.m.s. current density. This relationship is illustrated by Figs. 11 and 12. This indicates that the speed at which the intimate contacts are attained is a similar function of the r.m.s. current density. Each conductance characteristic covers a range of welds from incipient fusion through the expulsion of metal. It will be noted that the electrode-to-sheet characteristics show a tendency to flatten out at the higher current densities. This probably means that at this point in the weld cycle the maximum intimacy of contact has been reached by the electrode, while between the sheets the contact area continues to increase with increase in current density. We believe that other relationships will be obtained when the analysis of the resistance characteristics is extended.

All of the resistance characteristics discussed previously were obtained by measuring the oscillograms at only the peaks and at a few additional points in the first quarter cycle. To obtain a more complete picture oscillograms were measured at more frequent intervals to obtain the resistance characteristics shown in Figs. 13 and 14 for two stainless steel welds. The sheet-to-sheet "film effect" at the start of the weld was much more pronounced for the polished steel than for the dull. After the first half cycle the characteristics for the two finishes tended to merge, except during the last quarter cycle when metal was expelled from the weld made with the dull stock. There was very slight evidence of a film effect between the electrode and sheet. This was probably due to the fact that the electrode face was relatively soft and the pressure high, which resulted in a good contact before the current started to flow. The most interesting feature about these characteristics is the drop in resistance during the period when the current is passing through zero. This probably means that cooling is taking place during these intervals, heat flowing into the electrodes and being absorbed by the cooling water. This indicates that it would be desirable to make some studies of the factors that may affect this cooling. Additional characteristics of this nature showing film effects, cooling, etc., are also being published in our paper on automobile grade mild steel.[4]

MICROSCOPIC EXAMINATION

Figure 15 shows photomicrographs of welds in 0.047-inch mild steel. Photograph *A* shows a good example of one of the optimum welds made in six cycles, at 15,000 psi. Note the interior columnar structure surrounded by the three heat-affected zones, which are typical for this material. Just outside the columnar structure, especially noticeable at the end, is the incipient fusion zone. Outside of this is the region of complete transformation, and at the very outside, adjacent to the unaffected metal, is the area of partial transformation. Evidence as to the nature of these areas was supplied in one of our previous papers.[5] Photograph *B* shows an optimum weld made in 12 cycles. Note that the columnar structure is more clearly defined than in the six-cycle weld. This has been observed to be a general characteristic of these weld times. Photograph *C* is a weld made in twelve cycles, using an electrode pressure of 20,000 psi. It was observed that at this pressure serious distortion and separation of the sheets occurred. The

[4] "An Investigation of the Spot Welding of Automobile Grade Mild Steel," loc. cit.

[5] "Studies of the Spot Welding of Low Carbon and Stainless Steels," loc. cit.

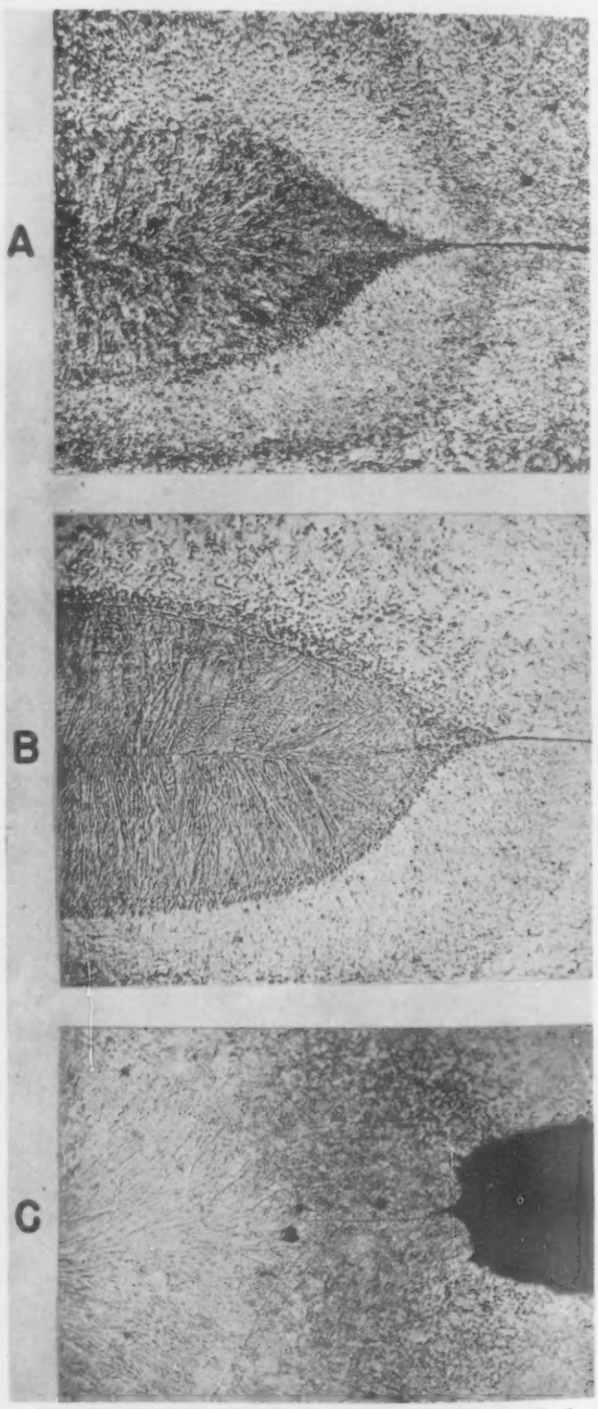


Fig. 15—Spot Welds in 0.047-Inch Annealed Low Carbon Steel
A—Characteristic heat zones around 6-cycle weld at 35×
B—Clearly defined structure of 12-cycle weld at 35×
C—Extrusion between sheets with 20,000 psi electrode pressure at 75×

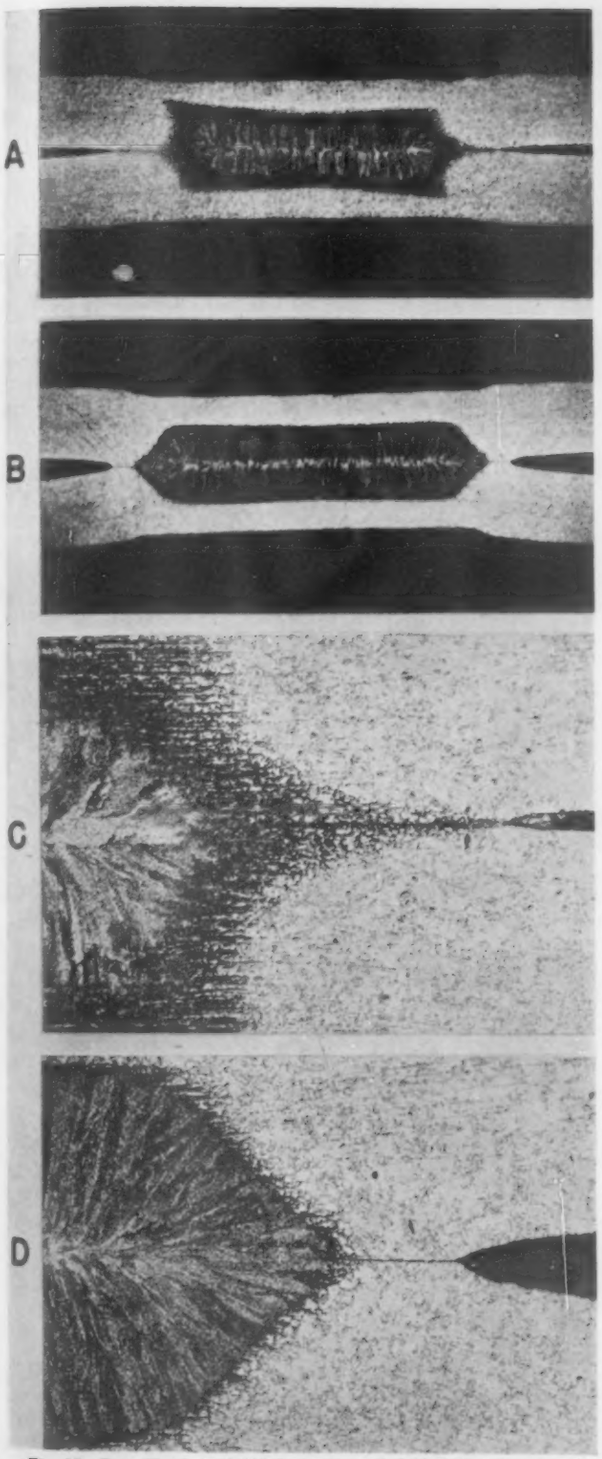


Fig. 16—Spot Welds in 0.068-Inch Annealed 18–8 Stainless Steel

A—Pronounced incipient fusion around smaller 2-cycle columnar weld at 7×
B—Characteristic 6-cycle weld selected to show mechanism of sheet separation at 7×
C—End of weld A showing heat zone and trace of expelled metal at 35×
D—End of weld B at 35×

nature and cause of this distortion, which occurred without any expulsion of metal, is shown in this picture. The metal in the completely transformed zone, the region of plastic austenite, has been extruded by the high pressure between the sheets, causing them to separate.

Figure 16 shows welds made in polished 18–8 stainless steel in two and six cycles. Photograph *A* shows a two-cycle weld with a pronounced zone of incipient fusion surrounding the columnar nugget. Photograph *B* shows a weld made in six cycles, with a much more clearly defined columnar weld. These photographs were made with a magnification of 7×. Photographs *C* and *D* show the ends of the same welds photographed at 35×. The two-cycle weld required 27,500 amperes. It was accompanied by a slight expulsion of metal, a trace of which is visible at the right end of photograph *C*. This was the largest weld that could be made in two cycles, without violent expulsion of metal. Note how much smaller it is than the corresponding weld made in six cycles, at the same pressure and with the same size electrode tips, which were $^{5}/_{16}$ inch in diameter. This gives a good reason why the two-cycle welds are considerably less strong than those made in six cycles. These welds were made in $^{16}/_{16}$-inch width material, and the six-cycle weld here shown was selected as illustrative of a different method of distortion than was found in mild steel and illustrated in photograph *C* of Fig. 15. In this case the distortion is evidently not due to extrusion, but to "beam action." Each sheet acts as a beam loaded at the center by the electrode, free to deflect at the center because of the molten weld metal, but supported at the end of the molten section by contact with each other. A certain distance beyond the end of the weld the sheets deflect away from each other as the electrodes force the centers together.

Figure 17 illustrates some interesting welds made in 0.068-inch, cold-rolled, 18–8 stainless steel. Photograph *A*, at 35×, shows a weld made in two cycles, which is entirely non-columnar, and had the excellent strength of 4320 pounds. A current of 26,100 amperes was used to make the weld. The relatively high initial contact resistance and the high current used must have caused an initial explosion of surface metal. A trace of the expelled metal is visible at the right of the picture. A slight flash of light from between the sheets was observed during the making of this weld, further evidence of the surface explosion. The weld failed by ductile shearing. Photograph *C* shows the center of this weld at 750×, and indicates the extremely fine grain of the weld metal. The explanation of this type of weld is based upon the fact that recrystallization may occur in transformationless metals, upon the application of heat following cold work. In stainless steel, therefore, a weld of this type should only be observed in cold worked material. These welds were made accidentally on cold worked material included in a shipment to us of annealed stainless steel. The reason for the anomalous behavior of these welds, made in dull finish material, requiring lower currents and having somewhat higher strengths than anticipated from other work, was detected by means of microscopic examination. Photograph *B* of Fig. 17 shows a similar weld made in six cycles, requiring 16,500 amperes, having a strength of 4040 pounds and a beautiful columnar structure.

RESULTS AND CONCLUSIONS

The results of the work described in the present paper fully substantiate those enumerated at the end of the October 1938 paper, loc. cit. To avoid repetition, the reader is referred to pages 47 and 48 of that paper. In addition to those cited we should like to make the following observations:

1. The diameters of the best welds in the 0.047-inch mild steel approach very closely that of the electrode tips. In the case of the strongest welds in the 0.068-inch stainless steel the fused sections were approximately 8% larger in diameter than the electrode tips.

2. The importance of securing good fusion in annealed mild, and annealed stainless steel, in order to ob-

tain consistent high strength, was evident from the results of this work.

3. In general welds made with too low current, resulting in the formation of no well-defined fusion, or only a small such area, failed by a brittle shearing between the sheets. On the other hand, welds made with proper current failed by a ductile tearing at the stress concentration point. The structural advantage of a ductile type of failure is well recognized.

4. The selection of the proper size of electrode tip for a given material is very important, in order that considerations of economy and a reasonably wide range of satisfactory operating current may be obtained.

5. The analysis of resistance characteristics, sheet-to-sheet and electrode-to-sheet, yields interesting results and information concerning what goes on during the making of a spot weld. Calculations of energy input to welds are readily obtainable from the results of resistance data.

6. The use of the microscope in connection with this type of investigation shows reasons for variations in strength, anomalous behaviors and distortion, and insight into the nature of the weld produced.

ACKNOWLEDGMENTS

Thanks are again due to:

Mr. F. T. Llewellyn and the United States Steel Corporation, whose generous supply of stainless and plain carbon steels enabled continuation of the work during the present year.

Mr. John D. Tebben and P. R. Mallory and Company for electrode materials.

Dr. C. A. Adams, Chairman of the Welding Research Committee, for personal advice in the conduct of the work and the financial support of his committee in providing research funds which made possible the support of a research fellow for carrying on this work.

Mr. H. M. Hobart, Chairman of the Fundamental Research Division, to whose division this paper is a contribution, for his cooperation and support of this work. After examining this manuscript, Mr. Hobart made the following helpful comments: "While the quantitative results obtained with various materials and conditions provide us with additional empirical data which will be of immediate practical use in design, we believe that the most valuable results obtained are the fundamental truths learned by the exploratory nature of the research. Attempts were made to improve every occasion to learn the reasons for the phenomena observed, particularly those which were unexpected and, at first, inexplicable. Dr. Adams repeatedly counsels this fundamental approach in research. In this instance, as, in fact, is almost always the case, our added knowledge in the form of fundamental truths will, in due course, far outweigh in value (as a contribution to the advancement of welding) the nevertheless very useful empirical data which were obtained, and which are immediately applicable in design."

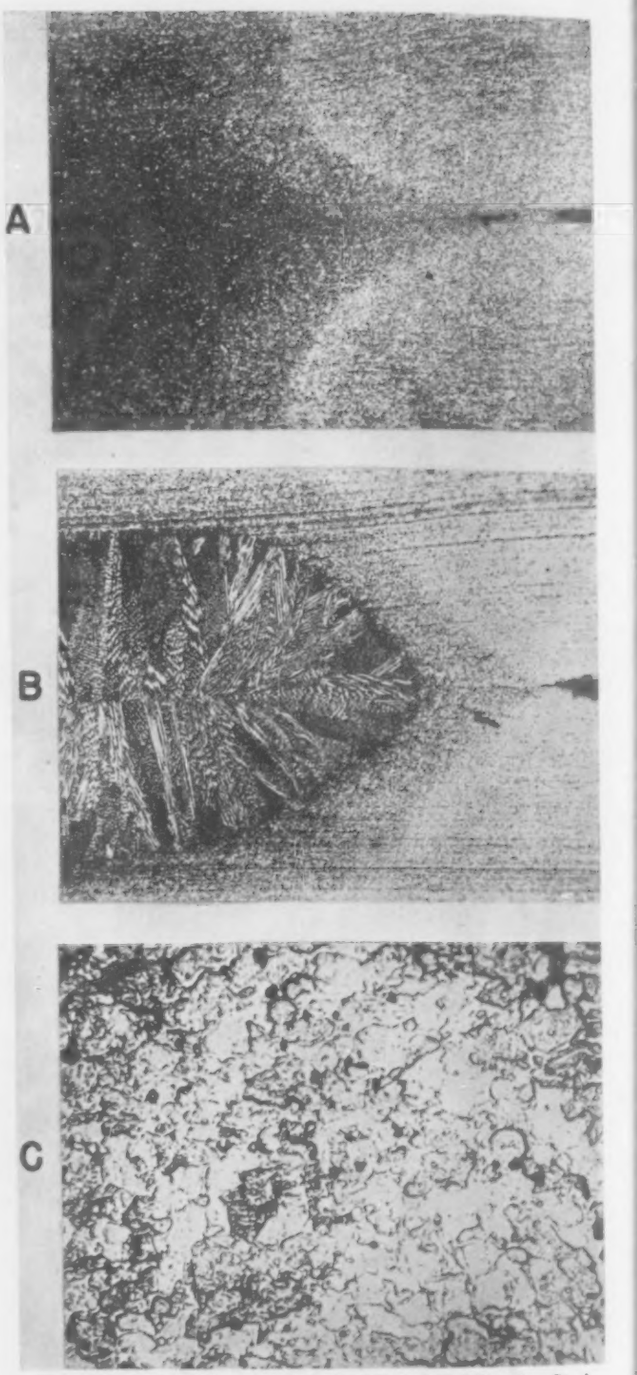


Fig. 17—Spot Welds in 0.068-Inch Cold Rolled 18-8 Stainless Steel
A—Non-columnar, recrystallized, 2-cycle weld at 35×
B—Beautiful columnar weld made in 6-cycles at 35×
C—Extremely fine recrystallized grains at center of weld A at 750×

DESIGNING WELDED FRAMES FOR Continuity*

By BRUCE JOHNSTON† and E. H. MOUNT‡

SYNOPSIS

THIS paper presents a procedure for the direct design of beams in welded building frames to take partial advantage of the reduction in maximum moment afforded by moment-resisting, semi-rigid connections. The aim has been to develop a direct and simple design procedure which takes into consideration the behavior of the frame as well as the connection. The average saving in the weight of beams is fifteen per cent or more and the type of construction is applicable for floor loads specified for apartment house and office building construction with a maximum beam length of about twenty feet. The actual economy effected is less than the saving in weight because heavier welds are needed in the connection details to produce the required end restraint.

Previous research on the design of connection details is reviewed and design procedures and charts developed for each step in the design. The results of tests of two full-sized welded building frame bents are presented to illustrate the agreement between test results and the methods of theoretical analysis on which the proposed design methods are based.

INTRODUCTION

The present investigation is one of several which have been sponsored by the Structural Steel Welding Committee of the American Welding Society at the Fritz Engineering Laboratory of Lehigh University. In September 1938, the Committee authorized the authors to proceed with a program involving the construction and test of two full-size model building frames using the type of beam-column connection previously developed by research at the Fritz Laboratory.[1,4,5]** Additional funds amounting to $600 were furnished by the Committee to cover the cost of constructing the frames and purchasing loading tanks. The Committee also requested that the authors review and correlate previous research on beam-column connections which had been made at the Fritz Laboratory under the direction of Dr. Inge Lyse, now Professor at the Norges Tekniske Hoiskøle, Trondheim, Norway.

The following presents a brief review of recent investigations which are related to the present program. In February 1935, Inge Lyse and Norman G. Schreiner discussed welded seat angle design[1] and in June of the same year Heath Lawson[2] suggested a design procedure. These investigations considered the design of end connections to resist only the vertical reaction. In January 1936, Wilbur M. Wilson[3] presented the results of tests of beam-column connections designed for as nearly complete rigidity as possible. In October of 1936 and 1937 Inge Lyse and Glenn Gibson[4,5], presented two successive reports which correlated test results with a design method applicable to beams with semi-rigid connections which were assumed to frame into fixed walls or non-bending columns. Inge Lyse and E. H. Mount[6] later presented a paper studying the stresses produced in the columns by this type of connection. The whole question of analysis and design of building frames, taking into account the existing continuity, has been studied by the Steel Structures Research Committee of Great Britain[7] in connection with tests of riveted building frames. The present authors in an as yet unpublished paper[8] have discussed in detail both the slope deflection and moment distribution methods of analysis as applied to frames with semi-rigid connections.

The present investigation is not presented as any final answer to the question of designing buildings to take account of partial continuity, but rather as a pilot investigation which shows that feasible methods for the design of beams in such frames may be developed. Much work remains to be done in the testing of other type joints in order to make design procedures similar to that proposed applicable to a wider range of beam sizes.

ACKNOWLEDGMENT

The investigation was sponsored by the Structural Steel Welding Committee of which Mr. Leon S. Moisseiff is Chairman and Mr. William Spraragen, Secretary. Acknowledgment is made to all members of the Committee for their assistance and interest in the program. The investigation was a regular research project of the Fritz Engineering Laboratory, of which Professor Hale Sutherland, Head of the Department of Civil Engineering, is Director.

Acknowledgment is made to Mr. Howard J. Godfrey, Engineer of Tests, and to all others on the laboratory staff for their assistance and interest in carrying out the program.

THE GENERAL PROBLEM

In the past decade there has been rising interest among engineers toward the use of continuity in structures. Fully continuous steel girders and rigid frames, both riveted and welded, have been built in increasing numbers both in this country and abroad. Engineers who have designed beams in building frames have also been conscious of the fact that partial continuity exists to a greater or less degree in all building frames, whether taken account of in design or not.

If the beam-column connections of a building frame are designed to transmit bending moment without allowing any relative rotation between the end of the beam and

* To be presented at Annual Meeting, A. W. S., Chicago, Oct. 23 to 27, 1939.

† Assistant Director, in charge of research, Fritz Engineering Laboratory, Lehigh University, Bethlehem, Pennsylvania.

‡ Formerly American Welding Society Research Fellow, Lehigh University, now with Dravo Corporation, Pittsburgh, Pennsylvania.

** These numerals refer to references at the end of the article.

column the connection may then be termed "rigid" and the resulting structure is fully continuous. The connections in such a case are said to afford one hundred per cent restraint. In the usual building frame the connections provide less than one hundred per cent restraint and there is a relative rotation between the beam and column when moment is transmitted. Such a structure might be said to be "semi-rigid," "semi-continuous," or simply termed "a frame with moment resisting connections." The latter term would apply equally well to the so-called "rigid frame."

Rigid frame construction with one hundred per cent restraint in the connections does not afford the greatest possible economy in building construction for two reasons:

1. The welded details to provide a connection with one hundred per cent restraint are more costly than those providing between fifty and seventy-five per cent restraint.
2. Except for the case of a concentrated load in the center of a beam the potential economy in beam

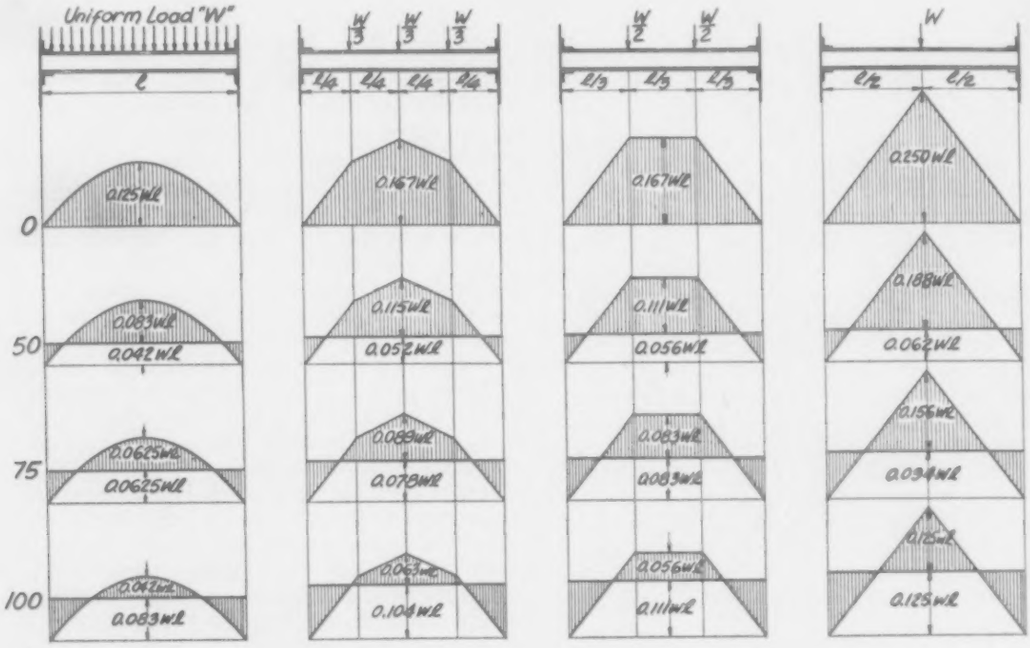


FIG. 1 CENTER AND END MOMENTS FOR BEAMS WITH VARIOUS AMOUNTS OF CONNECTION RESTRAINT FRAMING INTO FIXED WALLS.

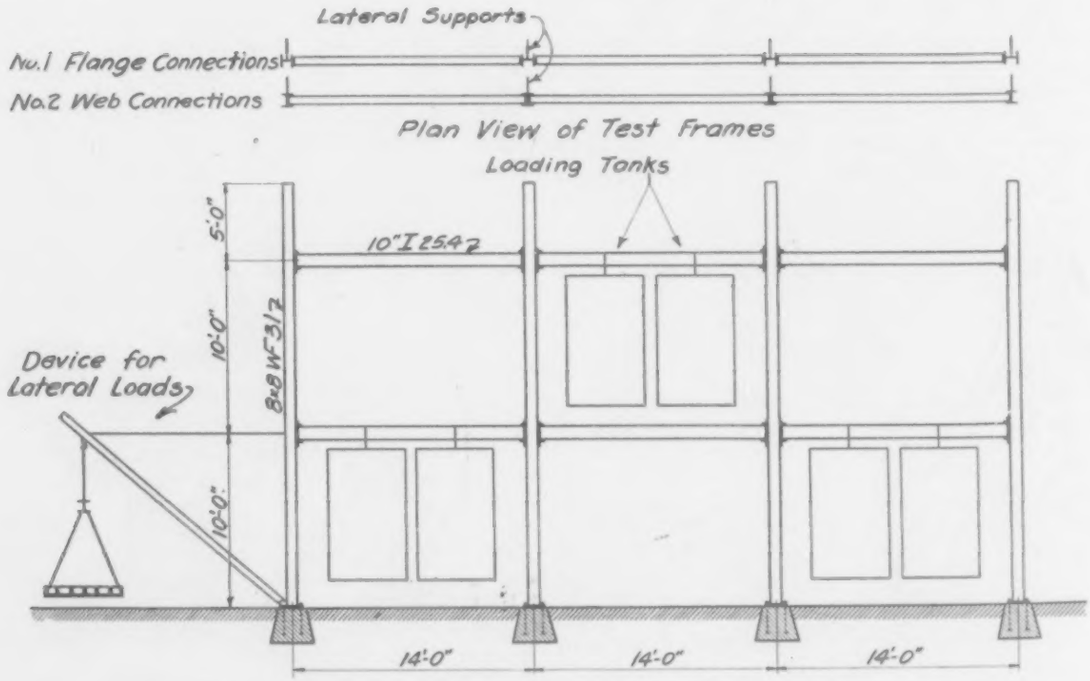


FIG. 2 GENERAL DIMENSIONS OF TEST FRAMES

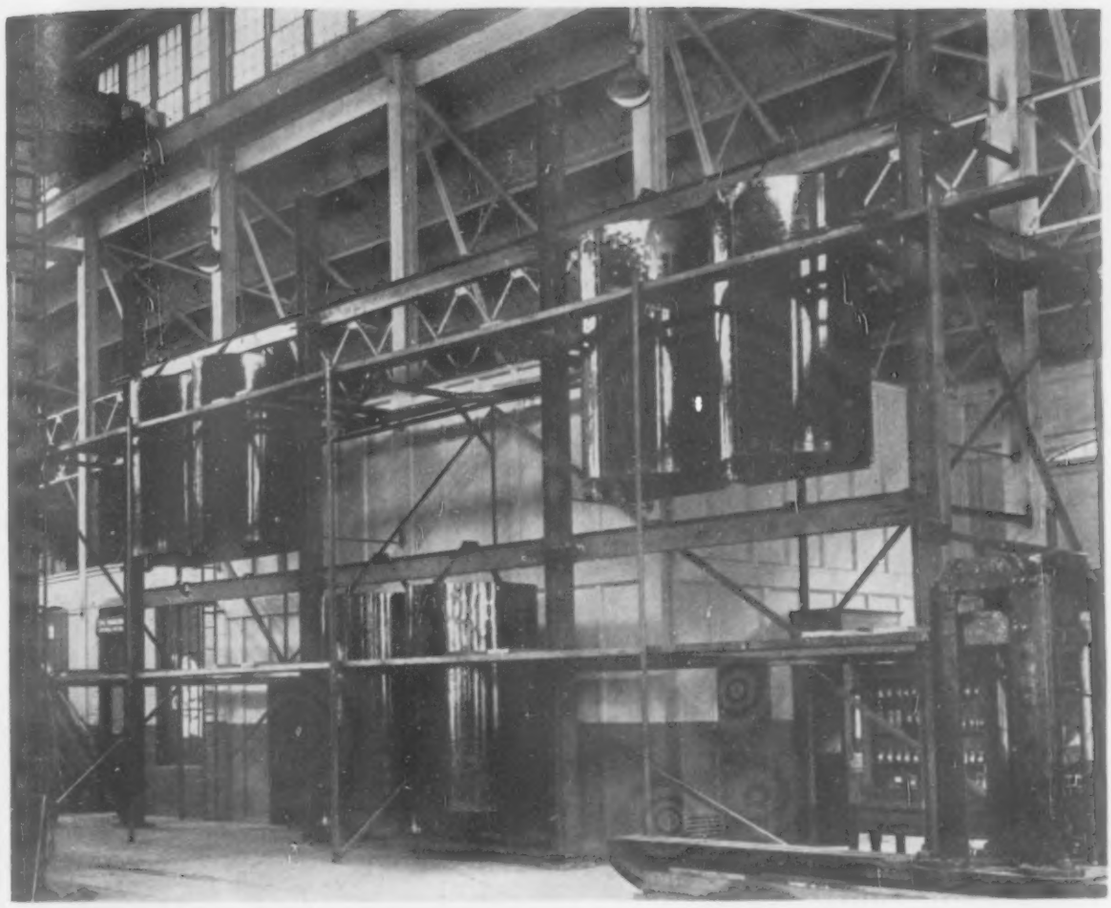


Fig. 3—Frame with Loading Tanks in Position

design is greater for a connection restraint of seventy-five per cent than for one hundred per cent.

Figure 1 illustrates several different loading conditions, showing the center and end moments for 0, 50, 75 and 100 per cent restraint in connections and with columns assumed not to bend. It will be noted that the moment in the center of the beam is always critical for any degree of restraint between 50 and 75 per cent and for any type of loading. Above 75 per cent the moments at the ends of the beam become critical. It should also be noted that if the beam is designed for the center moment, with connections having 50 per cent restraint, the beam itself will not be overstressed for any variation of restraint between 50 and 100 per cent.

In an actual building frame the conditions illustrated in Fig. 1 do not exist because the columns are not rigid but are free to bend. The bending of the columns will reduce the end moments to some extent and increase the center moments. The problem of design of the beams depends therefore not only upon the degree of restraint provided by the connections but upon the general behavior of the building frame as well.

TEST OF THE MODEL BUILDING FRAME

In order to study the general behavior of welded building frames and to correlate this behavior with methods of analysis and design, two full-size model building frames were constructed. Methods of analysis had already been developed[7,8] and it was desired to corroborate these by experimental test results on welded building frames so that the analytical methods could then be used to develop design procedures. A similar procedure was used in tests of riveted building frames in Great Britain.[7]

Frame No. 1 is illustrated by the line drawing in Fig. 2 and by Fig. 3, which shows a photograph of the frame with loading tanks in position. The 8 by 8-in. W.F. 31.0-lb. column sections were welded in a vertical position to 16 x 16 x $1^1/_2$- in. base plates. Cement grout was run between the base plates and piers and the anchor bolts were drawn tight after the grout had set up. The piers were flush with the floor, measured 1 ft. 6 in. x 1 ft. 6 in. at the top, and pyramided to a 2 ft. 6 in. x 2 ft. 6 in. size at the base. They were 2 ft. high and had four 1-in. diameter pipes cast in place to take removable $^3/_4$-in. diameter anchor bolts 18 in. in length. The beams were 10-in. I's at 25.4 lb. placed upon 4 x 4 x $^1/_4$ x 6-in. seat angles which had been welded to the columns with $^1/_4$-in. fillet welds along each of their 4-in. legs prior to erection of the columns. The design method previously proposed by Lyse and Gibson[4,5] was used in proportioning the top angles which were cut 3 x 3 x $^{11}/_{16}$ x $4^3/_4$ in. long from $3^1/_2$ x $3^1/_2$ x $^{11}/_{16}$ in. original size. The top angles were welded to the top flange of the beam and to the column flanges with $^{11}/_{16}$ in. fillet welds. The lower flanges of the beams were welded to the seat angles on each side with $^1/_4$-in. fillet welds $3^1/_2$ in. long, designed to take the thrust developed by the moment at the end of the beams. A qualified welder using Grade 10 Electrode was engaged for all of the welding.

Frame No. 2 was similar in general dimensions and details of design except that the columns were turned 90° with respect to their position in Frame No. 1. The beams were thus connected to the column webs rather than to the column flanges.

Each frame was braced laterally near each joint by

means of flexible ties welded between columns of the frame and columns of the laboratory. The lateral ties were 1-in. diameter bars with section reduced near each end to $^1/_4$-in. diameter for a length of $^1/_2$ in. The frame was in this way braced against movement out of its own plane but was at the same time free to bend or move in the plane of the frame.

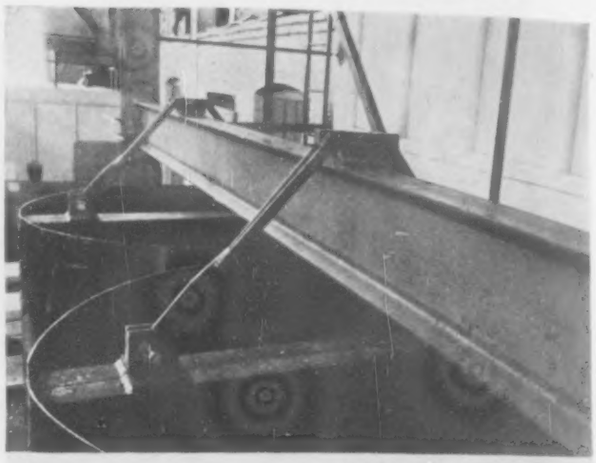
Vertical loads were applied to the beams of the completed frame by means of water tanks which are shown in Figs. 3 and 5. Lateral load was applied in one test only by means of the outrigger device shown in Fig. 4.

The six loading tanks, which are shown in one loading position in Fig. 3, were of $^3/_{16}$-in. welded plate construction. Each tank was 4 ft. 6 in. in diameter, 7 ft. 6 in. deep, and had a capacity of 7000 lb. of water. Both inlet and outlet was effected by means of 2-in. diameter valve and hose connections. A load increment of 6500 lb. was used in tests and a $^1/_2$-in. round steel rod welded vertically in the center of each tank was marked at points of initial and final load. The tanks were calibrated by running the water into auxiliary weighing tanks placed on platform scales of 1000 lb. capacity. Each tank was hung in position on the beams of the frame by means of welded hangers shown in Fig. 5. The removable pins which may be seen in Fig. 5 allowed rapid change of the tanks to different loading positions.

Prior to building the frames the beam and column sections were tested individually in pure bending to determine experimentally their coefficient of bending stiffness, $E \times I$. The size and thickness of material in beams and columns was also checked by micrometer calipers and was found to agree closely with handbook values.

The experimental determination of the moments in the beams and columns of the actual frame was made by measuring the absolute rotation of the ends of each beam, and of the columns at the joint centers at each floor level. The rotations were measured by the 20-in. level bar illustrated in Fig. 6, in position on one of the rotation bars. The level bar was sensitive to a rotation of $\pm\,^1/_{200,000}$ of a radian or $\pm$ 1 second, and consisted of a 10-second precision level bubble mounted on an aluminum bar. Two sharpened steel points supported the bar at one end and the other end was supported by a micrometer screw which was used to bring the bar to level position for each reading. The elevation of the micrometer end of the bar was read by a $^1/_{10,000}$ Ames dial. The dial movement divided by the gage length gave the relative rotation in radians between any two load conditions. For each measurement the level bar rested in an identical location upon the polished surface of rotation bars which were attached by arms to the gage stations of the frame. These bars are shown in position in Figs. 7*a*, 7*b* and 7*c* for an interior joint, a column base and for an exterior joint, respectively. Four $^1/_{1000}$ Ames dials as shown in Fig. 7*c*, measured the lateral movement of the frame at each story level for the two exterior columns.

Fig. 5—Tanks for Applying Vertical Loads



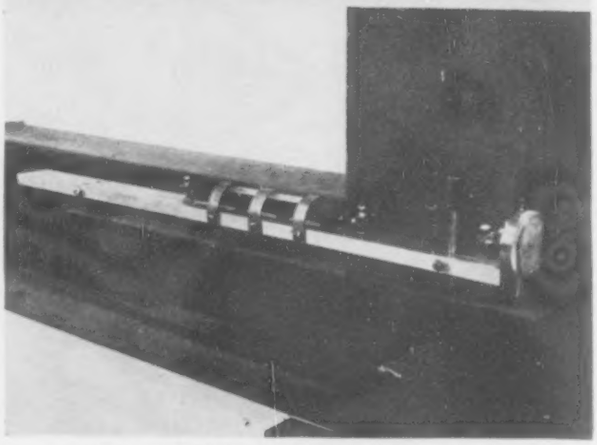


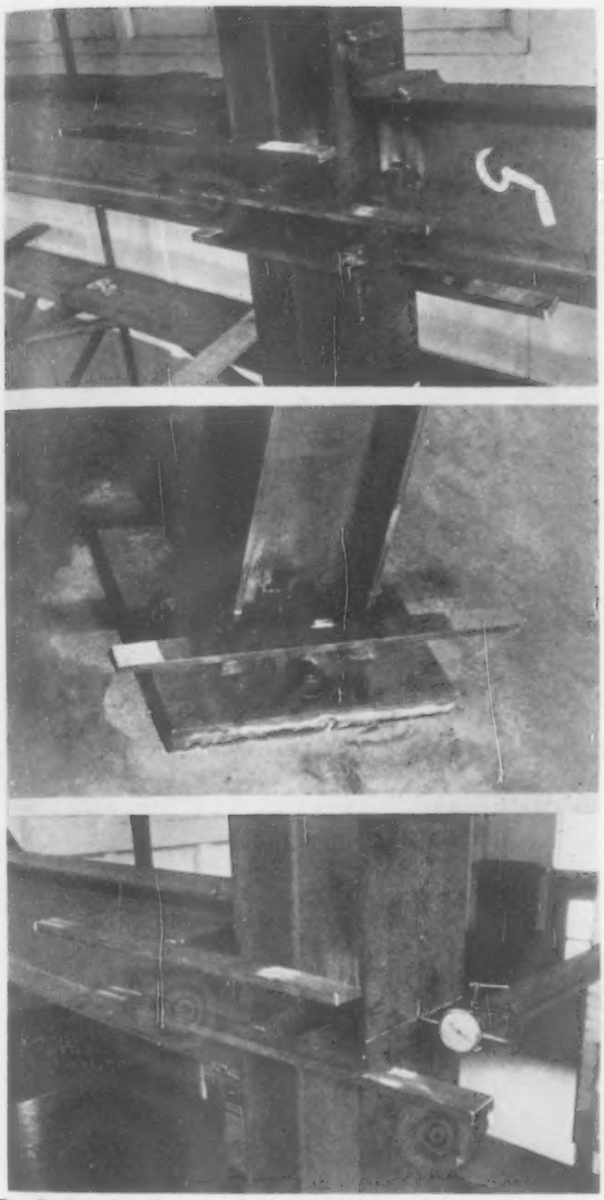
Fig. 6—Level Bars for Measuring End Rotations

Fig. 4—Outrigger Device for Applying Lateral Loads

The moments at the ends of each beam and at the joint centers of each column were computed directly from the well-known slope deflection equation:

$$M_{AB} = \frac{2EI}{l}\left(2\Theta_A + \Theta_B - \frac{\Delta}{l}\right) \pm M_{FR} \qquad (1)$$

Θ_A and Θ_B were the measured rotations at the near and far end, respectively, of any particular member. Δ was the relative lateral movement between the ends of a member, and was equal to zero for symmetrical loading conditions. The quantity EI was experimentally determined by the method previously explained. M_{FR} is the



Figs. 7a, b and c—Rotation Bars for an Interior Joint, a Column Base and an Exterior Joint

computed end moment in the loaded beams due to applied loads assuming rigid connections and fixed ends.

Direct strain readings were also made in the beams and columns by means of 10 and 20-in. Whittemore strain gages. While these checked reasonably well with calculated strains the results were somewhat erratic because of the small increments of measured strain. The rotation readings were accurate, sensitive, and gave very consistent results.

As a basis for the theoretical analysis of the frame by the method of moment distribution it was necessary to evaluate the relation between moment and angle change for the beam-column connection used in this particular frame. Three sample joints were therefore constructed identical with those used in the frames. Figure 8*a* shows the test set-up for the beam-to-column flange connection used in Frame 1, and Fig. 8*b* shows the test set-up for the exterior beam-to-column web connections, in which case the column elements acted independently to allow free deformation of the web. A third joint for interior beam-to-column web connections was similar to Fig. 8*a*. The relative rotation between the ends of the beam and the center of the column at the joint were measured with the 20-in. level bar in the same manner as on the test frames. The relation between moment and angle change at the connection is denoted by the Greek letter "γ," and termed the "connection constant."[7]

$$\gamma = \frac{\varphi}{M} \tag{2}$$

In equation (2) φ is the relative angle change between the end of the beam and the column at the joint center. γ may be defined as "angle change for unit moment."

Another constant α, relates the constant γ with any particular beam section and length used in an actual frame:

$$\alpha = \frac{2EI\gamma}{l} \tag{3}$$

In equation (3) I and l are the moment of inertia and length between connections, respectively, of any particular beam.

The method of theoretical analysis is developed elsewhere[7,8] but test results will be presented to show the agreement between theoretical and experimental values. Figure 9 shows the results of connection tests to determine γ, which is the inverse slope of the experimentally determined straight line curves. Because of the possibility of reversal of moment in the frame when loaded laterally the joints were loaded under both normal and reversed bending up to full design load. The dashed line in Fig. 9 represents a theoretical relationship in which all of the deformation is assumed to take place in the top angle. It is noted that the agreement is fairly good for the beam-to-column flange connections. The

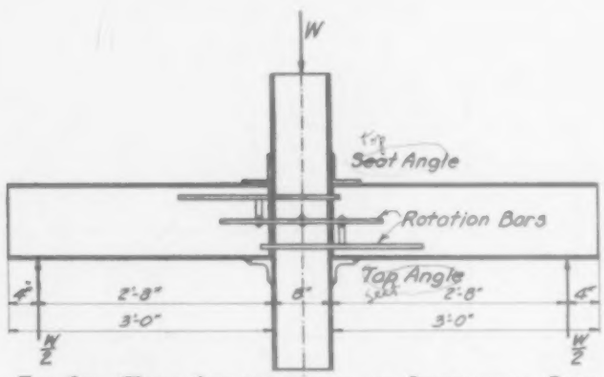


FIG. 8a TEST ARRANGEMENT TO DETERMINE BEAM-COLUMN FLANGE CONNECTION CONSTANTS

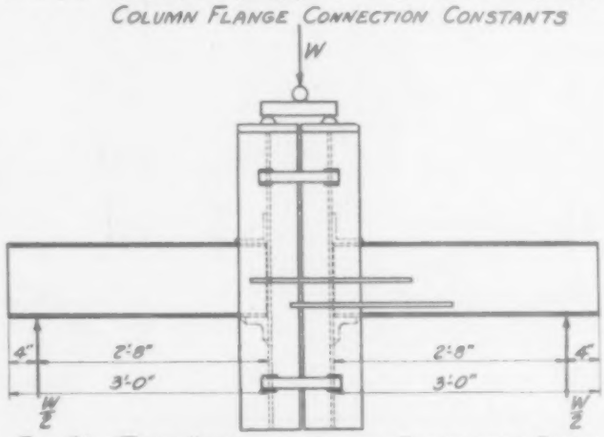


FIG. 8b TEST ARRANGEMENT TO DETERMINE BEAM-EXTERIOR COLUMN WEB CONNECTION CONSTANTS

agreement is excellent for the double web connection on an interior column but the connection for an outside web connection tested as shown in Fig. 8*b* has much greater flexibility in normal bending than the other connection types. This is believed due to the distortion in the web and may be prevented by welding reinforcing plates on the outside columns opposite the connections.

Using the experimentally determined values of γ as determined from Fig. 9 the method of moment distribution was used to analyze the frames for the various loading conditions used in the test.[7,8] Width of member was taken account of in the analysis. Figures 10 to 14, inclusive, show both the calculated and experimentally determined moments for several of the critical conditions of loading applied to Frames 1 and 2.

Figure 10 shows the moments in Frame 1 with all of the first floor beams loaded and the second floor beams unloaded. The close agreement between experimental results and analysis based on an experimental test of a typical connection should be noted. Figure 11 shows the test results for an unsymmetrical loading in which only one outside second story beam was loaded. Sidesway was present to a small degree but was neglected in the analysis. The agreement between theory and test is excellent and the rapidity with which the moments taper out away from the region of the loaded beam shows that only a local region of a building frame need be considered in analyzing any critical condition of loading. The analysis assuming completely rigid joints is also shown by the dash-dot line in this figure to show the divergence from experimental results and also to indicate the greater extent to which moments would be transferred in a rigid frame to sections remote from the loaded beam. Figure 12 is presented to show the maximum probable divergence between theory and test for a critical condition of loading. In this test the order of loading was purposely unbalanced, but the final loading showed fair agreement between theory and test. A similar test with loading maintained balanced during application resulted in much better agreement, however.

Figures 13 and 14 present the results of tests on Frame 2, with web connections. The difference in behavior between outside column connections and inside column connections was taken account of in the analysis and a very close agreement with test results is noted for each condition of loading.

All of these tests show that theoretical analyses which depend in part on a separate test of a sample joint agree well with experimental test results of a full size frame. The methods of analysis are therefore satisfactory as a basis for the development of design methods.

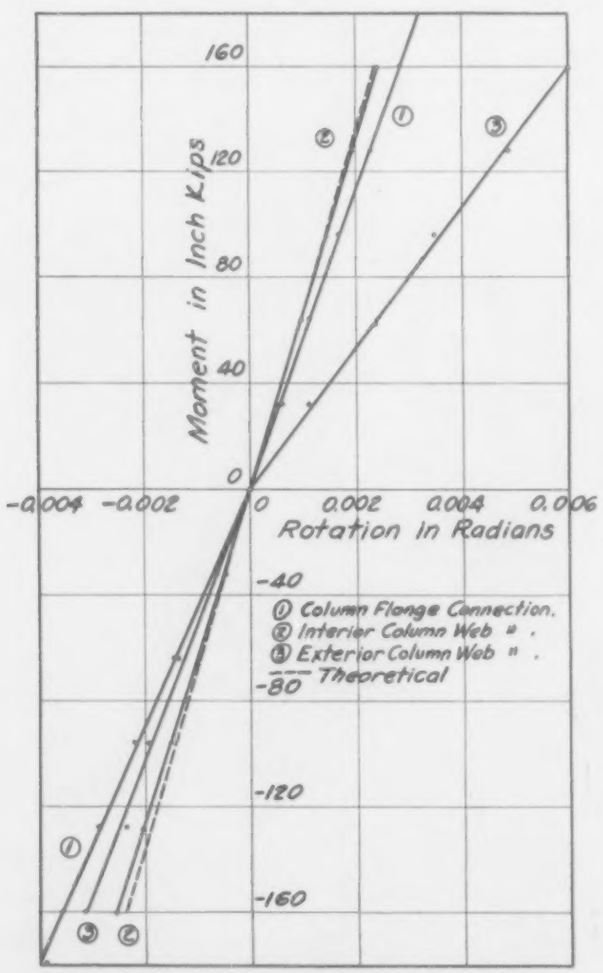


FIG. 9 TEST RESULTS OF CONNECTIONS USED IN WELDED BUILDING FRAMES

RELATION OF TEST RESULTS AND ANALYSIS TO DESIGN

The design procedures here developed are based on the results of both the present and former tests at the Fritz Laboratory.[1,4,5,6] The problem of design will be treated under the following separate headings.

1. Seat Angle Design.
2. Design of Top Angle Connections for Partial Restraint.
3. Design of Beams.
4. The Problem of Column Design.

The order of the preceding topics is the reverse of that to be followed in an actual design procedure.

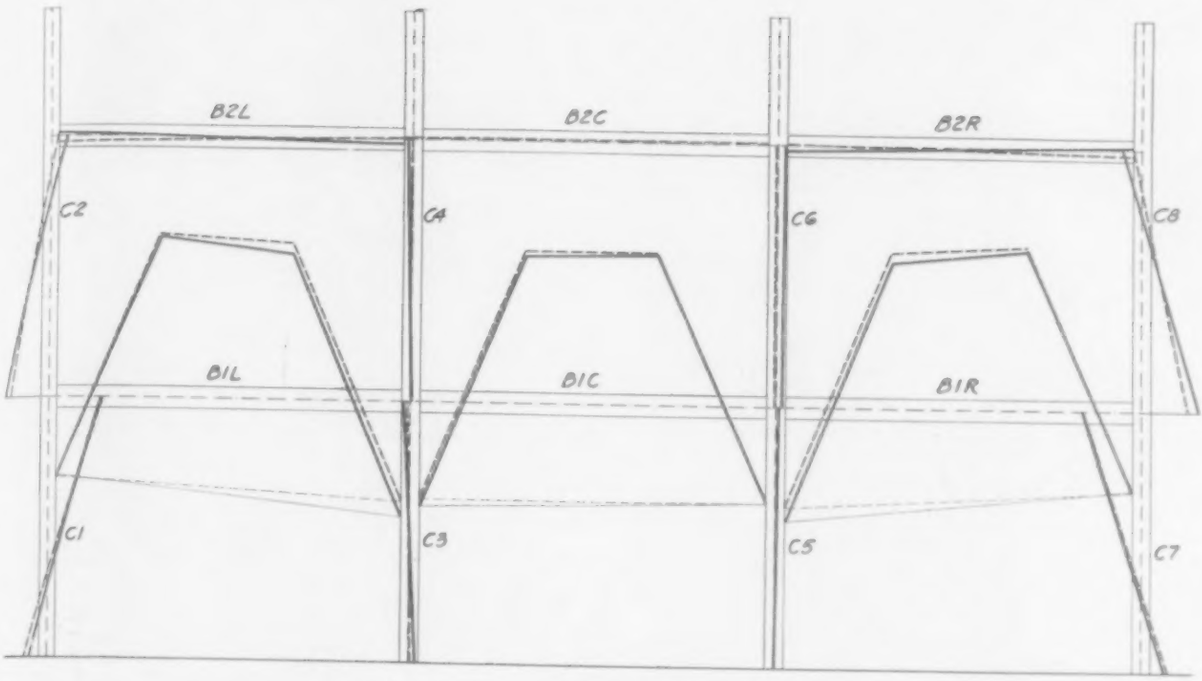
SEAT ANGLE DESIGN

The proposed method for seat angle design is based upon the test results reported by Lyse and Schreiner[1] and is similar to that proposed by Heath Lawson.[2] The method places no dependence on top angle or other connecting medium and the seat angle alone is designed to carry the full reaction load of the beam.

Figure 15 shows the details of a seat angle and the forces which it resists. The horizontal leg of the angle transmits the shear to the vertical legs which are welded to the column flange or column web. If the end connection is designed for end restraint the bottom flange of the beam must be welded to the seat angles with properly designed welds. The horizontal thrust induced along the top of the seat angle by end restraint tends to relieve the bending moments produced in the seat angle by the vertical loads but this will not be taken advantage of in the proposed design method. The end reaction of the beam will be assumed to act at a distance from the end of the beam equal to one-half the required bearing distance. The A.I.S.C. Specification for design of building construction requires the bearing length "N" to be:

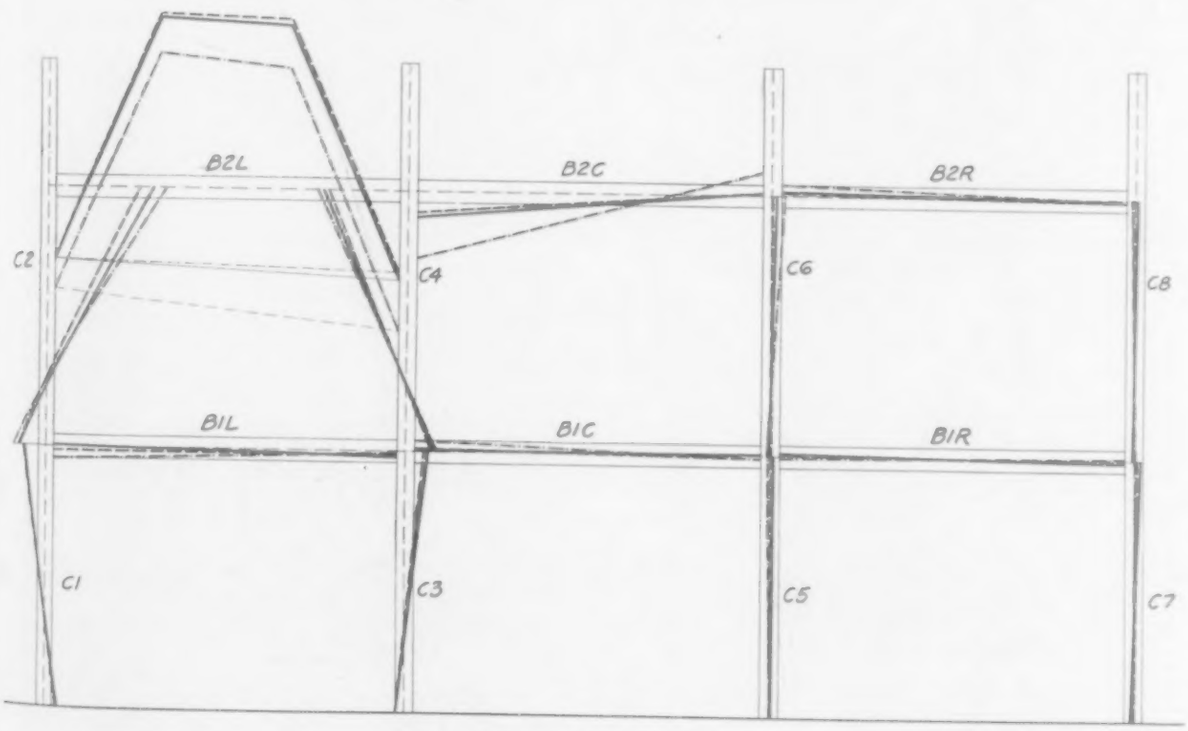
$$N = \frac{R}{w\sigma_B} - k \qquad (4)$$

R = end reaction of beam
w = web thickness
σ_B = allowable bearing stresses at root of beam fillet = 24 kips per sq. in.



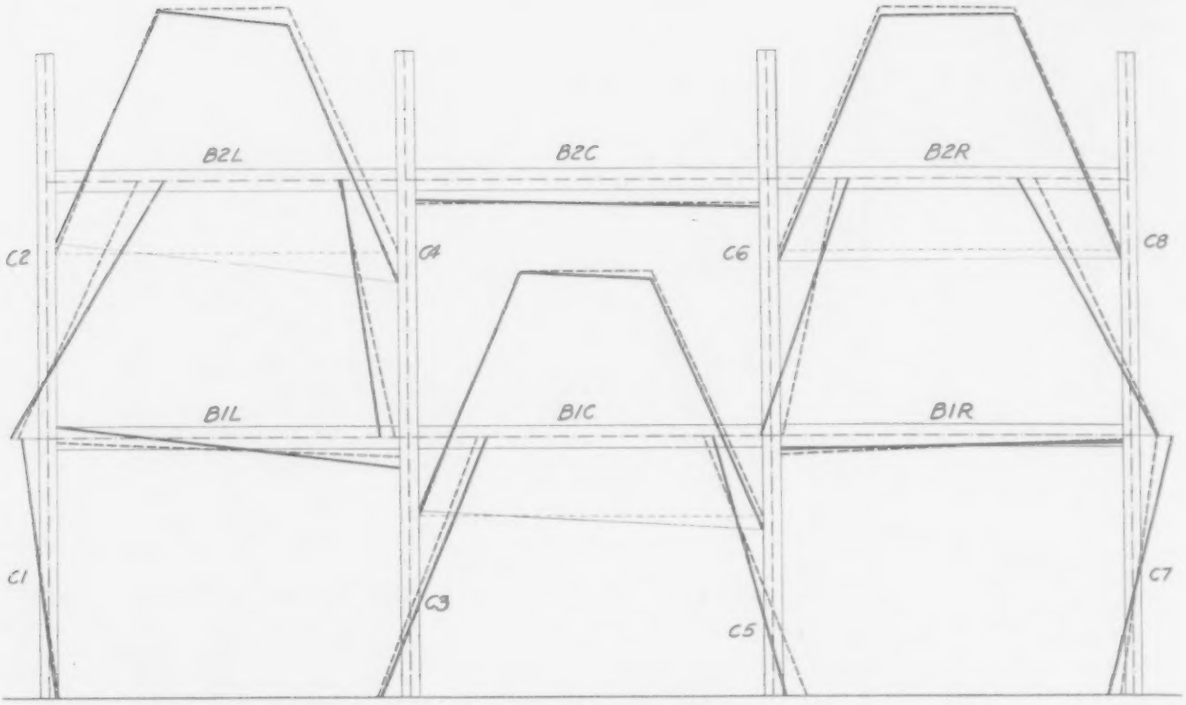
——Moments from test results
---Theoretical moments
Moment scale 1"=100 Inch Kips

FIG 10 MOMENTS IN FRAME No.1 BEAMS B1L, B1C, AND B1R LOADED



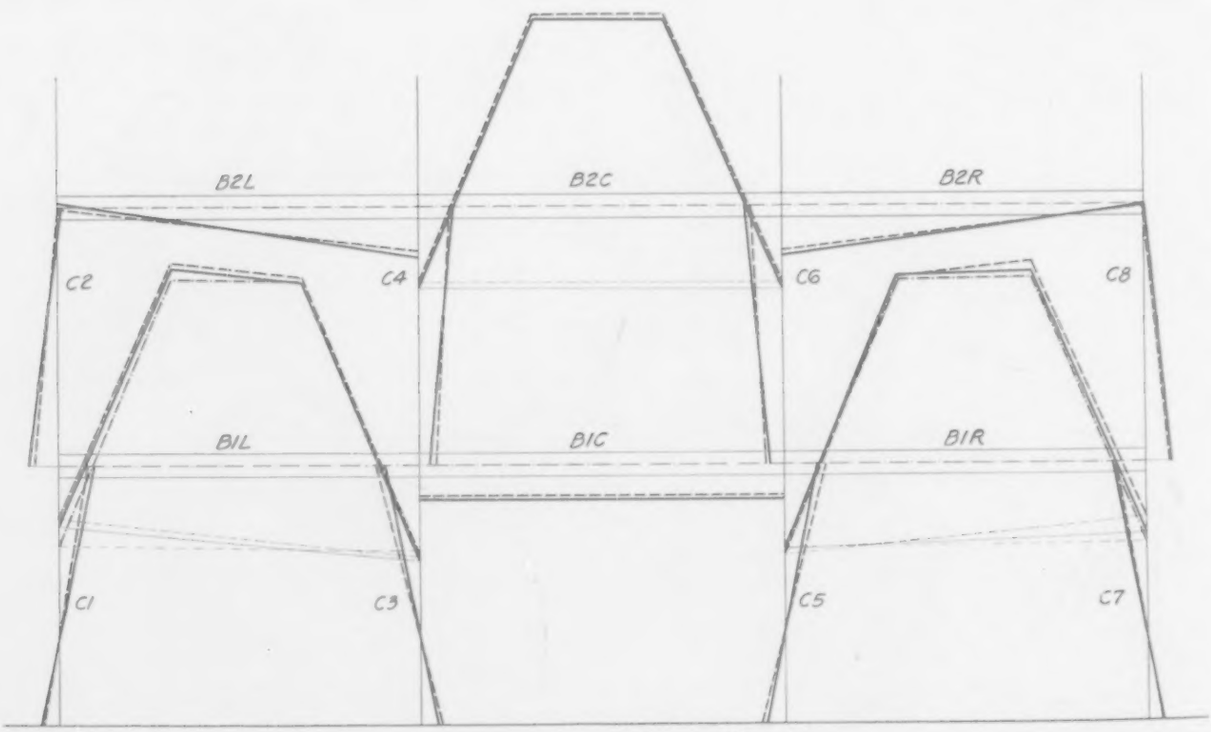
——Moments from test results
----Theoretical moments
——Theoretical moments, assuming 100% connection rigidity
Moment scale 1"=100 Inch Kips

FIG 11 MOMENTS IN FRAME No.1 BEAM B2L LOADED



——Moments from test results
----Theoretical moments
Moment scale 1"=100 Inch Kips

FIG 12 MOMENTS IN FRAME No.1 BEAMS B2L, B1C, AND B2R LOADED



——Moments from test results
----Theoretical moments
——Theoretical moments, exterior joints assumed equal to interior in rigidity
Moment scale 1"=100 Inch Kips

FIG 13 MOMENTS IN FRAME No.2 BEAMS B1L, B2C, AND B1R LOADED

k = distance between outside of flange and root of fillet as listed in steel handbooks.

In the proposed design method "N" shall in no case be taken as less than k.

The research of Lyse and Schreiner showed the critical section for calculating bending stresses in the angle to be a short distance back of the root of the fillet. Theoretical formulas were derived on the basis of a curved cross section,[1] but a simple approximation on the side of safety will be made. The critical section will be taken in the fillet $^1/_4$ in. from the exposed face of the angle, as shown in Fig. 15, and the cross section of the angle in the straight portion will be used in calculations. The actual fillet radius of seat angles will nominally vary between $^1/_8$ and $^5/_8$ of an inch. The use of $^1/_4$ in. is on the safe side and is convenient because it cancels with the $^1/_4$ in. clearance usually allowed between column and beam in detailing.

The required section modulus for bending of the angle leg will be:

$$S_{reqd} = \frac{Wt^2}{6} = \frac{R}{\sigma_a}\left(\frac{N}{2} - t\right) \tag{5}$$

S = section modulus
W = length of seat angle
t = thickness of seat angle
σ_a = allowable direct stress = 20 kips per sq. in., A. I. S. C. Specifications

The allowable reaction, from equation (5), is given by:

$$R = \frac{\sigma_a W t^2}{3N - 6t} \tag{6}$$

In rare cases the shear stress in the horizontal leg of the angle rather than the bending stress will determine the design, in which case the allowable reaction will be:

$$R = \tau_a \cdot W \cdot t \tag{7}$$

τ_a = allowable average shear stress = 13 kips per sq. in. by A. I. S. C. Specifications

For the unit stresses here quoted the shear stress will be critical when N is less than $2.51t$.

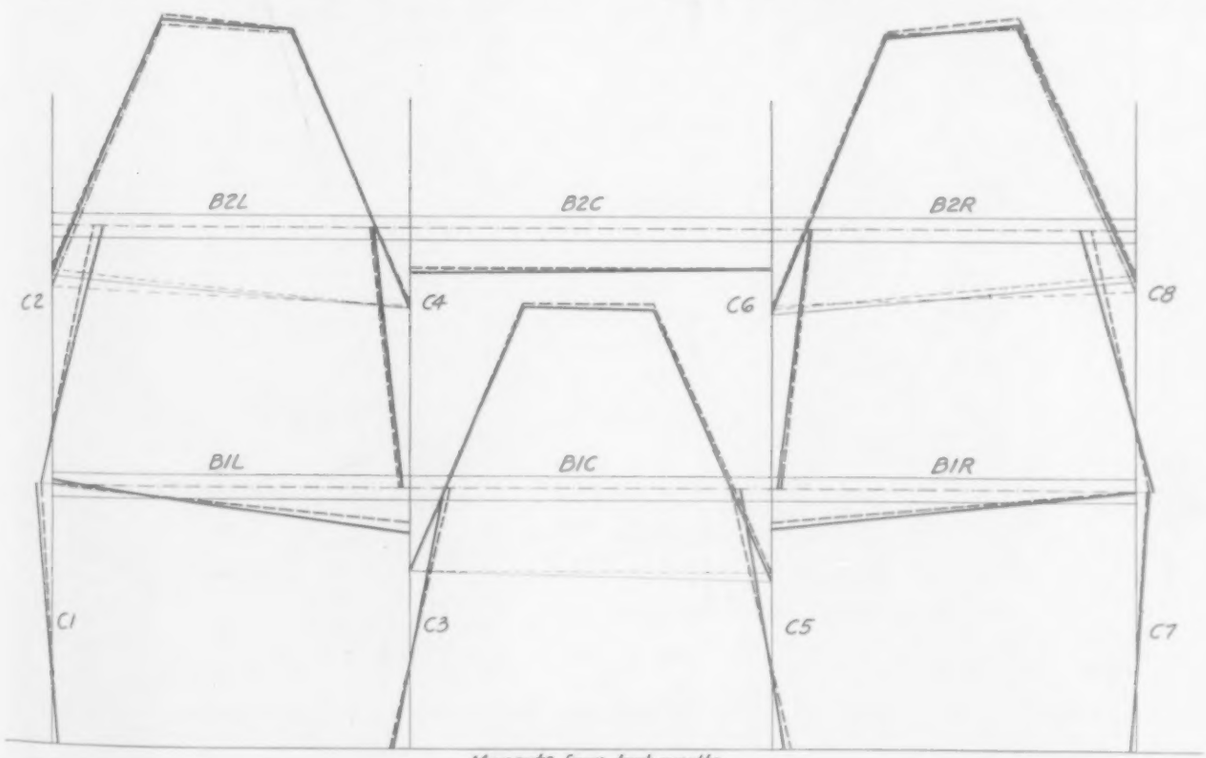
The fillet welds attaching the vertical legs of the angle to the column flange or web will run the full length of the vertical leg. Lawson[2] recommends that the weld be run continuously around the corner at the top of the seat angle and extended $^1/_2$ in. beyond as shown in Fig. 15. Most of the bending moment will be absorbed by the upper part of the vertical welds. Lyse and Schreiner[1] reported that the "neutral axis" in bending was considerably above the mid-height of the angle and this observation is in agreement with what should be expected theoretically. It will be assumed here that the neutral axis is at a distance $3t$ from the top of the angle. Using a rectangular distribution of stress the following formula uses the resultant of the horizontal and vertical shears as a criteria.

$$R = \frac{\tau_w A}{\sqrt{1 + \left(\frac{N}{3t}\right)^2}} \tag{8}$$

τ_w = allowable shear stress in weld metal = 13.6 kips per sq. in. for Grade 2, 4, 10 or 15 Filler Metal by A. W. S. Code for Building Construction.
A = total area in both vertical welds through the throat.

In Equation (8) N should in no case be taken as less than k.

The allowable load on a seat angle is to be taken as the minimum of the values given by Equation (6), (7) or (8). The procedure is essentially the same as that proposed by Lawson[2] except that Equation (8) is altered



Moments from test results
Theoretical moments
Theoretical moments, exterior joints assumed equal to interior in rigidity
Moment scale 1" = 100 inch kips

FIG 14 MOMENTS IN FRAME No. 2 BEAMS B2L, B1C, AND B2R LOADED

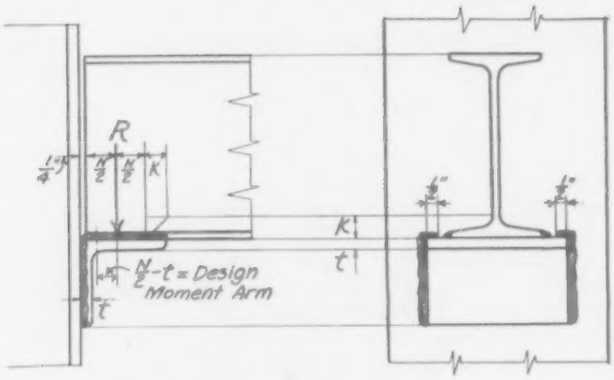


FIG 15 SEAT ANGLE

in one essential detail and is found to give a fairly consistent factor of safety when compared with all of the test results of Lyse and Schreiner,[1] which are summarized in Table 1. The average factor of safety against yielding (Column 8) as determined by scaling of the whitewash is 2.5 with a minimum of 1.5 in 26 tests. The average factor of safety against initial fracture (Column 9) of weld metal is 3.6 with a minimum of 2.0. An allowable stress of 11.3 kips per sq. in. was used in the welds since the filler metal was grade 40 in this investigation.

The foregoing procedure of seat angle design gives in every case a safe procedure of design. Design charts somewhat similar to those originally presented by Heath Lawson[2] are shown in Figs. 16*a* and 16*b*, which give the required angle thickness and weld size, respectively. The charts are based on Equations (6) and (8), using an allowable direct stress in the angle of 20 kips per sq. in. and an allowable resultant shear stress through the throat of the weld of 13.6 kips per sq. in. The procedure in using the charts is as follows:

1. Calculate N from Equation (4).
2. Select a trial length of seat angle, 1 to 2 in. longer than the flange width of the beam and calculate end reaction per inch length of seat angle.
3. Determine required seat angle thickness from Fig. 16*a* and select trial angle size.
4. Determine average end reaction per inch length of vertical weld and select weld fillet size from Fig. 16*b*.

The foregoing procedure does not include the design of the weld attaching the lower flange of the beam to the seat angle. Allowance for additional end reaction due to unbalanced end moment should also be made. These factors will be included in the section on "Design Procedure."

DESIGN OF TOP ANGLE CONNECTIONS FOR PARTIAL RESTRAINT

Test results and design procedures for top angle connections to give fifty per cent restraint have been presented in papers by Lyse and Gibson.[4,5] In the present paper a formula for calculating the end rigidity developed by the top angle connection has been derived which checks well with these and later[6] test results. The top angle must be strong enough to develop an end moment of half the fully fixed end restraining moment with an ample factor of safety. It must also be flexible enough to allow partial rotation with an adequate margin of safety with respect to deformation.

The forces acting on the end of a beam are assumed to

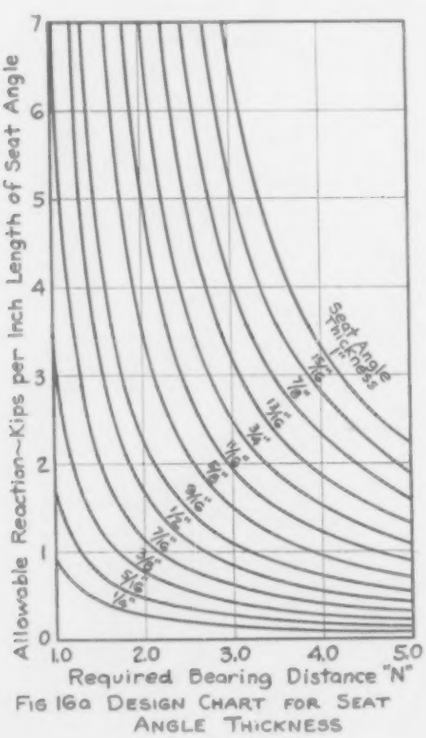


FIG 16a DESIGN CHART FOR SEAT ANGLE THICKNESS

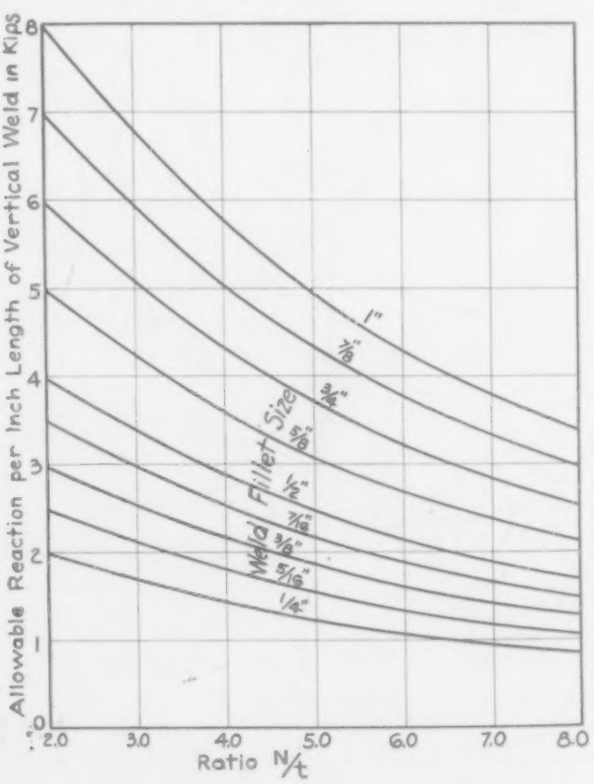


FIG. 16b DESIGN CHART FOR VERTICAL FILLET WELDS BETWEEN SEAT ANGLE AND COLUMN

TABLE 1

SPECIMEN No.	SIZE OF SEAT ANGLE	FILLET SIZE In.	DIST. BACK OF ANGLE TO LOAD	YIELD* KIPS	LOAD AT FAILURE† KIPS	ALLOWABLE LOAD BY EQ. 6, 7 or 8‡	F.S. AGAINST YIELD	F.S. AGAINST FAILURE
①	②	③	④	⑤	⑥	⑦	⑧	⑨
A 443 - 1	4×4×1/2×8	3/8	1.2	28.6	29.3	14.8 B	1.9	2.0
A 443 - 2	4×4×1/2×8	3/8	2.0	16.8	22.2	5.3 B	3.1	4.2
A 443 - 3	4×4×1/2×8	3/8	3.0	10.4	13.5	3.0 B	3.5	4.6
A 444 - 1	4×4×1/2×8	1/2	1.2	32.0	39.0	14.8 B	2.2	2.6
A 444 - 2	4×4×1/2×8	1/2	2.0	18.5	24.0	5.3 B	3.5	4.5
A 444 - 3	4×4×1/2×8	1/2	3.0	10.0	11.2	3.0 B	3.5	3.8
A 463 - 1	4×4×3/4×8	3/8	1.2	71.0	100.0	18.3 W	3.9	5.5
A 464 - 1	4×4×3/4×8	1/2	1.2	73.0	101.0	24.4 W	3.0	4.1
A 466 - 1	4×4×3/4×8	3/4	1.2	67.5	176.5	36.7 W	1.8	4.8
A 643 - 1	6×4×1/2×8	3/8	1.2	33.8	38.6	14.8 B	2.3	2.6
A 644 - 1	6×4×1/2×8	1/2	1.2	37.0	46.0	14.8 B	2.5	3.1
X - 1	6×4×1/2×8	1/2	1.2	22.2	44.1	14.8 B	1.5	3.0
Y - 1	6×4×1/2×8	1/2	1.2	25.8	43.7	14.8 B	1.7	3.0
A 654 - 1	6×4×5/8×8	1/2	1.2	53.1	74.7	29.3 B	1.8	2.5
A 655 - 1	6×4×5/8×8	5/8	1.2	57.0	102.0	29.3 B	1.9	3.5
A 664 - 1	6×4×3/4×8	1/2	1.2	100.0	136.3	36.7 W	2.7	3.7
A 843 - 1	8×4×1/2×8	3/8	1.2	49.0	74.3	14.8 B	3.3	5.0
A 844 - 1	8×4×1/2×8	1/2	1.2	53.0	74.0	14.8 B	3.6	5.0
A 864 - 1	8×4×3/4×8	1/2	1.2	87.2	132.0	48.9 W	1.8	2.7
A 884 - 1	8×4×1×8	1/2	1.2	137.5	164.5	54.3 W	2.5	3.0
A 884 - 2	8×4×1×8	1/2	2.0	55.0	120.5	35.6 B	1.5	3.4
A 884 - 3	8×4×1×8	1/2	3.0	36.1	51.4	15.2 B	2.4	3.4
A 886 - 1	8×4×1×8	3/4	1.2	137.5	170.0	81.4 W	1.7	2.1
A 444C - 1	4×4×1/2×8	1/2	1.2	37.0	76.0	14.8 B	2.5	5.1
A 644C - 1	6×4×1/2×8	1/2	1.2	37.0	54.1	14.8 B	2.5	3.6
A 644D - 1	6×4×1/2×8	1/2	1.2	35.1	52.5	14.8 B	2.4	3.5

*Load at first reported scaling in weld or angle
†Load at first crack at top of angle
‡Determined by bending of angle (Eq. 6), (W. Determined by weld, (Eq. 8))

be as shown in Fig. 17*a* and those acting on the top angle itself are illustrated in Fig. 17*b*. An analysis of the elastic behavior of the top angle has been made by the theory of least work. Bending, shearing and direct deformations were taken into account in this analysis, the details of which are omitted. The results of the analysis are as follows:

1. The point of inflection, or zero bending moment, in the vertical leg of the top angle lies between 1/3 and 1/2 the distance from the top of the beam for usual design proportions.

2. A formula is derived which satisfactorily approximates the minimum coefficient of connection rigidity, or "connection constant" to be counted upon at design load.

On the basis of (1) the horizontal pull on the top angle should be calculated by dividing the end moment by the sum of the beam depth and one-half the length of the vertical leg of the top angle. The following gives the total horizontal pull acting on the top angle as calculated on this basis.

$$P = \frac{M_F}{\left(d + \frac{a}{2}\right)} \tag{9}$$

P = load in kips
M_F = fifty per cent of end moment for one hundred per cent restraint, in inch-kips
a = length of the vertical angle leg
d = depth of beam

The critical stresses determining the strength of the top angle are a result of a maximum tensile stress due to the combination of direct load and bending moment in the throat of the top weld. A formula derived on the approximate assumption that the yield stresses are developed simultaneously in the throat of the weld and at the root of the angle fillet has been found to give a very consistent factor of safety with both the general yield and ultimate strength of the top angle as reported in previous tests.[4,5,6] The allowable horizontal pull on the top angle is given by:

$$P = \frac{18Wt^2}{\frac{3}{2}a - t} \tag{10}$$

By solving Equations (9) and (10) for M_F the following gives directly the allowable end moment for any particular top angle and beam depth:

$$M_F = \frac{18Wt^2\left(d + \frac{a}{2}\right)}{\left(\frac{3a}{2}\right) - t} \tag{11}$$

Columns 5, 6 and 7 of Table 2 compare the design end moment calculated by Equation 11 with the end moment at general yield and ultimate load. Excluding Tests G1, M9 and M10, the average factor of safety with the general yield, shown in Column 8, is 1.50 and the minimum is 1.34. The average factor of safety with the ultimate for the same tests, shown in column 9, is 3.05 and the minimum is 2.43. A typical diagram of the relation between moment and relative angle change between beam and column is shown in Fig. 18. The method used for determining the general yield is illustrated in this figure and is one that has been found to give good results in these and other tests of steel structures. A straight line tangent to the initial load-deformation curve is extended to an ordinate equal to the ultimate load. The actual load at the deformation corresponding to this ordinate is taken as the general yield, or "useful limit" of the member.

The excellent agreement between Equation 11 and the test results of 3 x 3-in. top angle connections should allow its extension to $3^1/_2$ x $3^1/_2$-in. top angles. Figures 19*a* and 19*b* give charts for the direct design of these two sizes of top angles for different angle sizes and depth of beam. The use of these charts will be explained in the succeeding section.

The top angle not only must be strong enough but must at the same time provide an end connection having a minimum of fifty per cent rigidity. In the discussion of tests of the building frame the experimental connection constant and its relation to frame analysis were discussed and the constants γ and α defined. In the application to design it is more convenient to use a new constant J, which is in direct relation with the joint rigidity and is given by:

$$J = \frac{1}{E\gamma} = \frac{2I}{\alpha l} \tag{11}$$

The connection is designed for the restraining moment which would exist with no column rotation at the joints. In this case the actual end moment is given by:

$$M_F = \frac{M_{FR}}{1 + \alpha} \tag{12}$$

M_{FR} = fixed end moment for one hundred per cent rigid connections as calculated, or given in various tables.

$$\text{Percentage rigidity} = 100\,\frac{M_F}{M_{FR}} = \frac{100}{1 + \alpha} = \frac{100}{1 + \dfrac{2I}{Jl}} \tag{13}$$

It follows from the preceding relations that for fifty per cent connection rigidity the minimum value of J is:

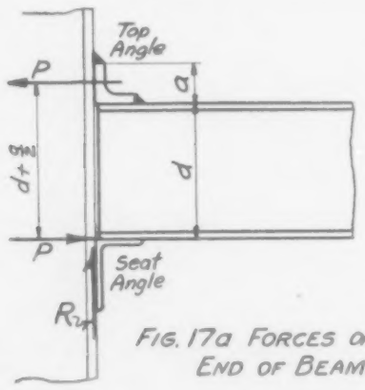


FIG. 17a FORCES ON END OF BEAM

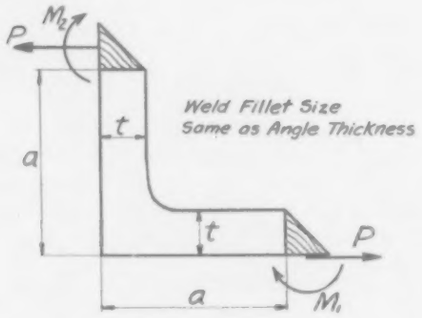


FIG. 17b FORCES ACTING ON TOP ANGLE

FIG 18 MOMENT-ANGLE CHANGE DIAGRAM FOR TYPICAL JOINT TEST

$$J = \frac{2I}{l}$$

for seventy-five per cent rigidity $J = \frac{6I}{l}$ and for eighty per cent $J = \frac{8I}{l}$. An eighty per cent connection rigidity may be taken as a satisfactory upper limit for design, although it is preferable to keep within the limits of fifty and seventy-five per cent.

The analysis of the top angle provides a formula for determining J. The assumption is made that the center of joint rotation is 0.2d above the seat angle, which is about the maximum reported in previous tests.[4]

$$J^* = \frac{Wt^3(0.8ad^2 + 0.8bd^2 + 0.9atd + 0.9btd + 0.9b^2d)}{3.64b^3a + 0.91b^4 + 4.9abt^2 + 3.9b^2t^2 + a^2t^2} + \frac{Wt^3(0.33b^3 + 0.5b^2t + 0.34at^2 + 0.61bt^2)}{3.64b^3a + 0.91b^4 + 4.9abt^2 + 3.9b^2t^2 + a^2t^2} \tag{14}$$

b = width of vertical leg
a = width of horizontal leg
W = length of top angle

For equal leg angles $a = b$ and Equation 14 reduces to Equation 14*a*:

$$J = \frac{Wt^3(1.6d^2 + 1.8td + 0.9ad + 0.33a^2 + 0.5at + 0.95t^2)}{4.55a^3 + 9.8at^2} \tag{14a}$$

Table 2 provides a comparison between previous test results[4,5,6] and values of J as determined by Equation 14*a*. Column 10 gives the value of J by Equation 14*a* and Columns 11 and 12 give the test values of J at design load and at the general yield. Columns 13 and 14 give the ratios of the test J, to J by Equation 14*a* at design moment and yield moment, respectively.

In the *G* and *M* series of tests the value of J at design is greater by test than by formula in all except two cases, M_{10} and M_{11}. In each of these cases unusually wide beam sections were used and the top angle extended the full width of the column flange. It is recommended that the length of the top angle be the same as the width of the beam flange but that it should in no case be greater than 0.8 of the depth of the column section. Top angle thickness should not be more than $^7/_8$ for 3 x 3-in. angles nor more than 1 in. for $3^1/_2$ x $3^1/_2$-in. angles. Both fillet welds connecting the top angle to the beam and column are to be the same size as the top angle thickness.

* The use of unequal leg angles might be economical in certain cases because 6 by $3^1/_2$-in. angles may be obtained from some mills up to one inch in thickness. The use of such an angle might eliminate the need for special cutting of thick angles to a 3 by 3 or $3^1/_2$ by $3^1/_2$ size. Design charts similar to Fig. 19 may be drawn for unequal leg angles but in the absence of test data such charts will be omitted in this report.

TABLE 2

TEST NO.	SIZE OF TOP ANGLE	BEAM SIZE	COLUMN SIZE	MOMENT IN INCH KIPS DESIGN FORM.	MOMENT IN INCH KIPS GENERAL YIELD	MOMENT IN INCH KIPS MAXIMUM	FACTOR OF SAFETY AT YIELD	FACTOR OF SAFETY AT MAX.	CONNECTION CONST. J BY FORMULA	CONNECTION CONST. J TEST J_{at} DESIGN LOAD	CONNECTION CONST. J TEST J_{at} GENERAL YIELD	RATIO $\frac{J_D}{J_F}$	RATIO $\frac{J_Y}{J_F}$
(1)	(2)	(3)	(4)	(5)	(6)	(7)	(8)	(9)	(10)	(11)	(12)	(13)	(14)
G 1	3×3×7/16×8"	12WF	—	419.0	570.0	855.0	1.36	2.04	10.46	15.4	9.42	1.47	0.90
G 2	3×3×1/2×6	12WF	—	91.2	123.0	222.0	1.35	2.43	1.60	2.85	1.83	1.78	1.14
G 3	3×3×3/4×6	12WF	—	218.0	315.0	596.0	1.44	2.73	5.14	8.08	4.20	1.57	0.82
G 4	3×3×3/16×6½	12WF28	8WF48	53.8	78.0	191.0	1.45	3.55	0.74	1.79	0.92	2.42	1.24
G 5	3×3×5/16×6½	12WF28	8WF48	159.0	240.0	456.0	1.51	2.87	3.32	3.57	2.20	1.08	0.66
M 1	3×3×1/4×5†	12I31.8	10WF49	18.0	31.4	88.1	1.74	4.88	0.17	1.28	0.48	7.53	2.82
M 2	3×3×1/2×5	12I31.8	10WF49	76.0	102.0	194.5	1.34	2.56	1.33	1.90	1.01	1.43	0.76
M 3	3×3×5/8×5	12I31.8	10WF49	112.5	160.0	317.0	1.42	2.82	2.55	3.29	1.88	1.29	0.74
M 4	3×3×3/4×5	12I31.8	10WF49	182.2	260.0	472.0	1.42	2.59	4.29	7.90	5.75	1.84	1.34
M 5	3×3×1/2×10†	10WF49	10WF49	129.3	193.0	389.0	1.49	3.00	1.92	2.37	1.47	1.23	0.77
M 6	3×3×1/2×8	10WF49	10WF49	103.4	167.0	300.0	1.61	2.90	1.51	2.05	1.30	1.36	0.86
M 7	3×3×1/2×6	10WF49	10WF49	77.6	119.0	256.0	1.54	3.30	1.15	2.22	1.26	1.93	1.09
M 8	3×3×1/2×5½	15I42.9	10WF49	102.1	149.0	276.5	1.46	2.70	2.20	2.71	1.77	1.23	0.81
M 9	3×3×1/2×4	8I18.4	10WF49	42.7	59.0	132.8	1.38	3.11	0.57	2.69	1.00	4.72	1.75
M10	3×3×1×10*	10WF42.9	10WF49	591.0	808.0	1320.0	1.36	2.23	13.60	5.56	3.81	0.41	0.28
M11	3×3×3/4×10*	10WF42.9	10WF49	320.0	516.0	710.0	1.66	2.22	6.16	4.08	2.44	0.66	0.40
M12	3×3×3/4×5¾	10WF42.9	10WF29	210.0	401.0	702.0	1.91	3.34	4.91	8.08	5.56	1.64	1.13
F 1	3×3×7/16×4¼	10I25.4	8WF31	108.4	—	—	—	—	2.30	1.92	—	0.84	—
F 2	3×3×7/16×4¾	10I25.4	8WF31	108.4	—	—	—	—	2.30	2.26	—	0.98	—
F 3	3×3×7/16×4¾	10I25.4	8WF31	108.4	—	—	—	—	2.30	0.91	—	0.40	—

*3/4 weld; † Outside design range; ‡ Connection made to column web, interior column; ‡Connection made to column web, exterior column

It should be noted that the constant "J" is quite sensitive to small variations in dimension and that between the limits of fifty and seventy-five per cent connection restraint the numerical value of constant J varies three hundred per cent. Connections of the proposed type having adequate strength usually have excess stiffness. For example, in Table 2 the worst case is test M_{10} with J by test only 41 per cent of J by Equation 14. Assuming the beam to have a length of 10 ft. in this case the end connection has adequate strength and by Equations 13 and 14 should develop seventy-five per cent restraint. Using the test value of J the joint by Equation 13 actually developed fifty-five per cent restraint and hence would be satisfactory. Nevertheless, the size limitations in the foregoing paragraph would rule it out.

Tests F1, F2, and F3 were made with joints representative of those used in the actual building frame tested, in which case J by Equation 14 is 2.30. The comparison between theoretical and test results is shown in Table 3.

Table 3

	Theoretical Equations 13, 14	Test of Flange Connection	Test of Inside Web Connection	Test of Outside Web Connection
J	2.30	1.92	2.26	0.91
% Restraint	60.10	55.80	59.70	37.40

Table 3 shows that both the flange connection and inside web connection developed restraint reasonably close to the theoretical value. The outside web connection, however, developed only 62.6 per cent as much restraint as indicated by formula. The test set-up for an outside web connection, in which the beam frames into only one side of the column, is shown in Fig. 8*b*.

The test results of the actual frame with web connections as shown in Figs. 13 and 14 indicate that the interior column connections for such cases develop full moment equivalent to the column flange connections but that the outside column web connections do not. An analysis which assumes the rigidity of the outside connections equal to the inside is shown by the dot-dash lines for the outside loaded beams in Figs. 13 and 14 whereas the dashed lines are based on an analysis using the experimentally determined rigidities for both inside and outside column connections. The difference between actual maximum moment and the analysis is seen to be small in either case. The exterior column web connections may be reinforced by plates welded in position as shown in Fig. 20, in which case they should develop a rigidity fully equivalent to an interior connection.

Within the stated limitations the formula for J gives a satisfactory guide to minimum rigidity developed by a beam-column flange connection or an interior beam-column web connection. Charts for determining J for 3 x 3-in. and $3\frac{1}{2}$ x $3\text{-}\frac{1}{2}$ in. angle sizes of different thickness and for varying beam depth are shown in Figs. 21*a* and 21*b*.

The following summary reviews the steps in the design of the top angle:

1. The beam and column sizes will have been selected. Assume a top angle length "W" equal to beam flange width.
2. Divide the end moment for fifty per cent rigidity by the top angle length to determine moment per inch length and select top angle size and thickness from Figs. 19*a* or 19*b*.
3. Determine J from Figs. 21*a* or 21*b* for the top angle size selected. J must be more than $\frac{2I}{l}$ and preferably not more than $\frac{6I}{l}$. $\frac{8I}{l}$ may be taken as an upper limit.
4. Design the seat angle by the method outlined on page 364.

5. Determine the horizontal thrust P by Equation 9 and design fillet welds between lower beam flange and seat angle to take this thrust.

The foregoing steps will be illustrated later in the section on "Design Procedure."

DESIGN OF BEAMS

The preceding sections on the design of seat and top angles have been based on a predetermined beam size, the selection of which would be the first step in normal design procedure. The design of the beams is based on the assumption that the connections will have a minimum rigidity factor of fifty per cent at design load and not over seventy-five per cent at a load of one hundred and fifty per cent of the design load. The choice of fifty per cent as a connection rigidity factor has been made after a consideration of the economy of both beam and connection design and of the factor of safety in beam design. Too high a rigidity factor will require expensive connection details whereas fifty per cent connection rigidity takes advantage of a large measure of economy with the relatively simple seat and top angle connection.

The factor of safety requires special attention because of the non-linear relation between moment and angle change at the connection. It may be noted in Table 2 that the minimum factor of safety between design moment and yield moment is 1.34 for all the connections tested in the previous investigations. As a minimum criteria of behavior let it be assumed that the joint yields at 1.35 times the design moment and that it takes no additional moment whatever. The solid line in Fig. 22 illustrates typical joint behavior and the dashed line represents the preceding assumptions. Now assume that the load on the beam increases until the stress in the center rises from a design stress of 20 kips per sq. in. to the specified minimum yield point of 33 kips per sq. in. The outside fiber stress at the partially restrained end could only rise from 10 kips per sq. in. to 13.5 kips per sq. in. after which it would maintain a constant value. The load ratio or factor of safety at the yield point will be a minimum of 1.55 as compared with the stress ratio of 1.65. This reduction is not serious even for the extreme assumption that the ultimate strength of the joint is no greater than the yield strength. If the joint actually develops more rigidity than the fifty per cent for which it is designed it may yield locally at design load until a condition of equilibrium is established. The load factor of safety with respect to beam fiber stress of 33,000 lb. per sq. in. will still be a minimum at 1.55.

It should be kept in mind that the total deformation is limited by the rotation of the end of the beam and that ultimate failure of the top angle can never occur in an actual structure prior to beam failure, provided the design is within the specified limitations. It should also be noted that even if the top angle is omitted the maximum stress in a beam designed for fifty per cent end restraint would be 30 kips per sq. in. which is below the specified minimum yield-point stress of 33 kips per sq. in. for structural grade steel. Reference may also be made to the original work of C. Batho,[7] who has developed graphical procedures to give the actual end moments developed by any end connection.

The beam-column connections are designed to develop a full fifty per cent of the end moment in a fully fixed

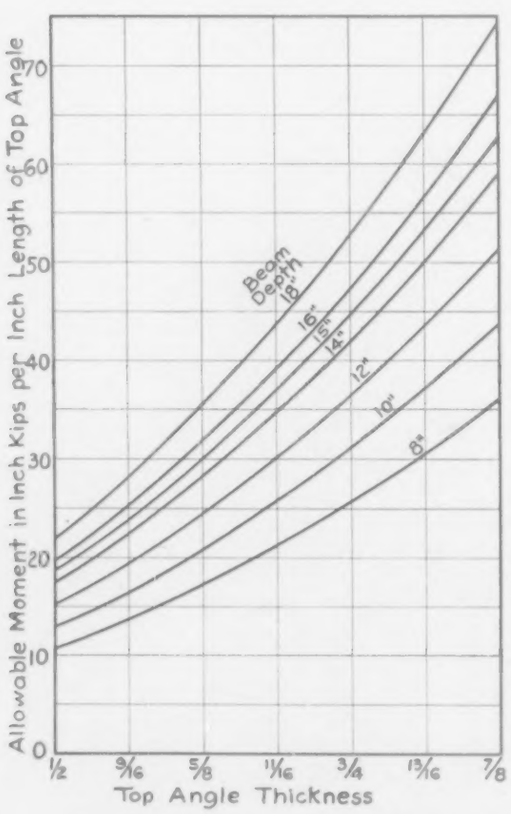


FIG. 19a DESIGN CHART FOR 3"×3" TOP ANGLES

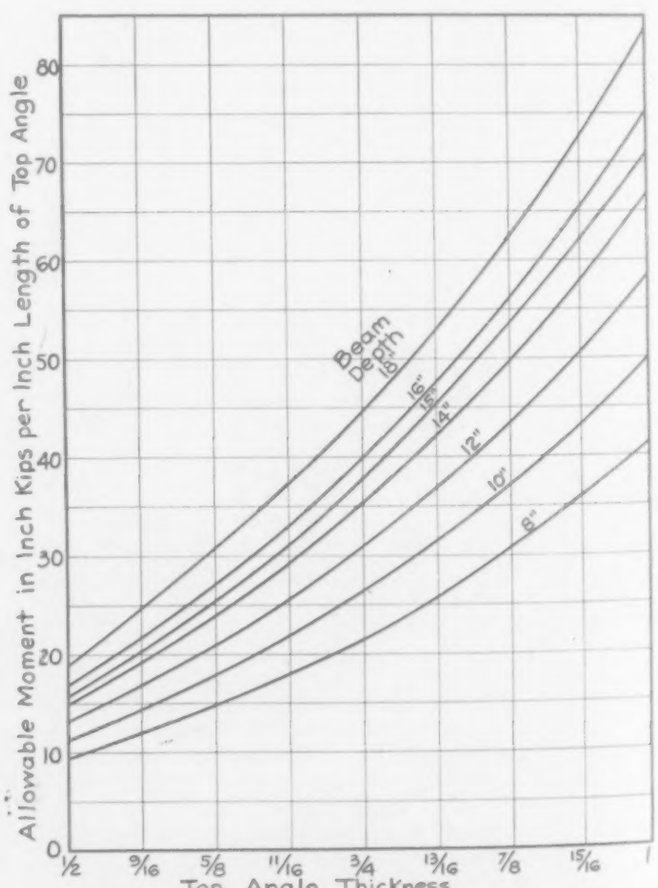


FIG. 19b DESIGN CHART FOR 3½"×3½" TOP ANGLES

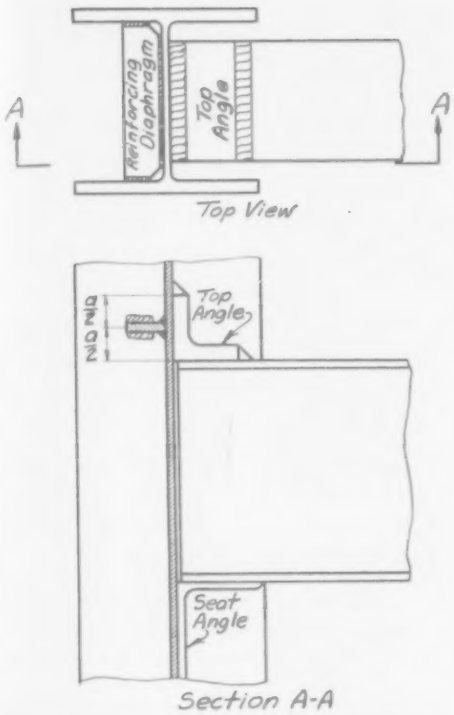


FIG 20 REINFORCING DIAPHRAGM FOR EXTERIOR COLUMN WEB. CONNECTIONS

beam, but in an actual building frame this moment can be developed only in the interior beams with all of the beams loaded as illustrated in Fig. 10 for the first story. For unsymmetrical conditions of loading or structure the columns will bend, relieving the end moments in the beams, and resulting in corresponding increase in the positive moment in the center of the beam. The maximum positive moment which may be expected from the most critical combination of live load will be used as a basis for design.

The condition of loading which produces the maximum moment in the center of any beam *A-B* is shown in Fig. 23*a*. As a simplification of the problem the section of the structure shown in Fig. 23*b* will be considered. The moments at *C*, *D*, *E* and *F* acting on the ends of the columns which frame with beam *A-B* will be assumed to act with a sense which produces greatest positive moment in the center of beam *A-B* and are assumed to have a magnitude equal to that at their juncture with the beam. The restraining effect of the beams on a level with *A-B* which frame to the opposite side of the columns will be neglected entirely, thus making the same procedure applicable to beams in both the exterior and interior bays of buildings.

The slope deflection equations for any beam *A-B* having end connections which develop fifty per cent restraint are:

$$M_{AB} = \frac{EI}{4l}(5\Theta_A + \Theta_B) - \frac{M_{FRA}}{2}$$
$$M_{BA} = \frac{EI}{4l}(5\Theta_B + \Theta_A) + \frac{M_{FRB}}{2} \qquad (15)$$

Using these equations for the beam and Equation 1 for the adjacent columns, which are assumed continuous, it may be shown that the end moment in the beam with fifty per cent end restraint is given by:

$$M_F = M_{FR}\left[\frac{1}{2 + \frac{K_B}{\Sigma K_c}}\right] \qquad (16)$$

In equation 16:

ΣK_c = the sum of $\frac{I}{l}$ for the column sections above and below one end of the beam

$K_B = \frac{I}{l}$ for the beam

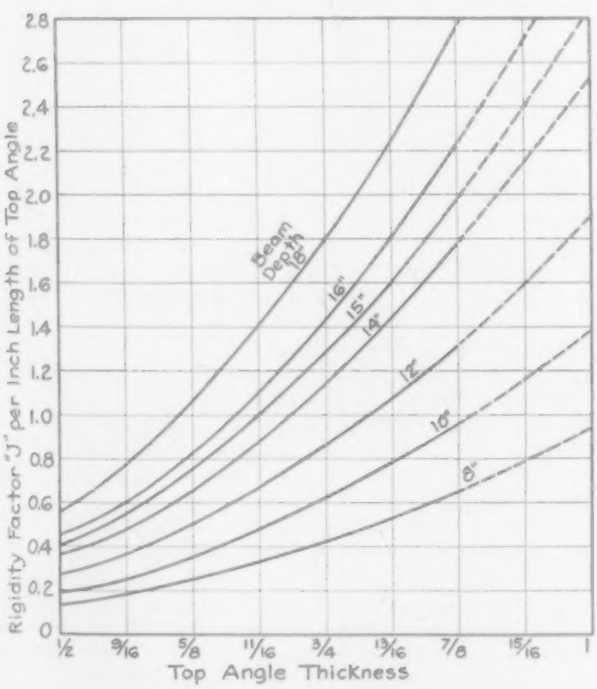


FIG. 21a CHART OF CONNECTION RIGIDITY FACTOR "J" FOR 3"×3" TOP ANGLES

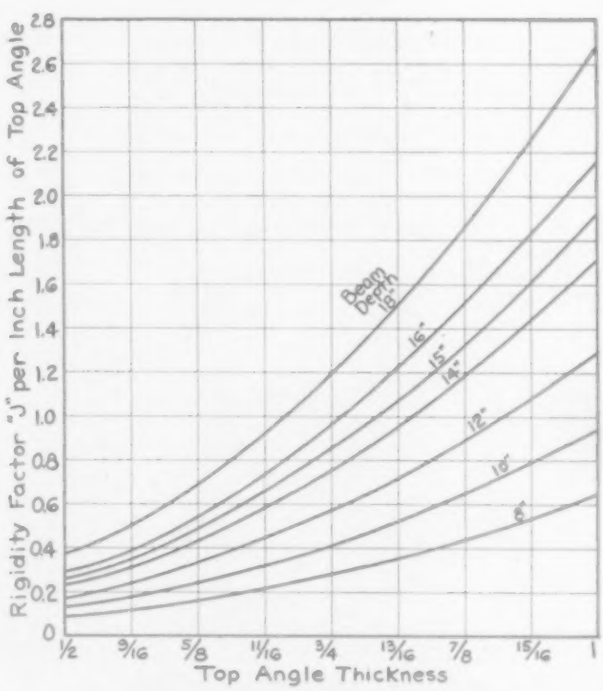


FIG. 21b CHART OF CONNECTION RIGIDITY FACTOR "J" FOR 3½"×3½" TOP ANGLES

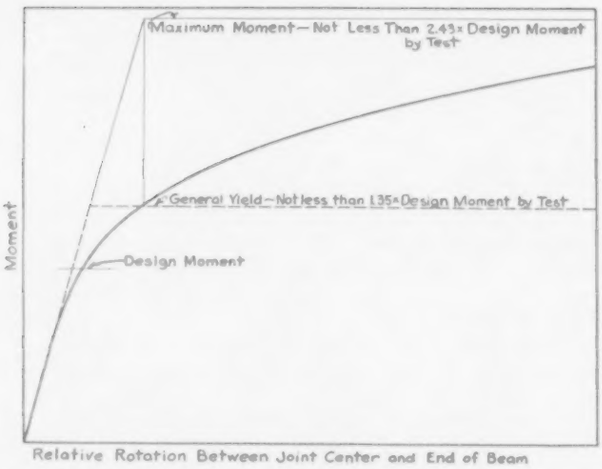

Maximum Moment—Not Less Than 2.43× Design Moment by Test
General Yield—Not less than 1.35× Design Moment by Test
Design Moment
Moment
Relative Rotation Between Joint Center and End of Beam

FIG. 22 MOMENT–ROTATION DIAGRAM, MINIMUM TEST VALUES

M_{FR} = end moment in a fully fixed end beam with "rigid" connections

Equation 16 is exact for a symmetrically loaded beam in a symmetrical structure and is closely approximate for ordinary unsymmetrical conditions. It should be kept in mind that the additional restraint provided by other beams, floor slabs, walls, etc., has been neglected.

For symmetrical conditions the maximum center moment will be:

$$M_c = M_s - M_F \quad (17)$$

M_F = minimum end moment by Equation 16
M_s = maximum moment in a simply supported beam

For symmetrical cases the design procedure may be simplified by calculating a reduction factor to be multiplied by the maximum simple beam moment assuming free supports.

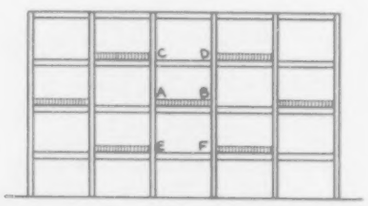


FIG 23a LOAD CONDITION FOR MAXIMUM CENTER MOMENT IN BEAM "AB"

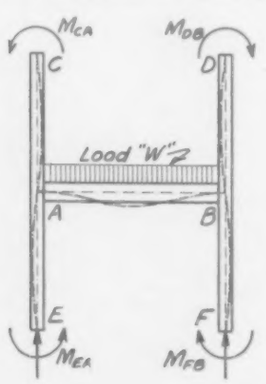


FIG 23b CONDITIONS ASSUMED FOR CALCULATING DESIGN MOMENT IN ANY BEAM "A-B"

$$\text{Reduction factor} = F = \frac{M_c}{M_s} = 1 - \frac{M_{FR}}{M_s}\left[\frac{1}{2 + \frac{K_B}{\Sigma K_c}}\right] \quad (18)$$

The moment to be used in the design will then be:

$$M_c = F \cdot M_s \quad (19)$$

In Equation 18 the ratio of $\frac{M_{FR}}{M_s}$ is $^1/_2$ for a concentrated load at the center, $^2/_3$ for uniform load or for two concentrated loads at third points and $^5/_8$ for three concentrated loads at quarter points. Figure 24 gives directly values of the reduction factor for various ratios of beam-to-column stiffness for these load conditions.

For unsymmetrical conditions the maximum center moment will be approximately equal to:

$$M_c = M_s - \text{Avg.}M_F \quad (20)$$

Avg.M_F is the average of the end moments at each end as calculated by Equation 16. Equation 20 will give results within a few per cent except for unusual conditions of loading such as beams carrying offset columns near one end. Equations 16 and 20 may be used for all cases where lack of symmetry exists in the structure due only to the use of different size columns.

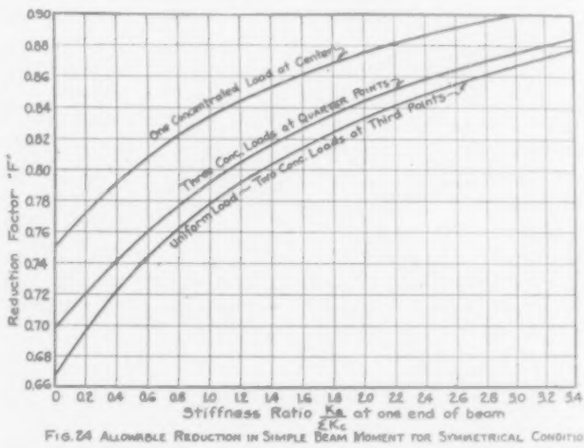


FIG. 24 ALLOWABLE REDUCTION IN SIMPLE BEAM MOMENT FOR SYMMETRICAL CONDITIONS

In the interior panels of structures of uniform beam length and uniform dead load, the full fifty per cent restraint could be counted on in calculating the dead load moment. The calculations will be simplified, on the safe side, and will give practically the same result in most cases, however, if the dead load is simply added to the live load and all treated alike.

A comparison of design moments calculated by the proposed method with the actual moments in the experimental building frame is presented in Figs. 25 and 26. The solid lines indicate the moments based on measured end moments superposed for all of the various load conditions and the dashed line indicates the design moment calculated by the proposed method. Figures 25*a* and 25*b* are for the second and first story beams, respectively, of Frame 1 having beam-to-column flange connections, and Figs. 26*a* and 26*b* are for the corresponding beams in Frame 2 having beam-to-column web connections. It may be seen that there is an ample margin of safety between design moment and actual moment in every case for the variety of critical load conditions used in testing the frames. This is true even in the case of the outside web connections which did not develop fifty per cent restraint.

It has been shown that the proposed design procedure

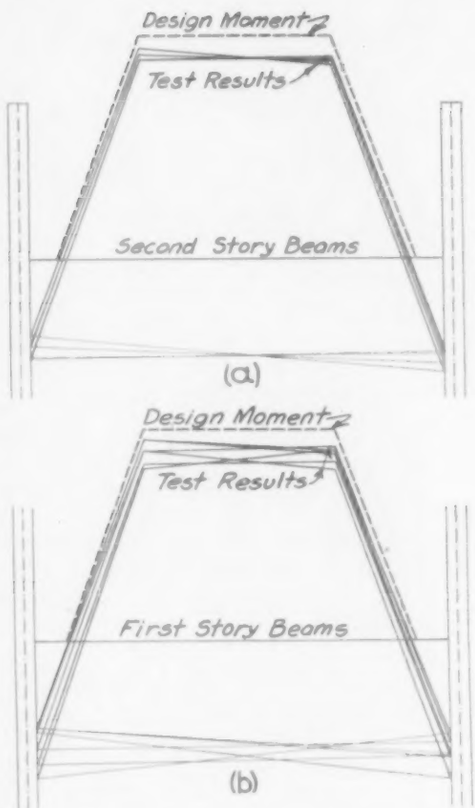


FIG 25 TEST RESULTS OF FRAME No.1 COMPARED WITH DESIGN MOMENT

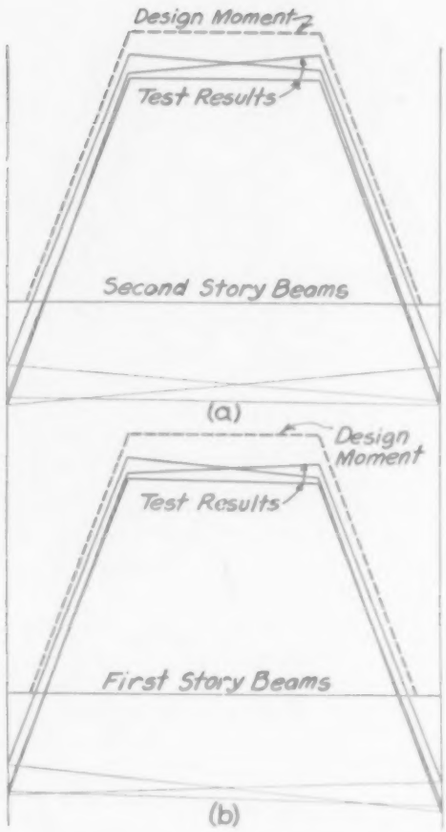


FIG 26 TEST RESULTS OF FRAME No.2 COMPARED WITH DESIGN MOMENT

gives an ample margin of safety for a loaded frame consisting of bare beams and columns in one plane. In an actual structure additional stiffness and strength will be introduced by beams at right angles to the frame considered, by floor slabs and by walls.

The procedure for designing symmetrically loaded beams may be reduced to the following:

1. Calculate M_s, the maximum moment in the beam assuming freely supported ends, and select a preliminary beam size designed to resist this moment. The required section modulus will be $M_s/20$ if the basic unit stress is 20 kips per sq. in.
2. Calculate the ratio of $K_B/\Sigma K_c$ on the basis of the tentative selection, determine the design moment M_c from Equation 19 or from Fig. 24 and select final beam size, the required section modulus being $M_c/20$.
3. Design seat and top angle connections by the methods previously outlined.

The selection of the tentative beam size, Step 1, need only be roughly approximate. To give a final design with the approximation on the safe side the tentative selection should have a moment of inertia greater than the final selection and such will always be the case by the procedure given.

The design procedure for unsymmetrical cases may be outlined as follows:

1. Same as for the symmetrical case.
2. Calculate the maximum moment by Equation 20 for moderate lack of symmetry or by graphical construction for doubtful cases and select required beam size.
3. Same as for the symmetrical case.

It should be noted that the structure is to be considered unsymmetrical only if the columns into which it frames have different stiffness factors. Since no advantage is taken of adjoining beams the beam in an outside panel may be considered symmetrical if the columns are equal in size. The preceding design methods will be applied to illustrative cases in a succeeding section.

The proposed design method applies only to the beams or girders which frame directly into the columns of a frame. The seat and top angle type of connection usually are not used for connecting floor beams to main girder beams. Although there should be possibilities of economy in designing such beams for partial continuity such a treatment is outside the scope of the report.

DESIGN OF COLUMNS

An experimental and theoretical investigation on the subject of column design is now in progress at the Fritz Engineering Laboratory under the sponsorship of the American Institute of Steel Construction. It is expected that this work will be completed about a year from now. In view of these facts the scope of this paper will be limited to beam design. The information obtained in this investigation should be of value at a later date in connection with the problem of column design.

Designing the beams for partial continuity as herein proposed will increase moments in the columns by only a few per cent. Column design procedures which are now

safe will have relatively the same degree of safety, therefore, when used in conjunction with the proposed method of beam design, provided the same type of connection is used. If a more rigid type of connection is used, such as the seat angle and top plate connection, the moments in the columns will be higher than by the proposed method even if no advantage is taken of continuity.

Columns are usually designed for vertical loads alone, whereas the tests of the frame show the presence of relatively large bending moments introduced by unbalanced end moments in the connecting beams. Such moments are present regardless of whether or not the beams are actually designed for end restraint or not. The British Steel Structures Research Committee[7] reports measured column stresses due to bending in actual buildings of riveted construction which exceed the yield point of the material. This Committee recommended procedures of design which take account of the moments introduced by continuity.

In view of these facts it may be concluded that changes in column design methods may possibly be advisable, but the consideration of such changes will be left to a later report.

DESIGN PROCEDURE

The procedures for designing the seat angle, top angle and beams have already been presented under separate headings. The following will summarize briefly all of the steps in the design procedure.

1. Select preliminary beam size for the section modulus required by the maximum simple beam bending monent, M_s.
2. Calculate beam-column stiffness ratio $K_B/\Sigma K_c$ and select final beam size from moment obtained by applying a reduction factor from Fig. 24 to M_s.
3. Assume a top angle length not greater than the width of the beam flange nor 0.8 the column depth and determine the end moment per inch for fifty per cent connection restraint.
4. Select top angle size and thickness from Figs. 19*a* or 19*b* and check connection rigidity "*J*" by Figs. 21*a* or 21*b*. "*J*" should be more than $2I/l$ and preferably not more than $6I/l$. $8I/l$ may be taken as an upper limit.
5. Select a trial length of seat angle 1 to 2 in. longer than the width of beam flange and determine end reaction per inch length of seat angle. The end reaction should be taken as the simple beam reaction plus M_F/l to allow for unbalanced moment.
6. Determine the required seat angle thickness from Fig. 16*a* and select trial seat angle size.
7. Determine average end reaction per inch length of vertical weld and select weld fillet size from Fig. 16*b*.
8. Determine horizontal thrust on seat angle by Equation 9 and design horizontal welds connecting the beam flange to the seat angle.

In actual design practice involving a large number of beams the entire procedure may be carried out in tabular form on a single sheet and reduced to a fairly rapid procedure.

The following illustrative examples will be presented in somewhat greater detail than would be necessary after a familiarity with the design method is obtained.

Case 1—Beam has 20-ft. clear span between column flanges and carries a combined dead and live load of 2 kips per foot. Columns are 10-in. *WF* at 49 lb. and the distance between floors is 12 ft.

(Symmetrical structure and load)

Simple Beam Bending Moment $= \frac{2 \times 20^2 \times 12}{8} =$ 1200 in.-kips

Required Section Modulus for Simple Beam Design

$$S = \frac{1200}{20} = 60$$

Selection as Simple Beam 16-in. *WF* at 40 lb.

$$S = 64.4$$

$$I = 515.5$$

Stiffness Ratio $K_B = \frac{I_B}{l} = \frac{515.5}{240} = 2.15$

$$\Sigma K_c = \frac{\Sigma I_c}{l} = \frac{2 \times 272.9}{144} = 3.79$$

$$\frac{K_B}{\Sigma K_c} = \frac{2.15}{3.79} = 0.567$$

Reduction Factor from Fig. 24 is 0.740.

Required Section Modulus $= 0.740 \times 60 = 44.4$

Beam Selection 14 *WF* at 34 lb.

$$S = 48.5$$

$$I = 339.2$$

(Note: The most economical selection is a 15*M* at 33 but the narrow width of the flange is found to require too stiff a top angle)

Top Angle Length = 6.75 in. = flange width

End Moment per inch of Top Angle $= \frac{1200}{3 \times 6.75} = 59.3$ in.-kips per in.

(Note: End moment for fifty per cent restraint, beam uniformly loaded, is $M_s/3$)

Using Fig. 19*b* at top angle size of $3\frac{1}{2}$ x $3\frac{1}{2}$ x 1 in. is required.

Using Fig. 21*b*, the stiffness factor *J* of the connection is calculated as:

$$J = 1.71 \times 6.75 = 11.53$$

Upper Limit of $J = \frac{8I_B}{l} = \frac{8 \times 339.2}{240} = 11.30$

(Note: *J* is about 2 per cent over the upper limit for 80 per cent connection rigidity and the actual rigidity by Equation 13 is 80.4 per cent. The example is introduced purposely to show a case at the upper limit of span length, load and end connection stiffness)

Choose Seat Angle Length as $6.75 + 1.25 = 8.0$ in.

End reaction $= 20 + \frac{400}{240} = 21.67$ kips

End reaction per inch of angle $= \frac{21.67}{8} = 2.71$ kips per in.

Required Bearing Distance by Equation 4

$$N = \frac{21.67}{0.287 \times 24} - \frac{15}{16} = 2.21 \text{ in.}$$

Required Seat Angle Thickness from Fig. 16*a* is $\frac{5}{8}$ in.

Choose a 6 x 4 x $\frac{5}{8}$-in. angle.

Vertical Fillet Welds Between Column Flange and Seat Angle:

Avg. reaction per inch $= \frac{21.67}{12} = 1.81$ kips per in.

$$\frac{N}{l} = \frac{2.21}{\frac{5}{8}} = 3.54$$

TABLE 4

SPAN LENGTH FEET	TOTAL LOAD KIPS	SIMPLE BEAM MOMENT	REQD. SECTION MODULUS	BEAM SELECTION AS SIMPLE BEAM	REDUCTION FACTOR "F"	REQD. SECTION MODULUS	BEAM SELECTION	PERCENT SAVING IN WEIGHT	TOP ANGLE SIZE	% END RESTRAINT
(1)	(2)	(3)	(4)	(5)	(6)	(7)	(8)	(9)	(10)	(11)
17	23.1	590	24.5	12B22	.696	17.1	12B16.5	25.0	$3\frac{1}{2}\times3\frac{1}{2}\times\frac{15}{16}\times4$	80+
17	28.9	737	36.9	14WF30	.718	26.5	12WF25	16.7	$3\times3\times\frac{13}{16}\times6\frac{1}{2}$	79
17	34.7	885	44.3	15M33	.730	32.3	12WF28	15.1	$3\frac{1}{2}\times3\frac{1}{2}\times\frac{15}{16}\times6\frac{1}{2}$	77
18	25.9	700	35.0	12WF28	.704	24.6	12B22	21.4	$3\frac{1}{2}\times3\frac{1}{2}\times\frac{1}{4}$	78
18	32.4	875	43.8	15M33	.728	31.9	12WF28	15.1	$3\frac{1}{2}\times3\frac{1}{2}\times\frac{15}{16}\times6\frac{1}{2}$	78
18	38.9	1050	52.5	16WF36	.738	38.7	14WF30	16.7	$3\frac{1}{2}\times3\frac{1}{2}\times\frac{15}{16}\times6\frac{1}{2}$	79
19	28.9	824	41.2	14WF30	.714	29.4	12WF25	16.7	$3\frac{1}{2}\times3\frac{1}{2}\times\frac{7}{8}\times6\frac{1}{2}$	78
19	36.1	1030	51.5	16WF36	.735	37.9	14WF30	16.7	$3\frac{1}{2}\times3\frac{1}{2}\times\frac{15}{16}\times6\frac{1}{2}$	79
19	43.3	1233	61.7	16WF40	.740	45.7	15M33	17.5	$3\frac{1}{2}\times3\frac{1}{2}\times1\times5\frac{1}{2}$	77
20	32.0	960	48.0	15M33	.722	34.6	12WF28	15.1	$3\frac{1}{2}\times3\frac{1}{2}\times\frac{15}{16}\times6\frac{1}{2}$	80
20	40.0	1200	60.0	16WF40	.740	44.4	14WF34	15.0	$3\frac{1}{2}\times3\frac{1}{2}\times1\times6\frac{1}{2}$	80+
20	48.0	1440	72.0	16WF45	.748	53.9	16WF36	20.0	$3\frac{1}{2}\times3\frac{1}{2}\times1\times7$	80

Fillet Weld Size from Fig. 16*b* is $^5/_{16}$ in.

Horizontal Fillet Welds Between Beam Flange and Seat Angle:

Horizontal thrust from Equation 9.

$$P = \frac{400}{14 + 1.75} = 25.4 \text{ kips}$$

Assuming Welds Effective for $3^1/_2$ in.

Avg. shear $= \frac{25.4}{7} = 3.62$ kips per in.

Fillet Weld Size Required:

$\frac{3.62}{9.60} = 0.377$ Use a $^3/_8$-in. fillet weld $3^1/_2$ in. long on each side of beam flange.

Case 2—(Unsymmetrical structure and load) Beam *A*-*B* has an 18-ft. clear span and at *A* frames into the flange of a 10 x 10 *WF* 49-lb. column while at end *B* it frames into the web of the same size column. The beam carries concentrated loads at the third points, the load nearest *A* being 21 kips and that nearest *B* being 15 kips. Distance between floor levels is 12 ft.

Simple Beam Reactions $V_A = \frac{2}{3}(21) + \frac{1}{3}(15) = 19$ kips

$$V_B = \frac{1}{3}(21) + \frac{2}{3}(15) = 17 \text{ kips}$$

Maximum Simple Beam Moment $M_s = 19 \times 6 \times 12 = 1368$ in.-kips

Required Section Modulus $= \frac{1368}{20} = 68.4$

Simple Beam Selection = 16 *WF* 45 lb.

$S = 72.4$

$I = 583.3$

Fixed End Moments for one hundred per cent rigidity (see steel handbooks for equations):

$$M_{FRA} = \left[\frac{21 \times 6 \times 12^2}{18^2} + \frac{15 \times 12 \times 6^2}{18^2}\right] \times 12 = 912 \text{ in.-kips}$$

$$M_{FRB} = \left[\frac{21 \times 12 \times 6^2}{18^2} + \frac{15 \times 6 \times 12^2}{18^2}\right] \times 12 = 818 \text{ in.-kips}$$

Stiffness Ratios

Beam: $K_b = \frac{583.3}{216} = 2.70$

Column at *A*: $\Sigma K_c = \frac{2 \times 272.9}{144} = 3.79$

at *B*: $\Sigma K_c = \frac{2 \times 93.0}{144} = 1.29$

Minimum End Moments by Equation 16

$$M_{FA} = 912\left[\frac{1}{2 + \frac{2.70}{3.79}}\right] = 336 \text{ in.-kips}$$

$$M_{FB} = 818\left[\frac{1}{2 + \frac{2.70}{1.29}}\right] = 200 \text{ in.-kips}$$

Maximum Moment = 1368 − 291 = 1077 in.-kips

Required Section Modulus $= \frac{1077}{20} = 53.9$

Use a 16-in. *WF* at 36 lb.

$S = 56.3$

$I = 446.3$

Design of top and seat angles follows the same procedure as given for Case 1. The connections at *A* and *B* are designed for fifty per cent of the full fixed end moments, or for 456 and 409 in-kips, respectively. The seat angles at *A* and *B* should be designed for 21.1 kips and 18.9 kips, respectively.

ECONOMY EFFECTED BY PROPOSED DESIGN PROCEDURE

In the two cases presented in the preceding section the saving in beam weight effected by the proposed method in comparison with simple beam design amounts to fifteen per cent for Case 1 and twenty per cent for Case 2. In order to obtain a better average of the saving which might be expected trial designs were made for beams of 17, 18, 19 and 20 ft. in length and for total floor loads corresponding to 80, 100 and 120 lb. per sq. ft. Each beam was assumed to frame into 10 x 10 *WF* 49-lb. columns and was assumed to carry a full panel load uniformly distributed. The results of these designs are

summarized in Table 4. The average saving in weight of beams for these twelve designs is 17.6 per cent and the minimum saving in any single beam is 15 per cent.

The net saving in cost produced by decreased beam weight may be offset to some extent by the additional welding required in the end connections. If fairly rigid end connections are desired to assist in resisting lateral loads, the full saving may be realized, but if the top angles are added only to provide stability they may be made very light and flexible. The cost of field welds is uncertain and variable and no definite conclusions along these lines will be attempted in this report.

SUMMARY AND CONCLUSIONS

1. Results of tests of all-welded building frames are presented to show that methods of analysis which take account of the partial rigidity of the connections are a satisfactory guide to the behavior of the frame.
2. The rigidity of the joints in a frame may be determined satisfactorily by a test of a representative sample joint.
3. A simplified design method is presented for the beams and beam-column connections of a building frame under the action of vertical loads.
4. The application of the seat and top angle connection herein described is limited to a maximum span length of about 20 ft. and a maximum total load for this span length of about 50 kips.
5. The bending of the columns and partial rigidity of the connections under critical conditions of loading are taken account of in developing the method of design.
6. The proposed design method is more closely allied to the actual behavior of the frame than the usual procedure of designing beams as simply supported.
7. Approximations made in developing the design method are on the side of safety.
8. A saving in weight of beams of between fifteen and twenty per cent is effected by the proposed design method, but this economy will be offset to some extent by increased cost of end connections.
9. Procedures similar to those presented may be developed for other types of welded joints.
10. Further tests to develop methods for determining strength and rigidity of other types of welded joints are desirable.
11. The present report does not cover column design, but column design procedures which are now safe will also be safe when used in conjunction with the proposed procedures for beam design.
12. The question of column design for moments as well as direct stress should also be the subject of further study.

REFERENCES

1. Inge Lyse and Norman G. Schreiner, An Investigation of Welded Seat Angle Connections, AMERICAN WELDING SOCIETY JOURNAL, **14** (2), 1–15 (1935).
2. Heath Lawson, The Design of Welded Seat Angle Connections, AMERICAN WELDING SOCIETY JOURNAL, **14** (6), 23 (1935).
3. Wilbur M. Wilson, Tests to Determine the Feasibility of Welding the Steel Frames of Buildings for Complete Continuity, AMERICAN WELDING SOCIETY JOURNAL, **15** (1), 28–38 (1936).
4. Inge Lyse and G. J. Gibson, Welded Beam-Column Connections, AMERICAN WELDING SOCIETY JOURNAL, **15** (10), 34–40 (1936).
5. Inge Lyse and G. J. Gibson, Effect of Welded Top Angles on Beam-Column Connections, **16** (10), 2–9 (1937).
6. Inge Lyse and E. H. Mount, Effect of Rigid Beam-Column Connections on Column Stresses, AMERICAN WELDING SOCIETY JOURNAL, **17** (10), 25–31 (1938).
7. First, Second, and Final Reports of the Steel Structures Research Committee of the Department of Scientific and Industrial Research of Great Britain.
8. Bruce Johnston and E. H. Mount, The Analysis of Building Frames with Semi-Rigid Connections, unpublished at date of writing.

CONTACT RESISTANCE IN Spot Welding*

By FRANK J. STUDER†

THE actual importance of contact resistance in the spot welding process has not proved to be easy to determine. This is because the characteristics of contact resistance under welding conditions have not been known. The opinion is often expressed that surface resistance is, in most cases, necessary for effective welding by this method, since it brings about the production of heat right at the boundary where welding occurs. On the other hand, it has been the feeling of some that boundary resistance really plays a rather minor part in the welding process; that under conditions where it is high enough to be of any consequence, it produces too localized heating, which results in a very thin weld nugget of low strength. The present study was undertaken to determine the characteristics of contact resistance under circumstances that occur in spot welding, from which it should be possible to tell more about its action during the welding cycle.

* To be presented at Annual Meeting, A. W. S., Chicago, Oct. 23 to 27, 1939. Contribution to Fundamental Research Division, Welding Research Committee.

† Union College, Schenectady, N. Y.

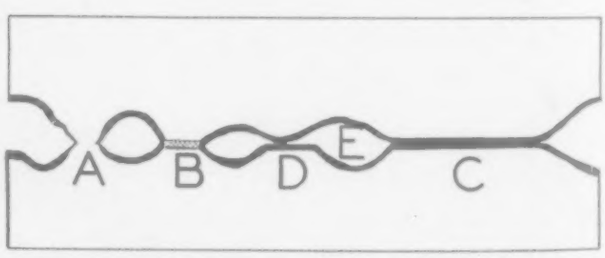


Fig. 1—Indicating Nature of Metallic Contact Between "Flat" Surfaces

THE NATURE OF CONTACT RESISTANCE

Electrical resistance across the boundary between two conducting surfaces may be accounted for in two different ways.

(1) The concentration of current through minute areas of intimate true metallic contact at the bounding surface.

(2) The persistence of a thin film of foreign material

between the surfaces of actual metal, these films being water vapor, oxide, oil or other foreign substance.

It seems likely that in the usual case both of these contribute to the contact resistance. If two flat metal surfaces of ordinary finish are pressed into contact, they will, in general, touch initially at very few points (normally three if resting freely, and one, if held in guides). As the force is increased, these points will yield, first elastically, then plastically, and new points will come into contact. This will continue until there is enough total area to balance the force without further yielding. When this condition has been reached, the probable situation at the boundary is that indicated in Fig. 1, with the following types of "contact" areas.

(1) Regions A, where the yield point of the material has been passed, and the surface films have been ruptured, so that there is intimate metallic contact.

(2) Regions C, where the surface films of the two surfaces are pressed together, but in such a way that they remain intact, and maintain a thickness of a fairly large number of molecules.

(3) Regions B, where the yield point of the metal has been exceeded, and the molecules of surface film have been pressed into and mingled with the metal, producing an area of poor conductivity, as compared to the A regions, yet good with respect to the unruptured film.

(4) Regions D which are pressed together, but where the elastic limit of the metal has not been reached. Here again the surface film may be ruptured, or left intact. The conductivity characteristics will be the same as areas of type *A*, *B*, *C*, respectively.

(5) Regions E where there is an actual air layer, many molecules thick between the samples. Not all types of contact need, of course, be present in any given case.

From considerations of this kind one might anticipate

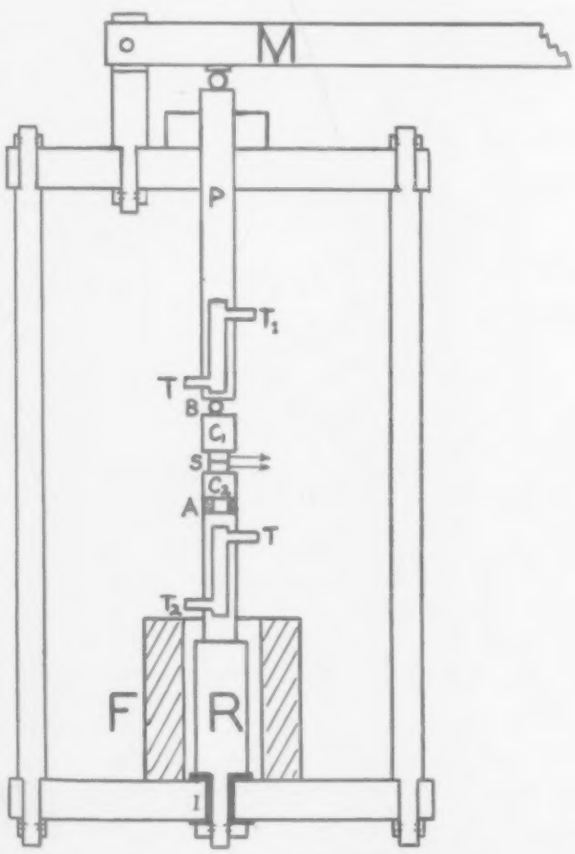


Fig. 2—Experimental Arrangement

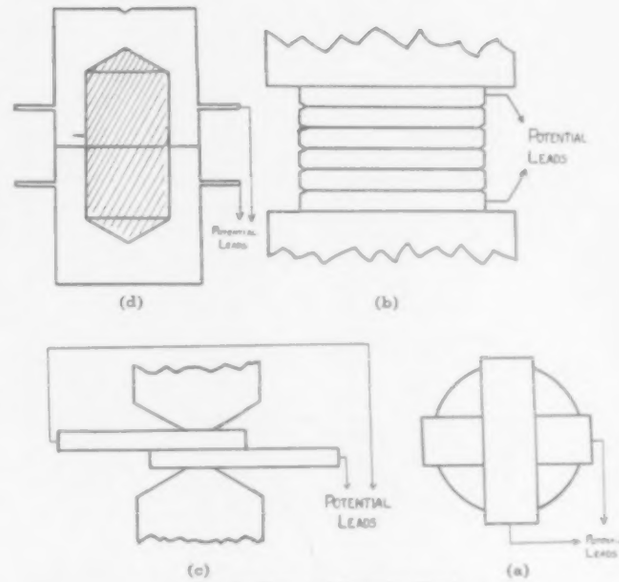


Fig. 3—Types of Samples Used

that the behavior of contact resistance is likely to be rather complex, and certainly that has been found to be true. An extensive fundamental study, on the basis of a picture something like the one given, has been made by Holm and his collaborators, in an attempt to formulate a comprehensive theory. Resistance due to areas of type A, Holm[2] calls "sieve" resistance, and he shows that its value M for a circular area of radius a is given by $M = \frac{\rho}{2a}$ where ρ is the resistivity of the metal. Resistances due to areas of type B and C he calls contact transition resistance, the former (for circular areas) having a value

$$R_1 = \frac{y \cdot \rho_s}{\pi a^2}; \quad \text{the latter} \quad R_2 = \frac{f(y)}{\pi a^2}$$

where y is the thickness of the surface film, ρ_s the specific resistance of the film, a the radius of the elemental contact region and $f(y)$ a function to be determined. The contact transition resistances are both inversely proportional to a^2, while the sieve resistance is inversely proportional to a, so that as a increases, the former lose their importance, if all three types are present. Most of Holm's experimental work was carried out using contact between small crossed cylinders, under idealized conditions.

The present report does not attempt to develop the theory, but is given rather to finding just how contact resistance behaves under conditions which most commonly occur in spot welding.

In a general consideration of the problem, the following specific questions immediately arise.

(1) How does the contact resistance depend upon the way the surfaces are held together?

(a) What effect has the area of overlap of the contacting surfaces?

(b) How does the resistance change with the area of the electrodes?

(2) How does the contact resistance of a given material depend upon the condition of the surfaces?

(a) How widely does its value change from point to point on apparently homogeneous surfaces?

(b) How does its value change from one type of surface to another?

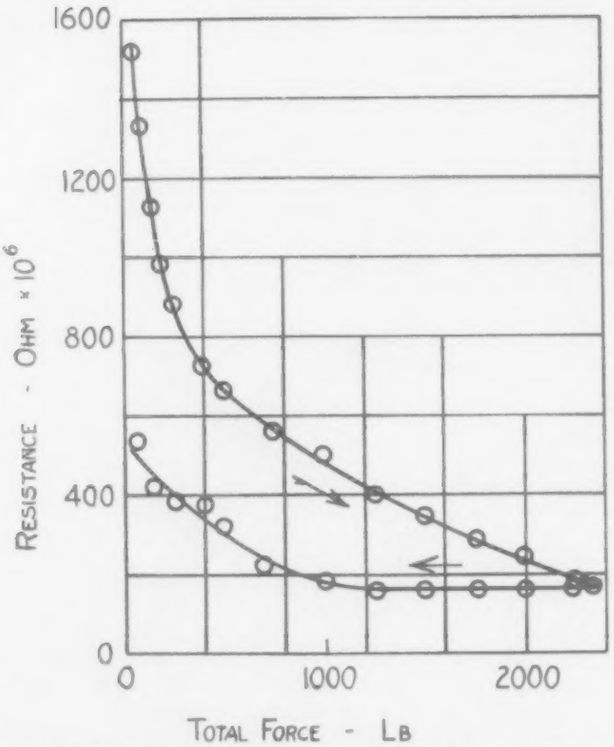


Fig. 4—Typical Pressure-Resistance Curve. 18–8 Stainless Steel, Polished Finished, Samples as in Fig. 3*a*.

(3) How does the contact resistance depend upon the force applied to the contacting surfaces?

(4) How does the contact resistance change with the temperature of the surfaces?

The answers to these questions, added to a knowledge of the temperatures developed in a weld, should tell us a great deal about just what importance contact resistance has in the process. This report gives the results of an attempt to find these answers.

EXPERIMENTAL ARRANGEMENTS

It seems desirable at the beginning to give a general description of the experimental arrangements that were used in the measurements. The method used is illustrated in Fig. 2. The samples to be studied were placed between the end surfaces of the hardened tool steel cylinders C_1 C_2, and the pressure was applied by means of a lever M (mechanical advantage 23) through the cylindrical steel plunger P, and the steel ball B. The ball B had two purposes, first, to bring about as even a distribution of pressure in the sample as possible, and second, in the work which was conducted at elevated temperatures, to minimize heat conduction away from the samples through P. In the high temperature experiments, a ring of rock asbestos A was inserted below C_2 to reduce the heat flow downward. The supports P and R were both made hollow, so that they could be cooled by a stream of water through them, using the inlet and outlet tubes T. When the samples were in place, and the electrical leads attached, the electric furnace F could be raised so that it surrounded them, and then its ends were packed with asbestos. This arrangement permitted observations up to a temperature of 750° C. The temperature was measured by means of a thermocouple resting on the samples.

Current was sent through the samples from a storage battery by connecting to T_1 and T_2 through a variable resistance. (R was insulated from the steel frame by a bakelite washer I.) The resistance (contact resistance plus a part of the body resistance) was determined by measuring the potential drop from S_1 to S_2 using a Wolff low resistance potentiometer in connection with a galvanometer which gave a deflection of 4 mm. when the setting was changed one microvolt. All experiments were carried out with small current (1 to 20 ampere) through the samples. Most of the work was done with one or two amperes, since the resistance was found in general to be independent of the current up to 20 amperes. The potential leads attached to the samples usually consisted of fine wires welded to the samples, though sometimes when convenient, contact was made with small clips. When the leads were welded, every effort was made to minimize the heating of the body of the sample.

The greater part of the present work was done with the usual finishes on stainless steel (18–8) sheets, and low-carbon steel sheets, in order that we would be able to put our results along side those obtained for the identical materials, at the R. P. I. welding laboratory, under the direction of Professor Hess, who was attempting to make measurements of the contact resistance through the actual welding cycle. Various types of samples were used, and most of these are listed below, and indicated in the diagrams of Fig. 3. Care was taken in most cases not to handle the surfaces. The edges were carefully rounded to avoid burrs.

(1) Rectangular strips, usually $^1/_4$ in. wide, and 1 in. long, of various thicknesses. (Fig. 3*a*.) Sometimes small squares $^1/_4$ in. to $^3/_8$ in. on a side. In some experiments a stack of six squares (Fig. 3*b*) was used, and the resistance thus measured through five surfaces in series.

(2) Small flat cylinders, cut out in a lathe from sheet stock, usually $^3/_8$ in. diameter, various thicknesses.

(3) Large overlapping strips (e.g., 1 in. by 2 in.) between electrodes. (Fig. 3*c*.)

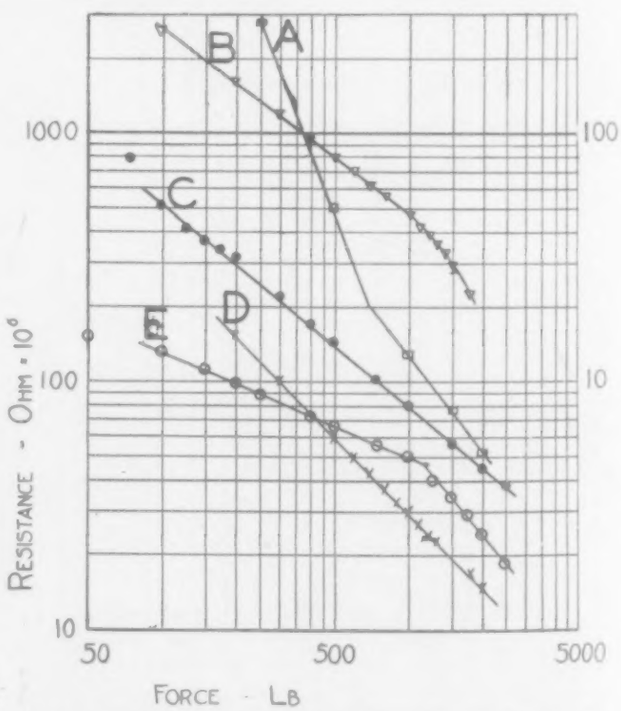


Fig. 5—Typical Log-Log Plots, Pressure-Contact Resistance

A Auto body stock, samples as Fig. 3*a*
B Cold-rolled sheet, low carbon, as Fig. 3*a*
C Aluminum alloy, special samples as Fig. 3*d*
D Cold-rolled steel, special samples, as Fig. 3*d*, cleaned with No. 1 emery cloth
E 18–8 Stainless steel, polished, as Fig. 3*a* (For *A* and *B* use scale at right)

(4) Special shaped samples (Fig. 3*d*), made in this fashion to allow for successive machining and grinding of the surfaces. These samples were easy to line up with accuracy, and the body resistance of the material could be accurately computed. They had the added advantage that potential leads could be welded to the flanges without heating the surfaces that were being studied.

EFFECT ON CONTACT RESISTANCE OF AREA OF OVERLAP AND SIZE OF ELECTRODE TIPS

It is pretty well established from the reports of a number of observers,[3,4,5] that contact resistance is for all practical purposes, independent of the area of overlap of the surfaces. This, of course, does not mean that the contact resistance is constant with changing area, in an absolute sense, but rather that the variation from one area of overlap to a different one is no greater, in general, than the variation for a given area taken at different places on the surfaces. It has also been found[5] that the resistance is unaffected by the area of the electrodes which push the surfaces together. The thickness of the metal sheets used apparently do not change these results. These conclusions were verified in a general way in the present experiments. One point which should have been obvious to consider when the experimental work was being done, was the effect on contact resistance of using crowned electrodes, and it is planned to study it in the future.

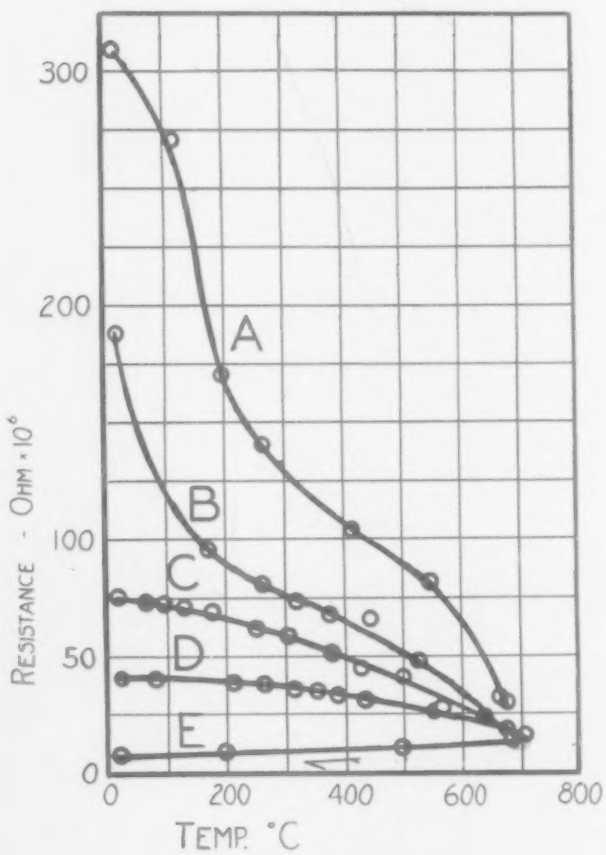


Fig. 6—Contact Resistance—Temperature, 18-8 Stainless Steel

A 250-lb. force, sand-blasted. ¾-in. strips, as Fig. 3*a*
B 1000-lb. force, polished finish, ¾-in. strips, as Fig. 3*a*
D 3000-lb. force, sand-blasted, ¾-in. strips, as Fig. 3*a*
C 1000-lb. force, sand-blasted, ¾-in. strips, as Fig. 3*a*
E Resistance as temp. is reduced, case *B*

Table 1—Example of Contact Resistance Spread (Ohms)

Force (lb.) 250	500	1000	2000	
1240 × 10⁻⁶	865 × 10⁻⁶	530 × 10⁻⁶	265 × 10⁻⁶	Finish
660	392	265	190	2*D*
1000	705	436	263	
811	457	380	196	Finish
1075	708	475	217	2*B*
1280	1050	766	500	
830	570	370	255	Finish
1860	1560	1130	637	4
860	527	340	240	
1405	661	452	261	Finish
441	294	294	218	6
3660	2500	1320	370	

Force (lb.) 250	500	750	
600 × 10⁻⁶	250 × 10⁻⁶	172 × 10⁻⁶	
534	250	168	Sand-blasted
445	229	161	

CONTACT RESISTANCE AND SURFACE CONDITION

In spite of all care to maintain uniform conditions over a surface, the contact resistance is usually considerably different when measured at one position than at another, and indeed, when measured a second time in the same region. Though this is not surprising, of course, when one stops to consider all the variables that are involved, which can scarcely be controlled without putting the samples in a vacuum, still it does give an experimenter a sort of feeling of frustration, if, after making a very careful set of measurements on very carefully prepared surfaces, he attempts to check his results with another set of samples prepared in apparently just the same way, and he finds that the resistance values are approximately halved or doubled! But the contact resistance between ordinary surfaces is as variable as that. This was rather strikingly illustrated in a series of experiments with a sample of cold-rolled steel of the special shape of Fig. 3*d*. The surfaces in this case, after being turned in the lathe, were ground with No. 1 emery between measurements. During the winter the measurements made on these samples were much more consistent than for most conditions that had been studied. The next summer the identical samples, on account of their consistent behavior, were selected for another phase of study, and treated, as far as could be determined, in identically the same way as previously. The values now turned out to be in the neighborhood of twenty times as large, and successive measurements varied widely. The only obvious condition that had changed was that in the winter the atmosphere of the room was extremely dry, while in the summer the relative humidity was very high. It should be pointed out in connection with this example, of course, that the surfaces here dealt with were in each case worked just before measurements were made, so that the rate of oxidation, or film formation, would be strongly affected by atmospheric conditions. Sheets that have been thoroughly seasoned in the atmosphere are not likely to be sensitive to the same changes.

These remarks will serve to point out the futility of attempting to give any sort of accurate value of contact resistance to be associated with the usual metal surfaces. Table 1 is included to illustrate the relative spread of measurements obtained in a typical situation. These results represent a series of consecutive measurements, using rectangular strips 1 in. × ¼ in., cut from samples of stainless steel in various finishes furnished through the

courtesy of the Carnegie-Illinois Steel Corporation. The finishes were designated by the manufacturers as 2*B*, 2*D*, 4 and 6, and are designated in the table.* In addition, some measurements are given for a sample of 18–8 sheet that was sand-blasted. While there is no indication of a difference between the four standard factory finishes, the sand-blasted sample shows a consistently much lower resistance at any given pressure. More extended observations at higher pressures showed also that the sand-blasted surfaces gave a much more constant resistance value than the other types of finish studied.

A rather extensive investigation of contact resistance in the case of low carbon steel (carbon content not exceeding 0.1%) has been made by Sidorenko,[5] and his report gives a great deal of data concerning the variation observed with different types of surfaces. In his paper, Sidorenko gives curves indicating the change in resistance with pressure for nine different types of surface. Below is given a table, taken from his curves, giving the contact resistance sheet to sheet, and electrode to sheet for the case of a force between the electrodes of 440 lb. (200 kg.). For these particular values he used samples 3 mm. thick, having an overlap area of 3 sq. cm. The electrodes were flat, and had a diameter of 8 mm. (see Fig. 3*c*).

Table 2—Contact Resistance (Ohms)

Condition of Surface	Sheet to Sheet	Electrode to Sheet
Pickled in acid	300×10^{-6}	300×10^{-6}
Cleaned, emery wheel	160×10^{-6}	220×10^{-6}
Rusted after cleaning	0.08 ohm	0.076 ohm
Oiled after cleaning	300×10^{-6}	370×10^{-6}
Covered with scale	0.08 ohm	0.10 ohm
Covered rust and scale	0.50 ohm	0.36 ohm
Machined with cutter	1200×10^{-6}	1300×10^{-6}
Smoothed with file	280×10^{-6}	430×10^{-6}
Ground with emery	110×10^{-6}	140×10^{-6}

While these values can, of course, be considered only indicative, on account of the wide variation which occurs between surfaces which have had apparently identical treatment, they will serve to give an idea of the order of magnitude or the changes in resistance from one kind of surface to another.

CONTACT RESISTANCE AS A FUNCTION OF PRESSURE

It has long been known that the contact resistance between two metallic surfaces is reduced by increased pressure.[3] From the conception of surface contact indicated in Fig. 1, one would predict that an increase in pressure would bring about a decrease in contact resistance, since an increase in pressure will tend to further flatten out the small surfaces of intimate contact, thus reducing both the sieve resistance and the surface transition resistance.

The relation between pressure and resistance has been variously reported. Some data given fits with surprising accuracy the relation $R = \frac{C}{P}$ where R is the contact resistance, P the pressure and C is a constant. Other data obeys the formula $R = \frac{C}{P^{1/2}}$. Some do not follow a simple power relation at all.

Figure 4 gives a typical pressure-resistance curve, for increasing and decreasing pressures similar to many taken

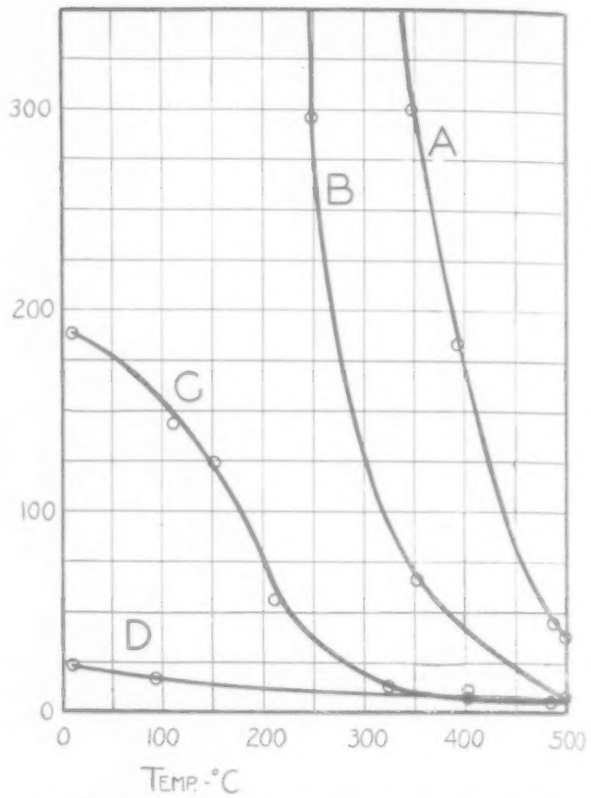


Fig. 7—Contact Resistance—Temperature, Low-Carbon Steel Samples ¼ Inch Square, in Stacks of 6, as Fig. 3*b*

A 50-lb. force (From 3550×10^{-6} ohm at room temp.)
B 250-lb. force (From 2800×10^{-6} ohm at room temp.)
C 750-lb. force *D* 3750-lb. force

in connection with the present work. This particular curve refers to data taken with samples as in Fig. 3*a*, of stainless steel (18–8), polished. Curves for mild steel are very similar, except that the resistance falls off more rapidly for the initial pressures. It is to be noted that with decreasing pressure, the resistance does not increase until the pressure has been greatly reduced, and even at the lowest value of pressure, the resistance returns to only a fraction of its original value.

In Fig. 5 is given a log-log plot for a number of typical cases taken from a large number of experiments. A great many of these, it will be noted, plot into a straight line over the greater part of the pressure range studied, indicating a simple power relation over that range. Only a few followed the inverse first power relation though those which did, followed it with surprising fidelity. The cold rolled steel, special sample (Fig. 3*d*), cleaned with No. 1 emery cloth, is notable in this respect. It is to be remarked that samples of the same material, with the same finish may give pressure-resistance curves of quite different slopes on the log-log plot.

Consideration of the results of other observers shows similar variations. Harris[3] gives data which shows very close obedience to the reciprocal law, for clean copper contacts. Enger[6] gives results for copper which more nearly obey the inverse half power law. Crampton and Vreeland's[7] observations on various kinds of brass, show about half the samples obeying the inverse first power law, fairly well, the other half varying widely from it.

From the point of view of welding application, the following points may be worth noting. The initial contact resistance decreases with the electrode pressure, and can therefore be controlled within limits. The contact resistance varies rather widely from one point on a sur-

* 2*D*—Full finish (Dull cold-rolled)
2*B*—Full finish (Bright cold-rolled)
4 —Standard polish on one side or both sides
6 —Standard polish Tampico Brushed

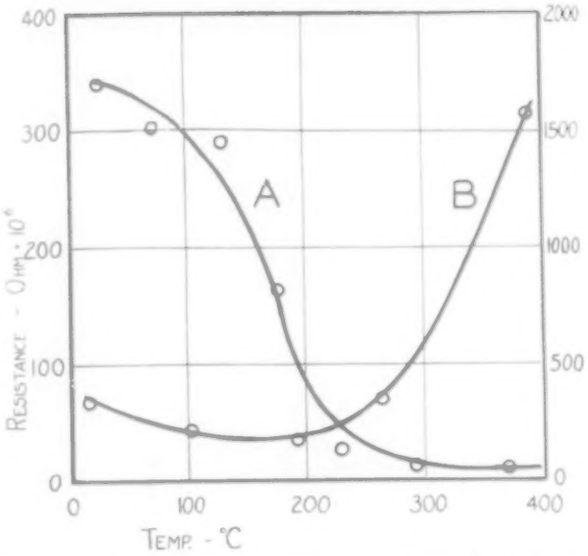


Fig. 8—Contact Resistance—Temperature, Aluminum Alloy Samples as Fig. 3*a*

Samples as Fig. 3*d*

A Aluminum on aluminum, 250-lb. force. (Resistance scale at left)
B Aluminum on cold-rolled steel, 250-lb. force. (Resistance scale at right)

face to another for most types of finish, but the variation is less at increased pressures. If it should be found desirable to have a relatively high initial contact resistance this could be done by starting the weld at low pressure, following up with an increase in pressure. If, on the other hand, it should be useful to make the weld with low initial contact resistance, and low pressure on the electrodes, this can be accomplished by applying a high pressure, and reducing it before the welding current is turned on. The contact resistance is diminished by the high pressure, and remains at a low value even when the pressure is to a large extent relieved.

DEPENDENCE OF CONTACT RESISTANCE UPON TEMPERATURE

It seems likely that any influence that temperature may have on contact resistance is due to one or another of the following factors.

(1) Lowering of the yield strength with increasing temperature. This would bring about an increase in the the size of the areas of intimate contact as the temperature rose.

(2) Driving off of surface films, such as adsorbed water vapor.

(3) Accelerating the formation of oxide film over the surfaces which might creep into regions of intimate metallic contact.

(4) Increasing the resistivity of the material itself, thereby increasing the sieve resistance.

The first two of these would bring about a decrease in the resistance of the contact, the latter two an increase, as the temperature was raised.

Direct measurement showed that in general the contact resistance decreased rapidly with increasing temperatures. The experiments were carried out by bringing a cylindrical electric furnace around the samples. In the arrangement indicated in Fig. 1, the samples were put in place, and all the connections made. Then the furnace F. was raised so that if surrounded the samples, after which the ends were packed with asbestos. Stainless steel, mild steel and an aluminum alloy were studied in this way. The more important results are given in the curves of Fig. 6, Fig. 7 and Fig. 8. Values here given are corrected for the body resistance of the sheets.

In Fig. 6, curves *A*, *C* and *D* refer to sand-blasted samples of 18–8 stainless steel, under different pressures. Curve *B* gives results for a polished sample. *E* shows the change in resistance as the temperature is decreased from a high value indicating that the contact resistance does not increase again on cooling. Figure 7 gives results for low-carbon steel, taken from standard cold-rolled sheets. The pressure was varied from 50 lb. for curve *A*, to 3750 for curve *D*. The curve of Fig. 8*A* was obtained with samples of the type indicated in Fig. 3*d*, of a commonly used aluminum alloy (General Electric designation B-12, H-7) (composition given as chiefly 92% aluminum, 4% copper). Figure 8*B* gives the rather interesting change in resistance observed between this alloy and cold-rolled steel. For this case the resistance fell with increasing temperature, until approximately 200° C. was reached, after which it rose rapidly, suggesting that a high resistance film was formed between the surfaces at high temperatures.

It will be noted that in every case (except Fig. 8*B*) the contact resistance is decreased as the temperature is raised, and that it reaches a very low value already at relatively low temperatures—far below the fusion range of the metals. This is true regardless of the pressure on the surfaces.

It will be noticed, too, that the fall in the value of resistance begins at a temperature well below that at which the metal shows any tendency to soften. It was thought at first that this could be accounted for by the fact that the current, being concentrated through the minute bridges of intimate contact in the surfaces, might cause local heating intense enough to soften the immediately surrounding region, thus bringing about local yielding, which would lower the resistance. However, if

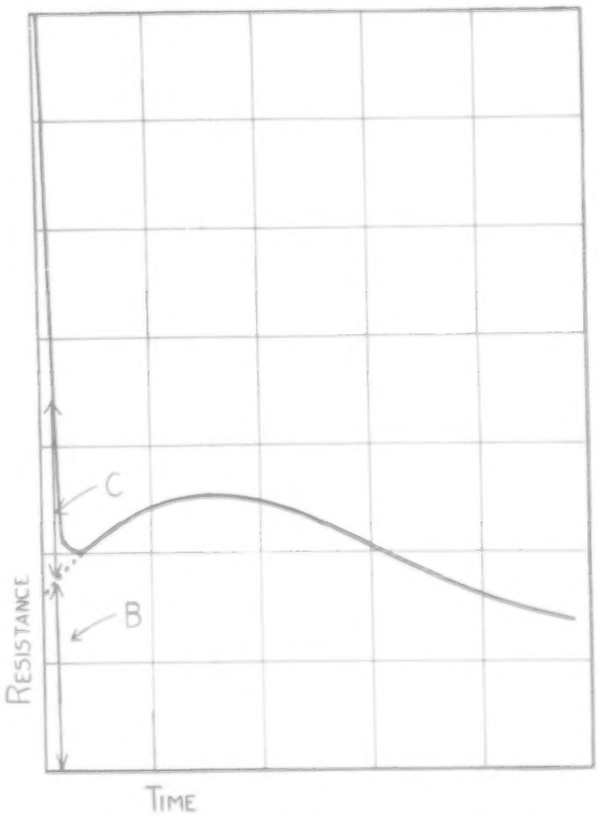


Fig. 9—Probable Changes in Resistance Through a Weld

B—Body resistance C—Contact resistance

this were true, and increase in the current by itself should cause the surface resistance to fall, and actual trial showed that no such effect occurred with changes of current from 0.1 ampere to 20 amperes.

It seems very likely, therefore, that the decrease in resistance with temperature is due to the driving off of a film of some sort (quite possibly adsorbed water vapor[4]) which covered both surfaces. It may be that at room temperature there is such a film layer between a large number, at least, of the actual contact points, and that these are weakened and driven out at higher temperatures.

The question now arises as to what happens to the contact resistance during the making of a weld. The results summarized in Fig. 6 and Fig. 7 doubtless give us an indication. There may be some question, to be sure, as to the effect of the slow rate of temperature rise in these experiments, as compared with the extremely rapid rise that occurs in making a weld. However, other evidence as indicated below, suggests that essentially the same changes occur in both cases.

By making use of oscillographic records, Professor Hess[9] and his collaborators at the welding laboratory at R. P. I., have determined the resistance across a spot weld during the time that it was being made. These measurements showed an increase in weld resistance to a maximum, and then a falling off as the weld was completed. Their results did not, however, include values during the first quarter cycle of the welding current, due to difficulties encountered in measuring the oscillograms in this region. In some later work, not yet published, points have been determined in this interval, and from them it is evident that there is a rapid decrease in resistance in the first quarter cycle.

The values of resistance which Hess measures, include the body resistance of one sheet of the material. Now, referring specifically to Fig. 3 of his article,[9] giving curves for stainless steel with $^1/_8$-inch electrodes, as nearly as can be estimated this sheet resistance is of the order of magnitude of the measured resistance through the weld, indicating that the true *contact* resistance is small. In the case referred to, the sheet resistance at room temperature is 64×10^{-6} ohm; at 1000° C. it would be 105×10^{-6} ohm. This is large enough to entirely account for the weld resistance measured by Hess, and would certainly indicate that after the first quarter cycle the contact resistance became negligibly small. Similar conclusions may be drawn from Dr. Hess' results for $^1/_4$-inch electrodes, and also for low-carbon steel.[9]

This discussion would seem to indicate that the resistance through the weld region in the course of making a weld varies in the fashion indicated in Fig. 9. As the surface is heated by the welding current, the *contact resistance* falls off very rapidly to a negligibly low value, leaving only the body resistance of the material. The time for this to occur is normally a small fraction of a cycle, but it will depend upon the magnitude of the welding current. The rise which is usually observed after the first minimum may be accounted for by the fact that the contact resistance disappears at a relatively low temperature, far below the temperature of fusion, as indicated in Fig. 6. After this the temperature of the metal is raised to the fusion point, with a considerable increase in resistivity. The decrease in resistance which then follows may result from the fusion, which increases the area of intimate contact between the surfaces somewhat beyond the electrode surfaces, or by actual indentation by the electrodes.

It seems likely, in any case, for the metals studied, at least, that any action for which contact resistance is responsible must occur very early in the welding cycle. Just what this action is, and just how important it is have still to be determined, and further experiments are under way in an attempt to do so.

ACKNOWLEDGMENT

The author wishes to extend his appreciation to the Welding Research Committee for the assistance given in this study. He is particularly indebted to Mr. H. M. Hobart, chairman of the Fundamental Research Division, and to Dr. G. E. Claussen, its secretary, for the help they have consistently given so freely. He is indebted, too, for many valuable suggestions, to Mr. E. A. Winzenburger, until recently of the Department of Engineering at Union College, with whom originally a joint paper was planned; and to Mr. M. V. Landon, and Mr. C. H. Guiles for performing much of the experimental work. There are many others to thank for materials, and for helpful discussions, in particular, Dr. R. H. Harrington, Mr. R. C. Gillette and Mr. E. R. Spittler of the General Electric Company, Professor W. F. Hess of R. P. I. and Mr. G. S. Mikhalapov, of the Heintz Manufacturing Company.

BIBLIOGRAPHY

1. Windred, G., *Beama Journal*, **42**, 122, April 1938. At the end of this article will be found a list of 102 articles on contact resistance, which is a fairly complete bibliography.
2. Holm, R., *Wiss. Veröff. aus dem Siemens-Konzern*, **10**, 1 (1931). This article gives references to Holm's previous work and summarizes his results. The article has been translated, and can be made available through the secretary of the FRD.
3. Harris, F. W., *Elec. Journal*, **X**, No. 7, 637 (1913).
4. Hopp, W., *E. T. Z.* **41**, 1920, p. 205.
5. Sidorenko, A. M., "Determination of the Physical Properties of Contacts in Spot Welding of Low Carbon Steels," published by the Academy of Sciences of the Ukrainian S. S. R., Kiev 1938. Translated by the FRD of the Welding Research Committee, and available through its secretary.
6. Enger, A. R., *Elec. Journal*, **21**, 316 (1924).
7. Crampton, D. K., and Vreeland, J. J., AMERICAN WELDING SOCIETY JOURNAL, **16** (10), Research Supplement 9–18, Oct. 1937.
8. Rohman, *Phys. Zeit.*, **21**, p. 417 and 699, 1920.
9. Hess, W. F., and Ringer, R. L., Jr., AMERICAN WELDING SOCIETY JOURNAL, **III**, 10, p. 39, Oct. 1938, and **IV**, 4, p. 113, April 1939.
10. Kochergin, K. A., *Avtogennoe Delo*, **9**, No. 12, 1938. "Change of Contact Resistance in Spot Welding," translated by the FRD, and available through its secretary, Dr. G. E. Claussen. The author attempts to obtain a record of temperature on an oscillograph, at the same time as he records current and voltage, by having a thermo-junction inserted between the strips being welded.

THE EFFECT OF ALLOYING IN Metallic Arc Welding*

By R. W. EMERSON†

INTRODUCTION

AS A RESULT of the ever-increasing number of ferrous alloys being used in the construction of welded products, it is becoming necessary that the welding engineer keep apace in the welding of these alloys.

It is the purpose of this paper to point out and discuss some of the problems involved in the welding of high alloy, and low carbon steels particularly from the standpoint of the alloying produced when a portion of the kerf surface of the base metal is melted and alloyed with the metal deposited from the electrode.

Many combinations of base metals and electrodes were investigated for the purpose of checking this "alloying effect." The results obtained have led to the establishment of some general conclusions regarding the composition of electrodes to use for a given type of steel.

I. METALLURGICAL CONSIDERATIONS

From a metallurgical viewpoint, there are two factors which must be taken into consideration when arc welding ferrous metals. The first factor to be considered is whether or not the steel is responsive to heat treatment, that is, whether welding heat produces a marked change in the physical properties of the heat-affected zone. If the physical properties of the steel can be changed appreciably by the effect of heat from welding, followed by rapid cooling due to conduction of heat away from the weld by the base metal, then precautions must be taken to overcome this difficulty.

The second factor to be considered is the effect of the alloying of the electrode deposit and base metal which is melted during welding. The importance of these two factors depends entirely upon the type of steel being welded and the type of electrode being used. This second factor becomes practically of sole consideration when welding low carbon steels and the high alloy steels such as the austenitic chrome-nickel steels and the ferritic chromium irons.

The steels used in this investigation with the exception of low carbon steel were essentially of the high alloy type and for this reason the alloying effect becomes of primary importance.

If the base metal is welded with an electrode of the same approximate analysis there is no problem of alloying.

In many instances, however, the electrode being used is of a dissimilar analysis to that of the base metal. This is particularly true in the welding of special alloy steels, the welding of non-magnetic inserts into an otherwise magnetic frame, the welding of stainless steel to low carbon steel and, in some instances, the welding of the

* To be presented at Annual Meeting, A. W. S., Chicago, Oct. 23 to 27, 1939. Contribution to Fundamental Research Division, Welding Research Committee.

† Research Engineer, Westinghouse Research Laboratories, East Pittsburgh, Pa.

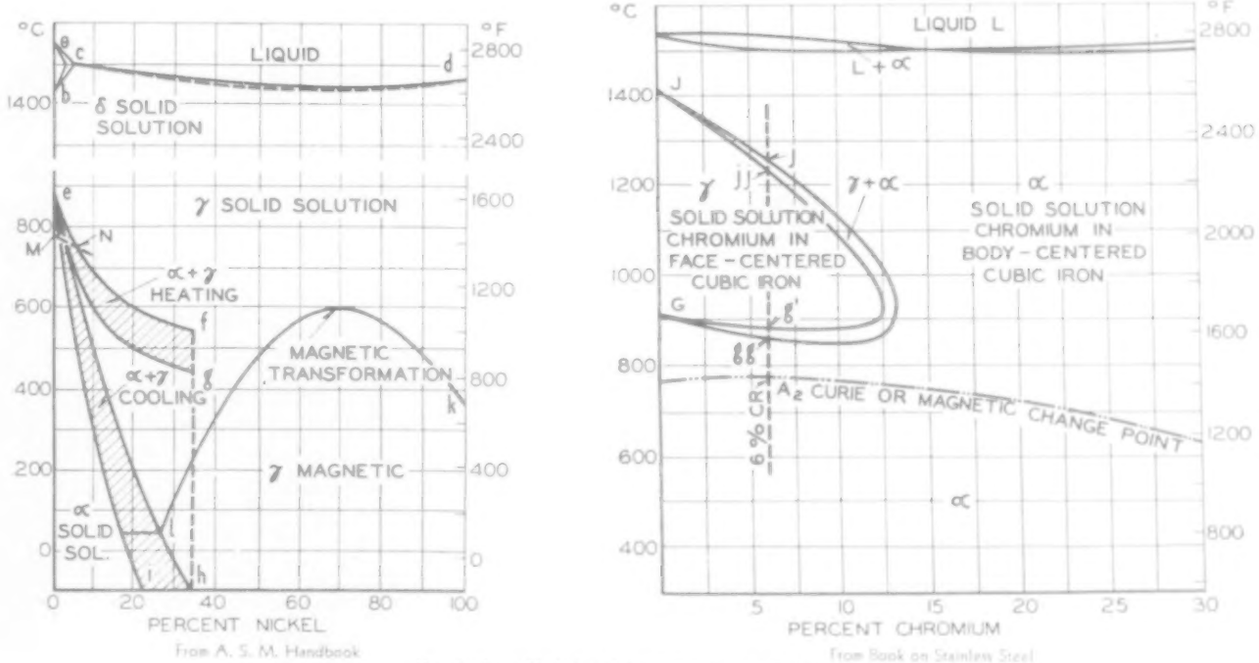


Fig. 1—Iron-Nickel and Iron-Chromium Systems

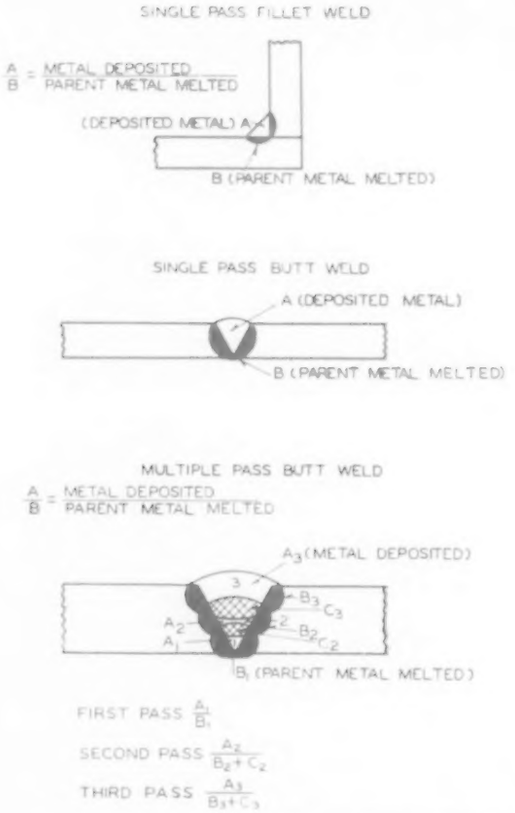

SINGLE PASS FILLET WELD
A / B = METAL DEPOSITED / PARENT METAL MELTED
(DEPOSITED METAL) A
B (PARENT METAL MELTED)
SINGLE PASS BUTT WELD
A (DEPOSITED METAL)
B (PARENT METAL MELTED)
MULTIPLE PASS BUTT WELD
A / B = METAL DEPOSITED / PARENT METAL MELTED
A3 (METAL DEPOSITED)
3
B3 C3
A2
2
B2 C2
A1
B1 (PARENT METAL MELTED)
FIRST PASS A1 / B1
SECOND PASS A2 / B2 + C2
THIRD PASS A3 / B3 + C3

Fig. 2—Effect of Alloying in Different Types of Welds

straight chromium irons. In all such cases there is a definite effect of alloying which must be considered.

II. THEORY OF ALLOYING

Unless the general theory underlying alloy steels is understood, it becomes difficult and quite puzzling to understand why certain combinations of base metal and electrodes are completely satisfactory and others are definitely unsatisfactory.

Iron has two crystalline forms. The body centered cubic lattice, known as alpha iron, is the stable form at temperatures below 900° C. and above 1400° C., and the face-centered cubic lattice, known as gamma iron, is the stable modification at temperatures between 900° and 1400° C. This phenomenon is known as allotropy and the transformation which takes place in iron is known as the allotropic transformation. Nickel has a face-centered cubic lattice and when alloyed with iron in increasing proportions the temperature at which the allotropic transformation takes place is gradually lowered until at approximately 30% nickel the transformation temperature is depressed to room temperature with the result that the alpha phase no longer exists. Chromium has a body-centered cubic lattice as does the alpha modification of the iron. Therefore, with increasing percentages of chromium in iron it is not surprising that the gamma phase becomes gradually less and less until at a percentage of approximately 14 per cent chromium the gamma phase no longer is in existence and we have the so-called high chromium ferritic steels (more commonly called high chromium irons). The iron–nickel and iron–chromium systems are shown in Fig. 1.

Since the stainless irons and stainless steels consist of the iron–chromium and iron–chromium–nickel alloys, it becomes necessary to know both the individual and combined effects of chromium and nickel on iron. Many investigators have studied the iron–chromium and iron–chromium–nickel systems as given in the bibliography.[1–4]

A diagrammatic sketch of a single pass fillet weld, a single pass butt weld and a multiple pass butt weld is shown in Fig. 2. This illustrates the alloying effect produced by melting the base metal adjacent to the weld and indicates the relative percentage of this metal in the weld as compared to the amount of metal deposited from the electrode. Obviously if the base metal and electrodes are of dissimilar analysis, the weld will be of still a third analysis. In fact in the case of a multiple pass butt weld it is to be pointed out that the weld itself will vary in analysis from the bottom bead to the top bead due to the fact that contamination from the parent metal decreases as each successive layer is applied. Referring to the multiple pass butt weld in Fig. 2 it can be observed that the amount of deposited metal is that necessary to fill up the "V" groove made by the two abutting pieces. The darkened area adjacent to the "V," however, indicates the amount of parent metal which is actually melted by the arc and alloyed with the deposited metal. To go a step further it is to be pointed out that the analysis of the first bead will depend on the ratio of A_1 to B_1 (ratio of metal deposited to parent metal melted). The analysis of the second bead, however, will be dependent upon the ratio of A_2 to B_2 plus C_2 (ratio of metal deposited to parent metal melted plus No. 1 weld bead melted). Likewise the analysis of No. 3 weld bead depends upon the ratio of A_3 to B_3 plus C_3. In the single pass welds the light areas shown represent the amount of deposited metal while the darkened areas in the parent metal indicate the amount of parent metal which is melted and alloyed with the deposited metal as a result of the heat from the welding arc. As a result of the extremely high temperature produced by the arc and also

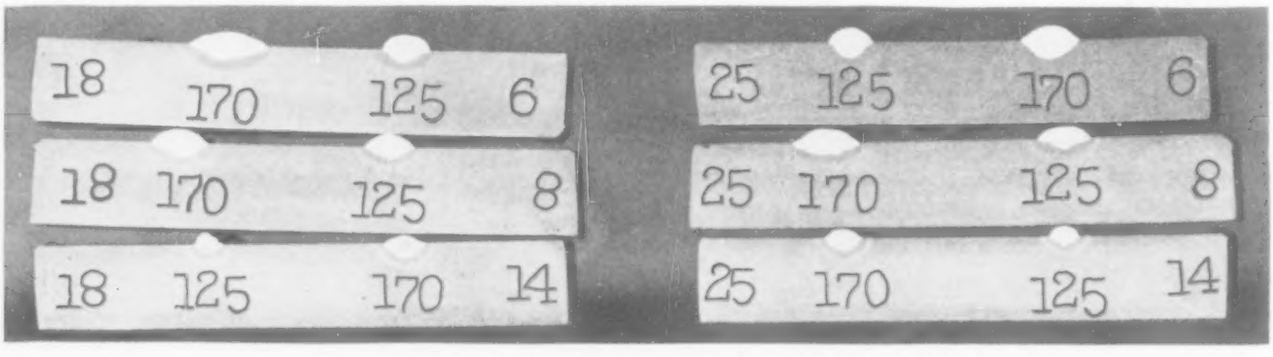


Fig. 3—Stainless Steel Deposits on Low Carbon Steel Plate

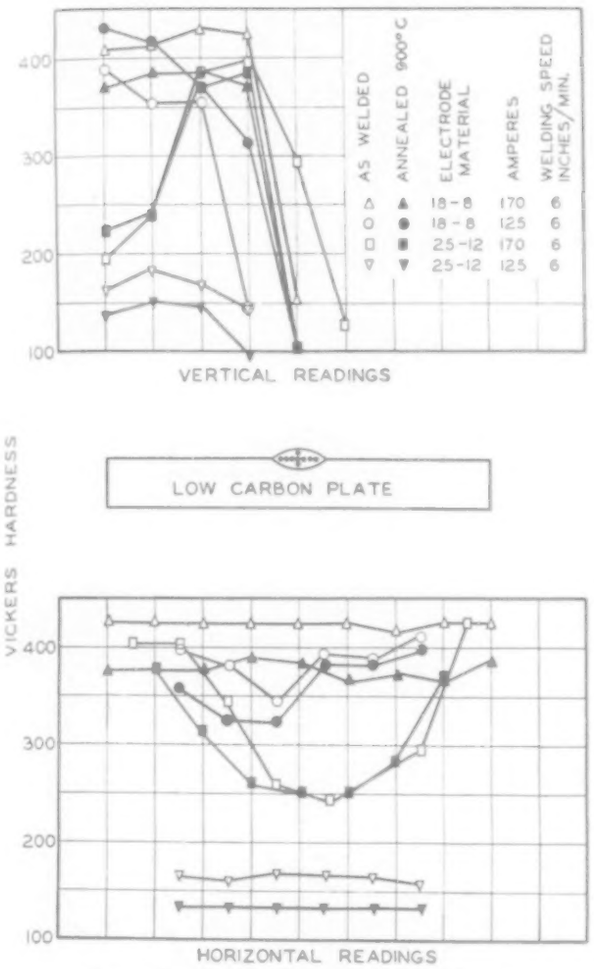


Fig. 4—Hardness Measurements in Bead Deposit

the turbulence of the molten pool of metal deposited by the arc, the effect of alloying (mixing of melted parent metal with the deposited metal) becomes relatively complete. Though there is no doubt that there is a composition gradient from the center of a weld to the edge this is not as great as might be expected.

According to a common assumption with which the writer disagrees, when low carbon or low alloy steel is welded with austenitic stainless steel electrodes, a martensitic band is formed adjacent to the low carbon or low alloy steel being welded with the major portion of the weld consisting of austenite. According to this it would be impossible to make a weld on low carbon steel plate with a 25% chrome–12% nickel austenitic stainless steel electrode without the formation of martensite since a narrow zone in the weld adjacent to the parent metal would be lowered in alloy content to the point where the alloy formed would be martensitic while a major portion of the weld bead would remain as practically undiluted 25–12. *The above concept is definitely incorrect* because, as previously mentioned, the high temperature and turbulence of the molten pool of metal under the arc causes the metal which is melted from the kerf surface to be effectively alloyed with the deposited metal throughout the entire cross section of the bead. Because of this fact, if low carbon steel is welded with 25–12 electrodes and 20–30% of the weld bead is made up of the low carbon steel with the remaining 70–80% being 25–12, the contamination by the low carbon steel is not sufficient to lower the alloy content of the entire bead below that necessary for the maintenance of stable austenite. In some instances this dilution becomes too great and in such cases the entire cross section of the bead will revert to martensite. In general it may be stated that the alloying is sufficiently effective so that whatever the micro-constituent formed, this constituent will be found throughout the entire cross section of the bead.

As can be observed from Fig. 2, however, the amount of contamination from the parent metal in the first bead of a butt weld is much greater than in the succeeding beads. The amount of parent metal in the first bead will be approximately the same as the amount of deposited metal in the bead. The top bead deposited may contain

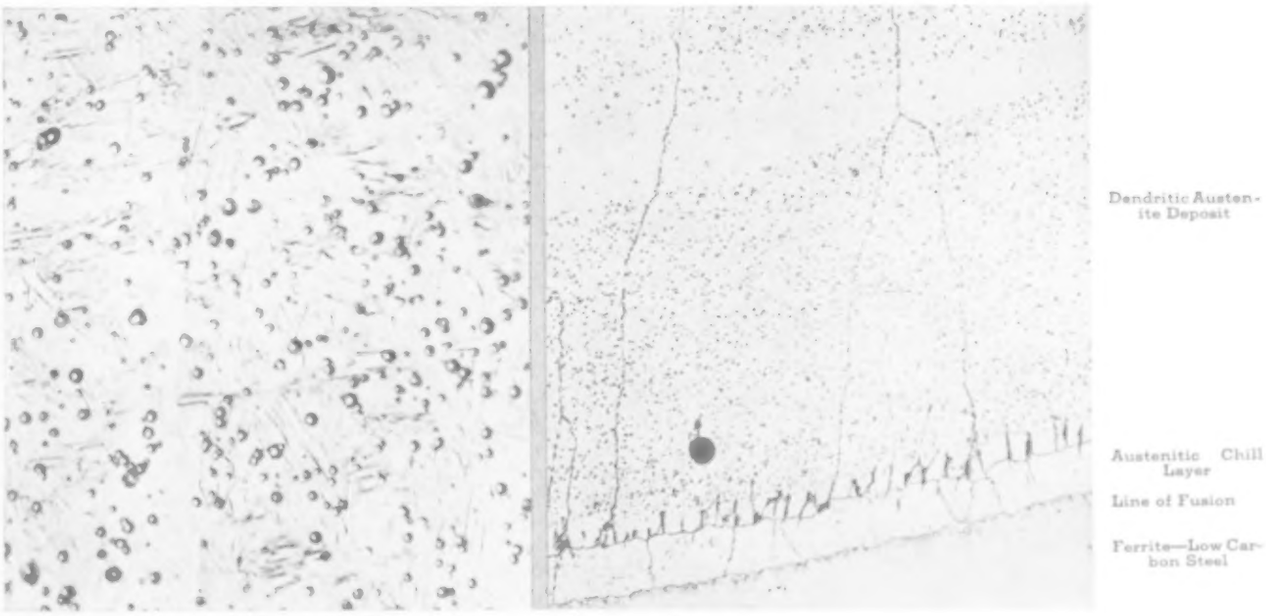
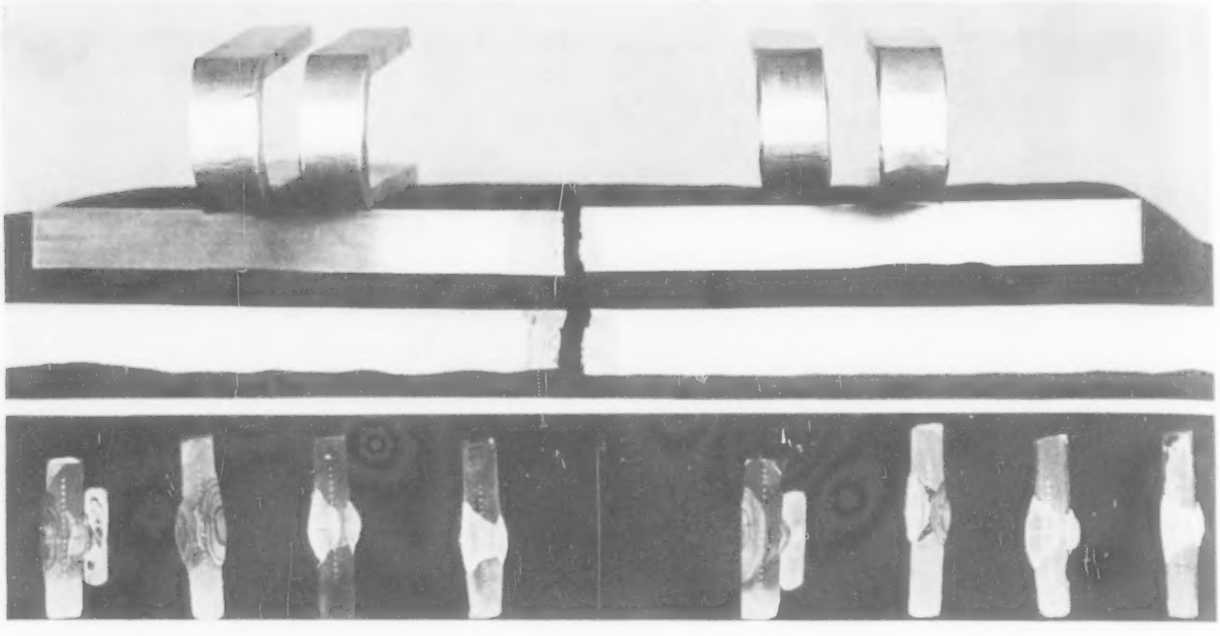


Fig. 5(a)—Microstructure of Deposit Made with 18-8 Stainless Steel Electrode on Low Carbon Steel Plate. 1000 X. Note That the Austenite Has Transformed

Fig. 5(b)—Microstructure Showing Fusion Line of Deposit Made with 25-12 Stainless Steel Electrode on Low Carbon Steel Plate. 300 X.

Fig. 6—Results of Bend Tests



1—18-8 Plate—low carbon steel electrode
2—25-12 Plate—low carbon steel electrode
3—Low carbon steel plate—18-8 electrode
4—Low carbon steel plate—25-12 electrode

Fig. 7—Macrographs of Welds Showing Hardness Indentations

as little as 10% parent metal since the "V" is much wider at the top. Since the individual beads can be diluted from approximately 50% parent metal at the bottom to 10% parent metal at the top it becomes obvious that variation in the microstructure will occur from bead to bead. For this reason, considerable thought must be given to the welding of dissimilar metals or the use of a dissimilar electrode since the resulting alloy formed in the weld may or may not have suitable physical properties.

III. THE WELDING OF LOW ALLOY AND CARBON STEELS WITH 25–12 AND 18–8 STAINLESS STEEL ELECTRODES

In an effort to show the alloying action which takes place when welding low alloy (1 to 2% special element) and low carbon steels with 18% Cr–8% Ni and 25% Cr–12% Ni electrodes, a series of beads were deposited onto low carbon steel base material by using these electrodes. The object of primary concern from an alloying standpoint is the ratio of the metal deposited to the parent metal melted (indicated in Fig. 2 as $\frac{A}{B}$).

A cross section of the test plates is shown in Fig. 3. This photograph gives a good indication of the effect of welding current and welding speed on the ratio $\frac{A}{B}$. There was considerable variation in this ratio along the same bead but in general it is believed that when using a $^{5}/_{32}$ in. coated electrode and 170 amperes, the ratio of $\frac{A}{B} = <1$ with welding speeds of 6, 8 and 14 inches per minute. Using 125 amperes with the above welding speeds, this ratio $\frac{A}{B}$ appears to be greater than unity. The specimens of Fig. 3 lettered 18 and 25 illustrate welds made with 18–8 and 25–12 electrodes, respectively.

A point which must not be overlooked is the fact that the weld beads are sufficiently high in chromium and nickel directly adjacent to the line of fusion so that they are not attacked by the nitric acid which darkened the low carbon base metal during the etching operation. The welds which were made at 14 inch/minute show sufficient dilution to cause partial darkening of the weld bead. This darkening which appears to a considerable extent throughout the weld bead, is especially noticeable in the 18–8 bead run at 14 inch/minute and 170 amperes.

The fact that there is a composition gradient from the center of the weld bead to the line of fusion is indicated by the hardness results shown in Fig. 4. The hardness of the bead made with 18–8 at 170 amperes is over 400 Vickers and this hardness is changed only slightly after annealing at 900° C. As can be observed from Fig. 4, the dilution of the 18–8 bead using 125 amperes was less and as a result the hardness was not consistently 400 Vickers but varied from 400 to as low as 340, thus indicating a partially if not completely transformed austenite. In this case, as above, the hardness was lowered only slightly by the annealing treatment. The hardness results on the 25–12 bead using 170 amperes indicate the existence of a composition gradient, the bead being more highly diluted adjacent to the line of fusion than in the center of the bead. The hardness of the 25–12 bead made by using 125 amperes was about 165 V. P. N. and indicated a truly austenitic structure.

Microexamination of the four beads on which the hardness readings were taken showed that the 25–12 bead using 125 amperes was the only bead which showed no transformation of the austenite. The other three

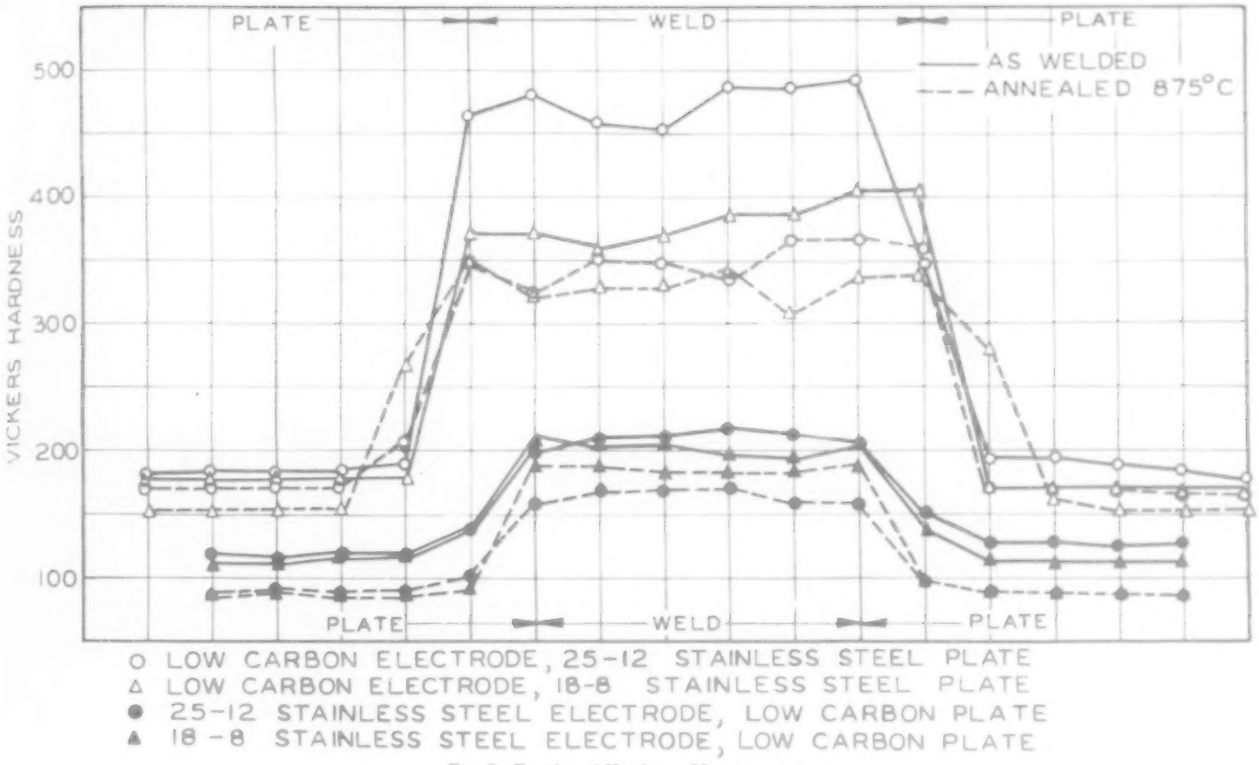


Fig. 8—Results of Hardness Measurements

beads all had structures showing preferred precipitation along the octahedral planes, typical of transformed austenite. Fig. 5(*a*) shows a photomicrograph of the diluted 18-8 weld bead using 170 amperes. This structure extended throughout the weld bead and showed no change until the sharp change at the line of fusion. Also shown in Fig. 5(*b*) is a photomicrograph of a bead made with 25-12. This bead remained austenitic after dilution by welding and followed by a 900° C. anneal. This substantiates the theory that when welding ferritic steels with austenitic electrodes a *narrow martensitic band does not exist along the line of fusion*. This photomicrograph shows the ferritic plate material and dendritic austenitic grains in the weld bead directly adjacent to each other. It is possible, however, that a martensitic band might be formed as a result of solid diffusion, should the weld be subjected to relatively high temperatures for a considerable period of time.

IV. PROPERTIES OF LOW CARBON STEEL WELDED WITH 18-8 AND 25-12 STAINLESS STEEL ELECTRODES

Welding Procedure

Two test plates were made from ½-inch low carbon steel using a 90° included angle. One plate was welded

Fig. 9—Martensitic Weld Metal (Upper Right) Did Not Respond to Etchant Used for 25-12 Parent Metal. Note Grain Growth of Parent Metal Due to Welding Heat. 100 X

Fig. 10—Etched Weld Metal of Fig. 9. 100 X

Fig. 11—Microstructure of Weld Deposit 18-8 Stainless Steel Welded with Low Carbon Steel Electrode Unetched. 20 X

using 18–8 stainless steel and the other plate was welded using 25–12 stainless steel. The electrodes used for the tests were $^1/_8$ inch in diameter. After welding, the two plates were machined into tensile, free bend and hardness test specimens.

Tensile Tests

The data on the tensile tests are given in Table 1, Nos. 1 and 2. From this data it may be seen that when using stainless steel electrodes, the weld obtained is stronger than the low carbon plate material.

Free Bend Tests

The data on the free bend tests are given in Table 2, Nos. 1 and 2. Observation of Fig. 6 shows results of bend tests. It is to be noticed that even though the relative ductility of the welds made with 18–8 and 25–12 electrodes were low as compared to the parent metal, the welds were capable of withstanding 180° free bends without failure.

Hardness Tests

Macrographs of the welds showing indentations from the hardness tests are shown in Fig. 7. It may be seen that the hardness impression on the 18–8 and 25–12 weld deposit are approximately the same size as the hardness impressions on the low carbon steel base metal (Samples 3, 3*A*, 4, 4*A*). This is further brought out by Fig. 8 in which it may be observed that the hardness in the weld is only approximately 75 V. P. N. higher than the parent metal.

V. PROPERTIES OF 18–8 AND 25–12 STAINLESS STEEL WELDED WITH LOW CARBON ELECTRODES

Commercially stainless steel would never be welded using low carbon steel electrodes. Likewise stainless steel should never be welded to low carbon steel using low carbon steel electrodes. The following tests were made, however, in an effort to point out the difficulties involved if such a practice were used.

Welding Procedure

Two $^1/_4$-inch test plates were made using 18–8 and 25–12 stainless steel and these were welded using low carbon steel electrodes. The plate bevel and electrode diameter were the same as used in the previous tests. After welding, the 25–12 plate was machined into tensile, free bend and hardness test specimens. No tensile or free bend tests could be made on the 18–8 plate welded with low carbon steel electrodes because, upon cooling, a longitudinal crack appeared through the center of the weld. This crack extended over 60% of the entire weld length. Although the weld cracked, a visually sound weld free from gas bubbles and porosity was made on 18–8 with a low carbon electrode. This crack did not occur for at least 10 minutes after completion of the weld and for this reason was believed to be an example of delayed cracking as mentioned by Harris in his article on "The Metallurgy of Welding."[9] After welding, the steel in the vicinity of the weld is heated to 200° C. or higher. Alloy steels transform very slowly in this temperature range and it is believed that cracking does not occur until a major portion of the austenite has transformed. The time for complete transformation may vary from 15 minutes to several hours. Though a low carbon steel electrode was used to weld the 18–8 stainless steel, sufficient chromium and nickel were picked up from the base metal to produce essentially an alloy steel weld deposit.

Tensile Tests

The data on the tensile tests are given in Table 1, No. 3. A sharp crystalline fracture (characteristic of a brittle material) was obtained directly through the center of the weld when using low carbon steel electrodes.

Free Bend Tests

The data on the free bend tests are given in Table 2, No. 3. Observation of Fig. 6 shows results of bend tests. The welds made with the low carbon electrodes on the 18–8 and 25–12 parent metals were hard and definitely brittle.

Hardness Tests

Macrographs of the welds showing indentation from the hardness tests are shown in Fig. 7. The samples shown at the left are in the as-welded condition while those on the right were annealed at 875° C. In specimens 1 and 1*A* (18–8 plate welded with low carbon electrodes) visual observation discloses a number of small cracks throughout the entire weld. In specimens 1, 1*A*, 2 and 2*A* considerable difference in the size of the hardness impressions can be observed as the boundary is crossed from the weld into the parent metal. This is definitely brought out in Fig. 8 in which the hardness of the weld and parent metal is shown graphically. It can be seen that annealing, though softening one of the welds somewhat, has little effect upon the hardness of the weld bead.

Microstructure

The photomicrograph in Fig. 9 shows the 25–12 parent metal and a portion of the weld. The low carbon elec-

Table 1—Tensile Tests

No.	Parent Metal	Electrode	Ultimate Strength, Lb./Sq. In.	Remarks
1	1/4 In. Low carbon	25–12	45,200	Fractured in parent metal
	1/4 In. Low carbon	25–12	45,000	Fractured in parent metal
2	1/4 In. Low carbon	18–8	45,000	Fractured in parent metal
	1/4 In. Low carbon	18–8	45,000	Fractured in parent metal
3	1/4 In. 25–12	Low carbon	35,200	Very coarse crystalline fracture directly through center of weld
	1/4 In. 25–12	Low carbon	46,000	Very coarse crystalline fracture directly through center of weld
4	1/4 In. 27% Cr	25–12	75,550	Fractured in parent metal
	1/4 In. 27% Cr	25–12	75,400	Fractured in parent metal
5	1/4 In. 27% Cr	18–8	76,100	Fractured in parent metal
	1/4 In. 27% Cr	18–8	75,600	Fractured in parent metal
6	1/4 In. 17% Cr	25–12	80,550	Fractured in parent metal
	1/4 In. 17% Cr	25–12	80,550	Fractured in parent metal
7	1/4 In. 17% Cr	18–8	77,800	Fractured through edge of top bead and along line of fusion
	1/4 In. 17% Cr	18–8	No test	
8	1/4 In. 12% Cr	18–8 C. B.	62,200	Fractured in parent metal
	1/4 In. 12% Cr	18–8 C. B.	62,450	Fractured in parent metal
9	1/4 In. 12% Cr	4–6% Cr	62,200	Fractured in parent metal
	1/4 In. 12% Cr	4–6% Cr	54,600	Fractured in parent metal
10	1/4 In. 18–8	27% Cr	87,300	Fractured in parent metal
	1/4 In. 18–8	27% Cr	85,000	Fractured through center of weld; extremely crystalline throughout
11	1/4 In. 18–8	17% Cr	67,200	Fracture through top edge of weld and fusion line. Fracture occurred first in weld followed by a shear failure along fusion line
	1/4 In. 18–8	17% Cr	67,800	
12	1/4 In. 25–12	27% Cr	92,800	Fractured in parent metal
	1/4 In. 25–12	27% Cr	93,000	Fractured in parent metal
13	1/4 In. 25–12	17% Cr	88,700	Fractured through weld. The top bead (on face weld) appeared very brittle while root of weld appeared very ductile. Top bead showed very crystalline fracture
	1/4 In. 25–12	17% Cr	82,800	

Table 2—Free Bend Tests

No.	Parent Metal	Electrode	% Elongation in Welded Face	Remarks
1	Low carbon	25–12	15.0	180° bend—difficult to get uniform bending (not maximum elongation of weld)
	Low carbon	25–12	13.0	180° bend—difficult to get uniform bending
2	Low carbon	18–8	6.5	180° bend—non-uniform bending through and adjacent to weld
	Low carbon	18–8	9.5	180° bend—non-uniform bending through and adjacent to weld
3	25–12	Low carbon	0.0	A bend of 60° was obtained, followed by failure through weld. The 60° bend took place in parent metal adjacent to weld
	25–12	Low carbon	0.0	A bend of 25° was obtained, followed by failure through weld. The 25° bend took place in parent metal adjacent to weld
4	27% Cr	25–12	18.5	180° bend—appearance good, appearance of bend typically austenitic
	27% Cr	25–12	19.0	180° bend—appearance good, appearance of bend typically austenitic
5	27% Cr	18–8	17.0	180° bend—appearance and ductility good
	27% Cr	18–8	16.0	180° bend—appearance and ductility good
6	17% Cr	25–12	14.5	Weld satisfactory but cracked in heat-affected zone
	17% Cr	25–12	16.5	Weld satisfactory but cracked in heat-affected zone
7	17% Cr	18–8	20.5	Weld satisfactory but cracked in heat-affected zone. Need for annealing 17% Cr plate after welding
	17% Cr	18–8	17.0	Weld satisfactory but cracked in heat-affected zone. Need for annealing 17% Cr plate after welding
8	12% Cr	18–8 C. B.	10.5	180° bend, appearance satisfactory
	12% Cr	18–8 C. B.	10.5	180° bend, appearance satisfactory
9	12% Cr	4–6% Cr	14.0	180° bend, appearance satisfactory, ductility appears O. K.
	12% Cr	4–6% Cr	16.0	180° bend, appearance satisfactory, ductility appears O. K.
10	18–8	27% Cr	6.5	180° bend
	18–8	27% Cr	6.5	180° bend
11	18–8	17% Cr	3.0	180° bend
	18–8	17% Cr	6.5	180° bend
12	25–12	27% Cr	16.5	180° bend, appearance satisfactory
	25–12	27% Cr	15.0	180° bend, appearance satisfactory
13	25–12	17% Cr	2.5	Fractured through weld face; face of weld brittle but root of weld ductile
	25–12	17% Cr	5.5	Fractured through weld face; face of weld brittle but root of weld ductile

Table 3—Chemical Analysis of the Steels

Material	Heat No.	C	Mn	P	S	Si	Ni	Cr
12% Cr	67,972	0.11	0.42	0.016	0.014	0.28	0.12	11.87
17% Cr	57,732	0.08	0.47	0.017	0.015	0.59	0.15	17.57
27% Cr	68,850	0.13	0.56	0.014	0.016	0.55	0.17	27.00
18–8	37,172	0.05	0.48	0.018	0.016	0.39	9.24	18.65
25–12	No analysis							

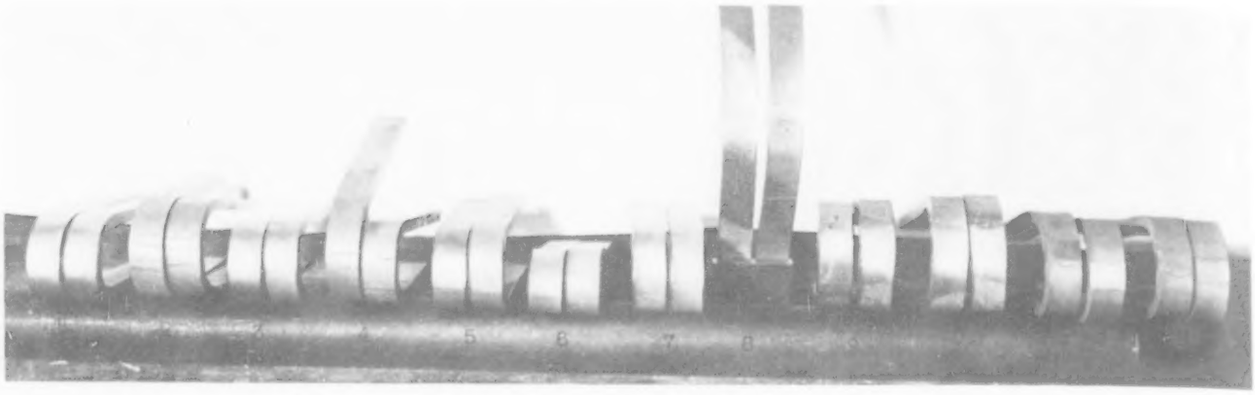
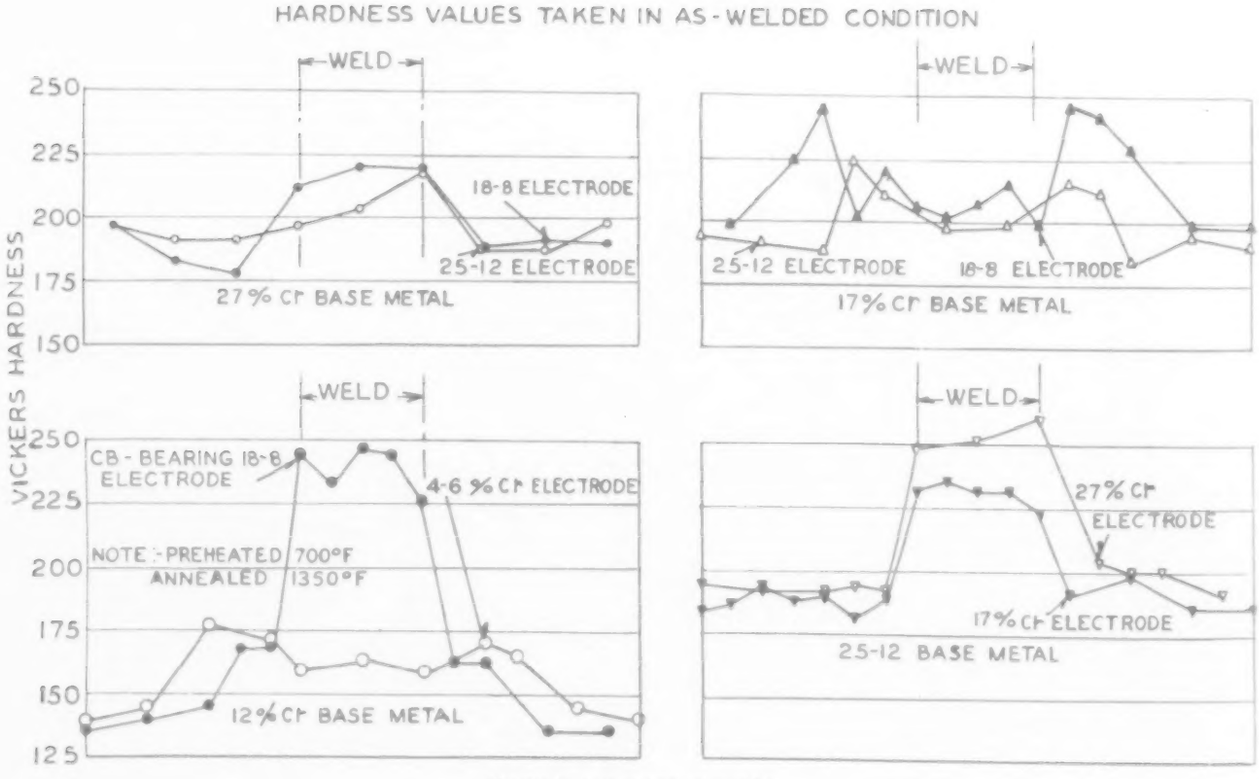


Fig. 12—Bend Tests of High Alloy Stainless Steels

trode which was diluted with 25–12 as it was deposited, did not respond to the etchant used for the base metal and therefore showed up unetched. It is to be noted that the heat from welding was sufficient to cause grain growth in the parent metal adjacent to the weld. Figure 10 shows the structure of the weld deposit, previously referred to, after etching. It can be seen that definite precipitation has taken place which indicates that a large proportion of the bead has transformed to the alpha modification of the iron. Shown in Fig. 11 is a photomicrograph of the low carbon weld made on the 18–8 plate material. This photo, unetched at 20×, shows the weld to be full of cracks. These cracks run parallel to the dendritic structure produced as a result of cooling. Since these cracks run parallel to the dendrites it seemed probable that they were associated with carbide precipitation since carbide precipitation gives rise to both weakness and brittleness. These cracks form during contraction of the weld while cooling.

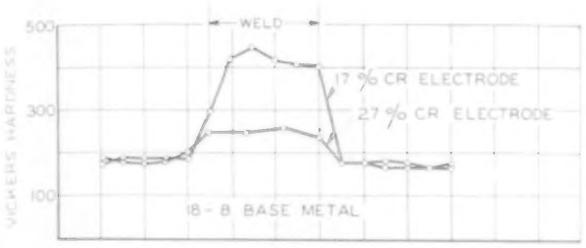
VI. THE WELDING OF HIGH ALLOY STAINLESS STEELS AND IRONS

When welding the straight chromium stainless irons with commercial electrodes of the same analysis, the welds are very weak, especially toward shock. It may be observed that iron containing more than 14–15% chromium is outside of the gamma loop in the iron–chromium system (Fig. 1) and therefore grain refinement by reheating is impossible without mechanical working. Since grain refinement cannot be accomplished, long columnar grains are produced in the weld. This is typical of all cast structure and is one of the weakest type of grain structure which can be obtained. As a result of this, in many instances, the high chromium irons are welded with the nickel-chromium austenitic stainless steels. Metallurgically and physically, these welds have been found satisfactory.



Distance between readings enlarged 5×

Fig. 13—Hardness Tests of High Alloy Stainless Steels



HARDNESS VALUES TAKEN IN "AS WELDED" CONDITION

Fig. 14

Welding Procedure

The welding procedure used in this case was the same as that used when welding the low carbon steel with stainless steel electrodes as previously described. One-eighth-inch electrodes were used throughout, using 95 amperes and 23 volts. The chemical analysis of the steels used is given in Table 3.

Tensile Tests

The tensile data are given in Table 1, Nos. 4–13. Failure occurred in the weld or heat-affected area in four of the 10 combinations of alloying tried in this group of tests. The fractures appeared to be of three different types. The 17% chromium base metal welded with 18–8 fractured through the top bead near the line of fusion, the crack then following the fusion line. The same type of failure occurred in 18–8 plate welded with 17% chrome electrode. The latter failure seemed much more logical than the former. The second type of failure occurred in the 18–8 plate welded with 27% chrome electrode. This failure was completely through the weld. The weld was non-ductile and the fracture was very crystalline. This failure was believed to be due to lack of nickel in the deposit. The third failure occurred through the center of the weld but the face bead was relatively hard and crystalline and the bottom bead appeared relatively ductile and fine grained. This type of fracture occurred using a combination of 25–12 plate material welded with 17% chrome electrode. The bottom bead was sufficiently high in nickel from dilution with the base metal while the top bead contained insufficient nickel for good ductility.

Bend Tests

The results of the bend tests are given in Table 2, Nos. 4 to 13, and photographs of the test pieces are shown in Fig. 12. The free bends have a radius of approximately 1¼ inches. As a result of differences in hardness of weld and parent metal it was extremely difficult to get even bending to take place in the weld and adjacent parent metal. The 17% chromium plate, welded with both 18–8 and 25–12, cracked in the heat-affected zone, indicating a definite need for annealing. Contrary to this fact, the observed microstructure of the as-welded 17% chromium plate does not seem to agree with this conclusion. There were, however, no cracks observed in the welds made using the 17% chromium steel plate.

As could be predicted from the tensile tests on the 25–12 plates welded with 17% chrome electrodes, fracture occurred through the face bead on bending. This fracture was very fine and sharp, indicative of a very brittle material. All other bend tests of this group showed no sign of cracking. Though no cracks were observed in the 18–8 plate welded with 17% chrome electrodes after 180° bend, hardness tests as well as per cent elongation in the bend test indicated that the weld made with 17% chrome electrode was definitely hard and therefore highly questionable.

Hardness Tests

Hardness tests were made on each of the 10 combinations and are shown plotted in Figs. 13 and 14. From the indentations shown on the macrographs in Fig. 15 it can be seen that the beads are all relatively soft. The only exceptions to this are four readings taken on the top bead of the 25–12 plate welded with 17% chrome (specimen No. 8). Welds made on 18–8 plate with 17% chrome electrodes (not shown in Fig. 15) were also relatively hard.

Microscopic Examination

All of the 10 alloy combinations discussed in this group were examined microscopically. Figures 16 to 21 illustrate a few of the various structures observed. The observations made were very enlightening as to the happenings when an electrode of one analysis is alloyed with the parent metal of a different analysis.

Figure 16 clearly points out that considerable alloying takes place in welded joints. Here is shown the top, middle and bottom beads of a butt weld made on 27%

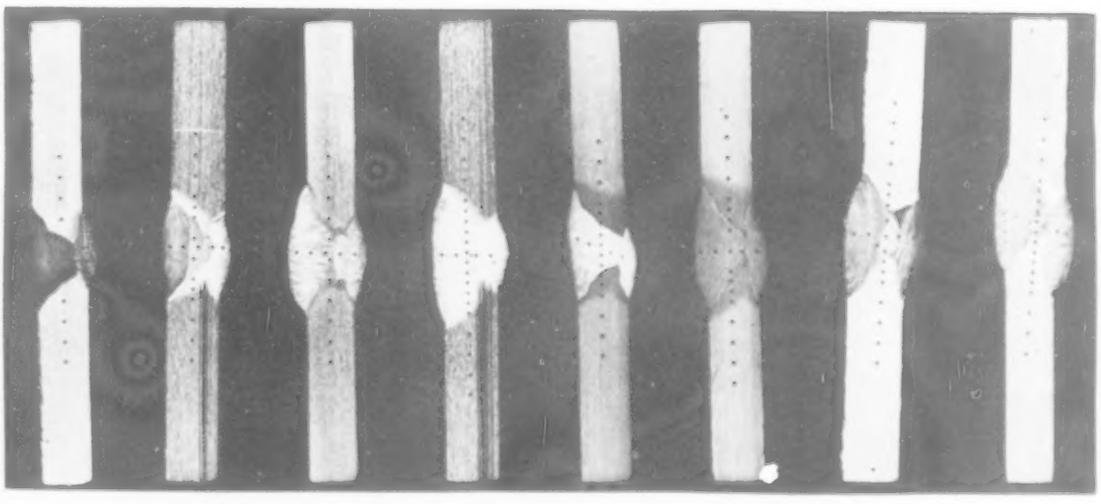


Fig. 15—Macrographs of the High Alloy Stainless Steels

chrome plate with 25–12 chrome–nickel austenitic electrodes. The middle bead, line of fusion and 27% chromium plate directly adjacent to the middle bead are also shown in this figure. Of the three deposited beads, only the top is characteristic of a truly austenitic deposit. The middle and bottom beads consisted of both austenite and ferrite. The bottom bead consisted of austenitic dendrites, with what appeared to be a pseudo eutectic of austenite and ferrite as filler metal for the dendrites. Here again is a weld bead consisting of austenite and ferrite directly adjacent to chromium ferrite with no evidence of martensite formation.

Figures 17 and 18 show the edge of the weld deposit and the heat-affected zone of 17% chrome steel in both the as-welded and annealed conditions. The structure of the as-welded material containing delta grains bounded by austenite would appear to be a structure with greater ductility than one containing large ferrite grains bounded by carbide as that of the annealed structure. The heat-affected area of the as-welded structure, however, definitely lacks ductility. Shown in Figs. 19 and 20 are the line of fusion and weld deposit of 25–12 parent metal welded with 17% chrome electrode, and also the deposit of a 17% chrome electrode using 18–8 parent metal. The weld deposit of the former shows long columnar grains of delta ferrite showing filler metal of austenite. It is believed that the formation of small amounts of austenite was not only due to nickel picked up from the base metal but also due to carbon segregation in the metal which solidified as austenite.

The latter photomicrograph mentioned shows grains of chromium ferrite with small amounts of austenite present, carbide envelopes around some of the austenite and some austenite showing no carbide present. The hardness of 400 Vickers which was obtained on this material was somewhat puzzling in view of the fact that the microstructure was apparently chromium ferrite. In an article by F. M. Becket,[10] however, it is pointed out that hardening accompanied by brittleness does occur in chromium irons with chromium varying from less than 20% to more than 50%. At the present time, however, little is known about this phenomenon.

Figures 21(*a*)–21(*d*) illustrate structures produced when welding 25–12 stainless steel with 27% chrome electrode. When welding 27% chrome steel using 27% chrome electrodes the weld deposit is extremely brittle, characteristic of the as-cast chromium ferrites. When using 27% chrome electrodes for welding 1/4-inch 25–12 plates sufficient nickel is picked up from the parent metal during welding so that a considerable amount of austenite is formed, thus having a relatively fine grained austenitic, ferritic structure which produced relatively

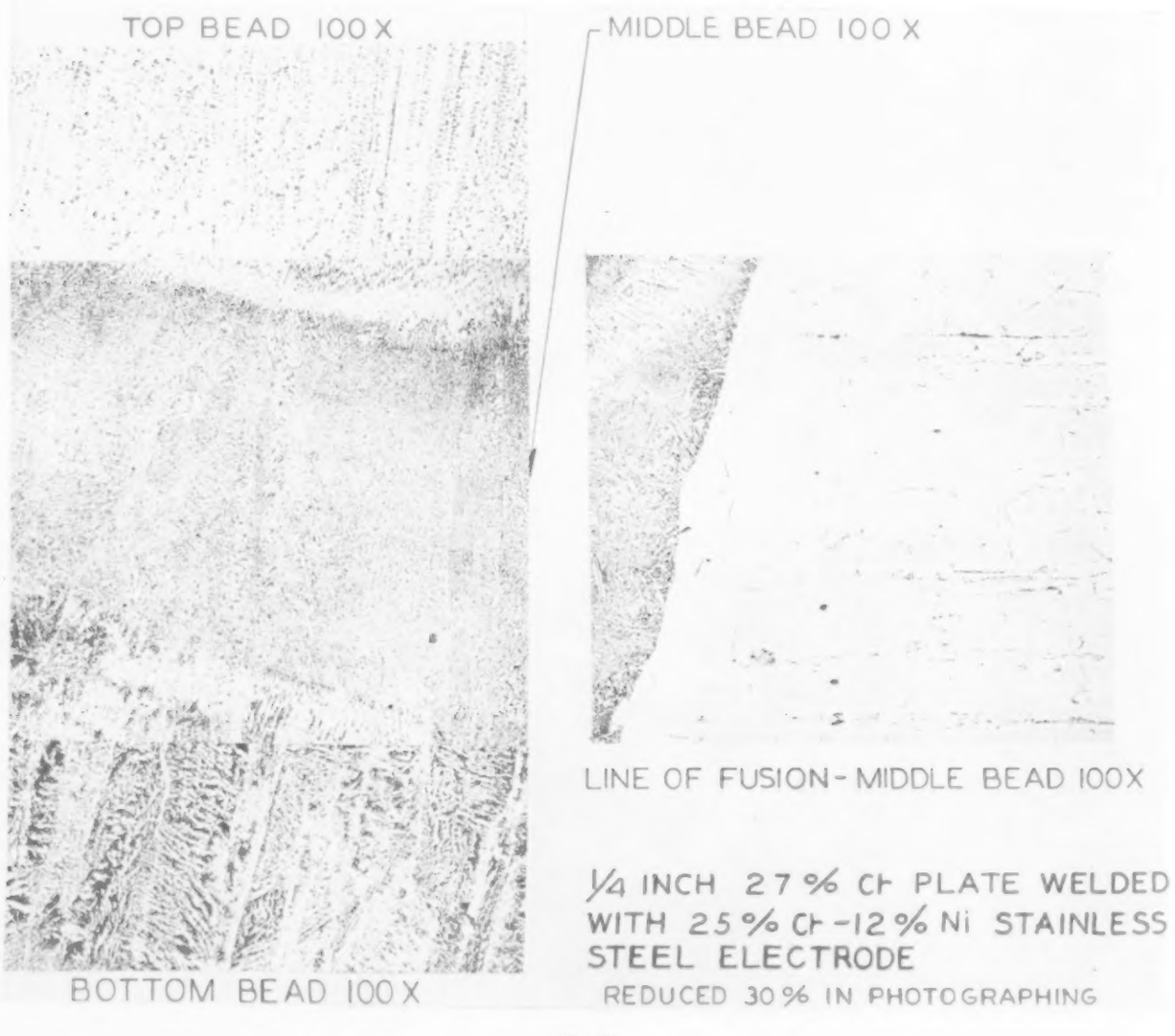

TOP BEAD 100 X
MIDDLE BEAD 100 X
BOTTOM BEAD 100 X
LINE OF FUSION - MIDDLE BEAD 100X
1/4 INCH 27 % Cr PLATE WELDED WITH 25 % Cr - 12 % Ni STAINLESS STEEL ELECTRODE
REDUCED 30 % IN PHOTOGRAPHING

Fig. 16

Fig. 17—Fusion Line and Heat Affected Zone 17% Chrome Iron Welded with 25–12 Stainless Steel Electrodes—As-Welded. 100 X

Fig. 18—Fusion Line and Heat Affected Zone 17% Chrome Iron Welded with 25–12 Stainless Steel Electrode—Annealed 900° C. 100 X

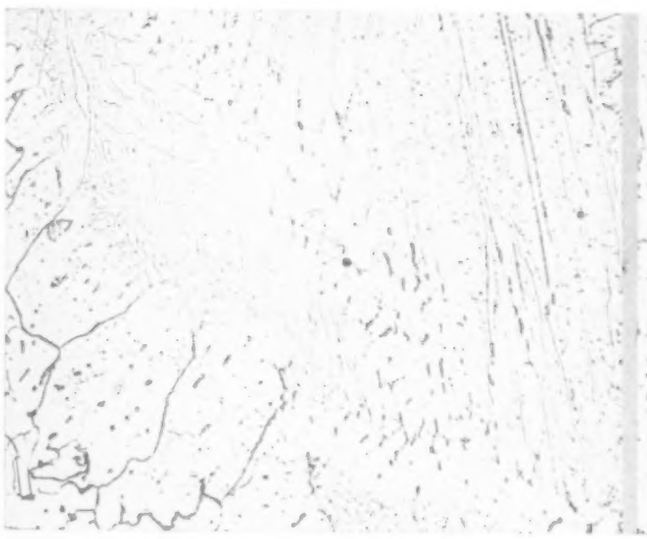


Fig. 19—Fusion Line and Weld Deposit 25–12 Stainless Steel Welded with 17% Chrome Electrode—Annealed 900° C. 100 X

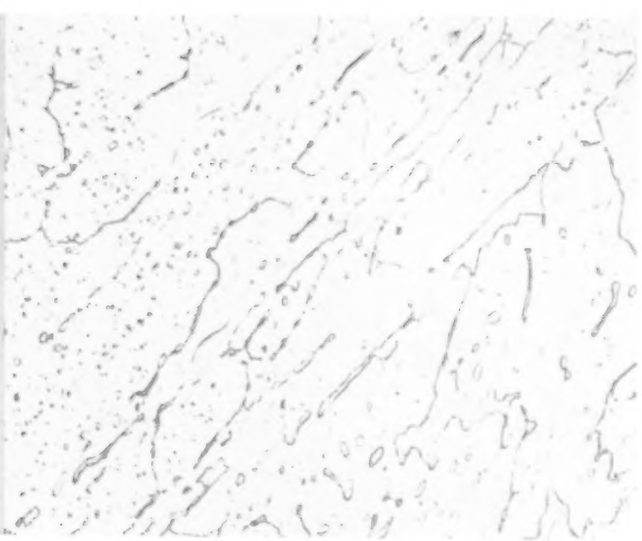


Fig. 20—Weld Deposit—18–8 Stainless Steel Welded with 17% Chrome Electrode—Annealed 900° C. 300 X

high ductility and a tensile strength greater than the 25–12. The top bead consisted of small austenite particles embedded in ferrite. The middle and bottom beads also consisted of austenite embedded in ferrite.

A photomicrograph showing the line of fusion between 25–12 plate and 27% chrome electrode deposit is also shown. The austenitic parent metal can be seen at the right. Adjacent to the austenite is a large peninsula of ferrite, adjacent to which is a band of austenite followed again by a matrix of ferrite in which needles and small diamond shaped austenite particles are embedded.

CONCLUSIONS

1. When metallic arc welding ferrous alloys, there are two factors which must be given consideration: (*a*) the effect of rapid heating and cooling on the parent metal as a result of the welding operation, and (*b*) the effect of alloying when the base metal is melted and alloyed with the deposited metal.

2. Low carbon and high alloy stable austenitic steels are not affected adversely physically by welding heat and therefore the only consideration is that of the effect of alloying. This problem is not encountered if the electrode used for welding is of approximately the same commercial analysis as the steel being welded. The problem of alloying is only encountered in the welding of steels where an electrode of dissimilar analysis must be used and also in the case of the welding of dissimilar metals.

3. In cases where it is necessary to weld low carbon or relatively low alloy steel (0.5 to 4% special element) with austenitic electrodes, it is desirable to use an electrode in which the austenite is sufficiently stabilized so that after dilution with the low carbon or low alloy steel it will still remain as stable austenite and not revert to martensite. The use of 25–12 or 25–20 chrome–nickel electrodes is believed to be preferable to 18–8 chrome–nickel electrodes for work of this nature.

4. A narrow band of martensite is not formed along the fusion line of a weld made with an austenitic elec-

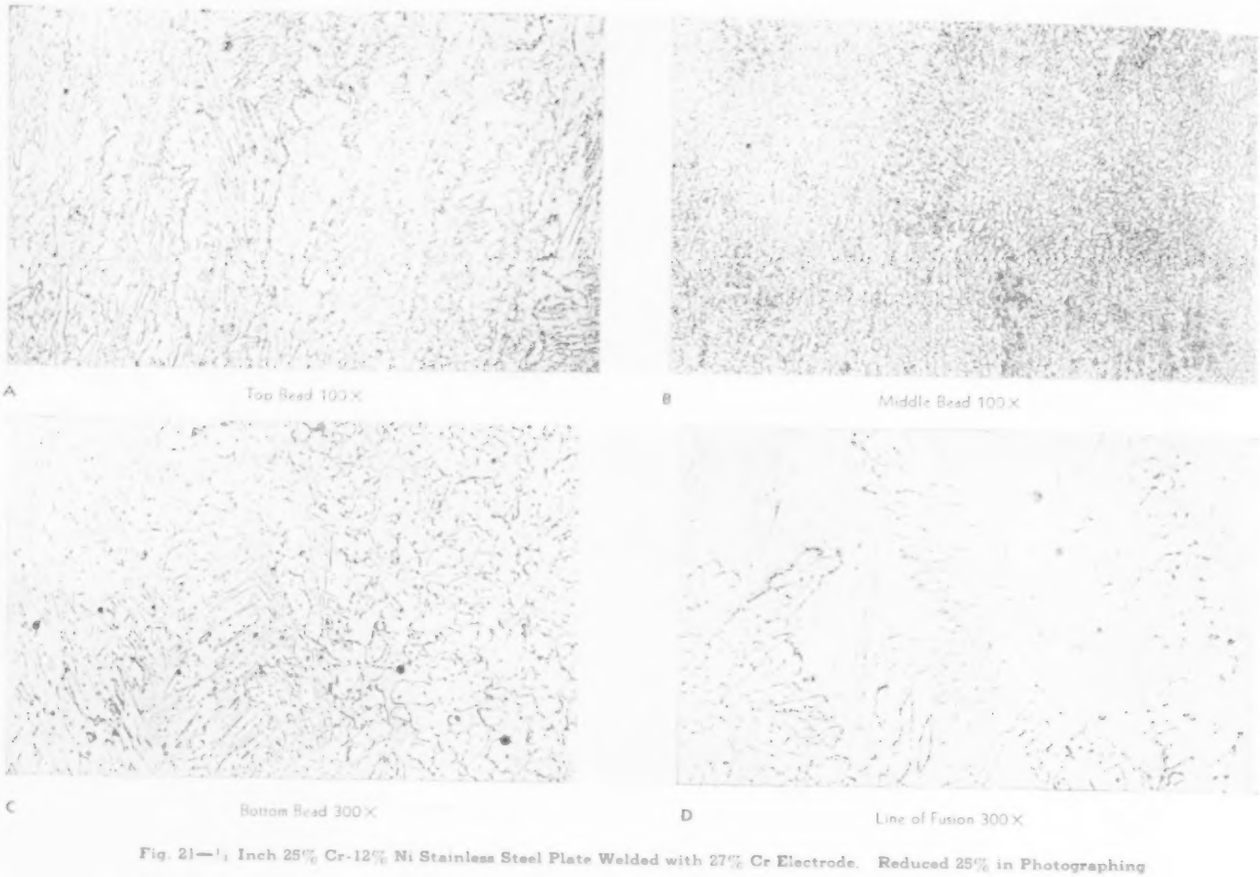


Fig. 21—¼ Inch 25% Cr-12% Ni Stainless Steel Plate Welded with 27% Cr Electrode. Reduced 25% in Photographing

trode and a low carbon or low alloy steel. Each layer of deposited weld metal is entirely austenitic or else the dilution is sufficient to cause reversion of the entire layer to martensite (transformed austenite).

5. It is believed that, in general, any ferrous alloy which has sufficient alloying elements present to cause the steel to be in a stable austenitic state, i.e., having a face centered cubic lattice, can be satisfactorily welded with an electrode of stable austenite such as the 18–8 or 25–12 variety of stainless steel.

6. Successful welds can be made on both 17 and 27% chrome iron with 18–8 or 25–12 stainless steel electrodes. Though the welds, as such, are satisfactory, annealing of the welded material may be desirable to produce the desirable ductility in the heat-affected zone of the parent metal. It is to be pointed out, however, that due to the differences in contraction and expansion of the straight chromium ferritic irons and the austenitic stainless steels, it is not desirable to use the above mentioned combinations if the welded parts are to be subjected to alternate heating and cooling, since this would soon lead to failure of the welded parts. In such instances it is desirable to weld the chrome irons with electrodes of the same chemical composition.

7. Plates of 25–12 stainless steel ¼ inch thick when welded with 27% chrome electrodes produced an ultimate strength which was greater than 90,000 lb./sq. in., failure occurring in the parent metal.

8. Twelve per cent chromium steel was successfully welded using both 18–8 stainless steel and also 4–6% chromium steel electrodes. Very good physical properties were obtained. This steel was preheated to 700° F., followed by a 1350° F. anneal after welding, since good physical properties of this steel, when welded, depend upon a relatively slow cool.

9. The use of 17% chrome electrode does not appear suitable for the welding of either 25–12 or 18–8. The hardness of the weld deposit made with 17% chrome electrodes on 18–8 parent metal was approximately 400 Vickers though microscopically there was no evidence of a structure capable of producing that hardness.

ACKNOWLEDGMENTS

The author wishes to acknowledge the helpful suggestions and cooperation given by Mr. C. H. Jennings, engineer in charge of welding research, Westinghouse Research Laboratories, and Dr. A. Allen Bates, Manager, Chemical and Metallurgical Division, Westinghouse Research Laboratories.

Acknowledgment is also made to Miss Mildred Ferguson, Westinghouse Research Laboratories, for the metallographic studies, and Mr. Paul Ehrhardt for his assistance in welding the test plates.

BIBLIOGRAPHY

1. E. C. Bain, "The Nature of the Alloys of Iron and Chromium," *A. S. S. T.*, **9**, p. 9, January 1926.
2. E. C. Bain and William E. Griffiths, "An Introduction to the Iron-Chromium-Nickel Alloys," *A. I. M. E. Trans.*, **LXXV**, p. 166, 1927.
3. V. N. Krivobok and M. A. Grossmann, "A Study of the Iron-Chromium-Carbon Constitutional Diagram," *A. S. S. T.*, **18**, p. 760, July–Dec. 1930.
4. V. N. Krivobok and M. A. Grossmann, "Influence of Nickel on the Iron-Chromium-Carbon Constitutional Diagram," *A. S. S. T.*, **18**, p. 808, July–Dec. 1930.
5. R. H. Aborn and E. C. Bain, "Nature of the Nickel-Chromium Rustless Steels," *A. S. S. T.*, **18**, p. 837, July–Dec. 1930.
6. Arthur Phillips and R. W. Baker, "Study of High Chromium, Low-Carbon Steel," *A. S. S. T.*, **18**, p. 894, July–Dec. 1930.
7. V. N. Krivobok and Maxwell Gensamer, "Dilatometric Study of Chromium-Nickel-Iron Alloys," *A. I. M. E.*, Iron and Steel Division, p. 325, 1931.
8. E. E. Thum, *Editor*, "The Book of Stainless Steels," American Society for Metals, 2nd Edition.
9. H. Harris, "The Metallurgy of Welding," Institute of Welding Transactions, October 1938.
10. F. M. Becket, "On the Allotropy of Stainless Steels," *A. I. M. E.*, Iron and Steel Division, **131**, p. 15.

THE ENGINEERING FOUNDATION
Welding Research Committee

Sponsored by the American Welding Society « « » » and American Institute of Electrical Engineers

Supplement to the Journal of the American Welding Society, November 1939

SHRINKAGE AND OTHER INITIAL Stresses in Welds*

By M. F. SAYRE†

IN JUNE 1937 a grant was made to Union College by the Welding Research Committee to cover expenses for a study of shrinkage stresses in welds. The plan outlined for this research called for an effort to collect information from recent progress in allied fields not connected with welding, which could be used to help in outlining proper methods of experimental attack on the problem in welds themselves. Part of the money provided was for traveling expenses to be used in collecting this information, and part for expenses for a collaborator who would work on collection of information from available literature. A verbal statement of the results of this study was presented at the AMERICAN WELDING SOCIETY meeting, October 1938. The present paper constitutes a final report.

There are three phases to the problem of shrinkage effects in welds:

(1) The resultant changes in size and in shape during welding and the methods to be used to limit the magnitude of these changes or to pre-allow for their effect.

(2) Failures during the process of welding which may result from these shrinkage effects, in cases where, for one reason or another, plastic yield is inhibited and cracking occurs instead.

(3) The possible future effects upon the strength or reliability of the welded joint, due to the presence of entrapped stresses resulting from differential thermal expansion and contraction during welding. These effects may include either warping during machining or during later use, or actual failure through impact, through fatigue, or through accelerated corrosion at points of stress concentration. In the event that freedom from later warping is necessary, the obvious cure is a stress relief anneal following welding, so that the problem resolves itself down to one of finding out under what conditions, if any, these initial stresses will later result in service failures.

In commercially used metals, the thermal change in length from solidification temperature down to room temperature is of the general order of magnitude of 1%, five to ten or more times as great as the possible elastic elongation or shortening in the same materials within their elastic limits. Plastic changes in size, accompanied by development of internal stresses, due to the expansion followed by shrinkage during welding are therefore virtually inevitable. By proper welding technique these effects can be minimized, and by proper design, the size changes can be allowed for. It is foolish to expect to eliminate them entirely, and unnecessary to desire it. In fact, measurements of the magnitude of the entrapped internal stresses present immediately after welding may be of no great value from a general point of view, since in all but very simple cases, localized or general entrapped stresses approaching the yield strength of the material may normally be expected.

The real problem concerns not the presence or the magnitude of these stresses, but rather the specific conditions under which they may cause damage, either during welding or later. During the early days of welding such damage occurred much too frequently, and the prevention of failures was a major problem. With the development of better welding rods, and of better welding technique and a better knowledge of what to avoid during welding, failures have become very infrequent, but at the expense in some measure of limiting the base metals which may be commercially welded, and of more time-consuming technique during welding. The emphasis has therefore shifted to some extent from that of preventing failures to that of minimizing these expensive limitations so that the field of welding may be further expanded. This even more emphatically calls for a thorough understanding of the basic causes for the few failures which occur.

CONDITIONS UNDER WHICH CRACK FAILURES OCCUR

As an initial step, Mr. E. A. Winzenburger, Instructor in Engineering at Union College, in cooperation with the author, prepared an analysis‡ based upon all of the clearly described published cases of crack failure of welds which could be found.

Five basically different types of welding failures due to thermal effects can readily be distinguished. A somewhat surprising fact, however, is the extent to which, as indicated both in published articles and in personal conversations, individual welding engineers tend to concentrate attention upon some one of these types of fail-

* Contribution to Fundamental Research Division, Welding Research Committee.

† Professor of Applied Mechanics, Union College.

‡ This in some measure overlaps the summaries recently published in the Welding Research Committee Supplement to the AMERICAN WELDING JOURNAL. It has therefore not been printed but is available in mimeographed form on request to Fundamental Research Division, Welding Research Committee.

ure, ignoring or minimizing the effect of the others. These five types are:

I. Cracks which occur while the metal is still at a red heat or white heat. These cracks in general are intergranular in nature, following the boundary of austenite grains. They are called by the Germans "Warmrisse."

II. Cracks occurring apparently below the transformation temperature and within the so-called blue-brittle range or secondary brittleness range of the material. These cracks run either at right angles to the weld or along the length of the weld.

III. Cracks connected with martensite formation. These seem to be mainly of two forms: (*a*) minute intragranular cracks, extending transversely across individual martensite needles and (*b*) "quenching" cracks, which are intercrystalline, following austenite grain boundaries through or in the immediate vicinity of martensite areas.

IV. Failures resulting from the geometry of the pieces being welded, or of the weld itself. These result from the combination of a fairly high degree of restraint at the edges of the piece coupled with the presence either of notch concentration effects or of a short section which is thin and relatively weak, between two thicker sections, as after the placing of the first bead in a multilayer weld.

V. A fifth, and rare type of failure, important in spite of its present-day infrequency because of its seriousness when it does occur, is that occurring some time after the welding and resulting from the combination of live load stresses with entrapped stresses or from the entrapped stresses alone. This obviously gradually merges into the case where failure results simply from excessive live load stresses alone, usually at points of undesirable stress concentration, and it is difficult to draw the line between the two cases.

Incidentally, during this investigation, there were found no publicly described cases of post-welding failures which could be clearly ascribed to entrapped stresses. For obvious reasons, this is unfortunately not adequate proof that such failures do not occur, or that presence of entrapped stresses can be neglected in design. It does, however, indicate that failures are very rare in comparison to the large number of welds now in satisfactory service, in which the initial entrapped stresses undoubtedly at some one or more spots reached the yield strength of the material.

The high temperature cracks of Type I seem to be more prevalent in the welding of relatively thin sheet or tubing as in aircraft work. In some cases these failures seem to have occurred at temperatures not so far below the melting point of the metal as to prohibit the interpretation that the intergranular material was still fluid or at least very low in strength. Fractures of this kind are closely analogous to "hot tears" in castings, and the research work now under way at the U. S. Naval Research Laboratory and elsewhere on this subject should be studied carefully in this connection.

This research work demonstrates very clearly the existence in normal steel castings of a brittle range extending from 50 to 150° C. below the melting point. In the presence of certain impurities which tend to influence grain boundary properties, this brittle range may extend to considerably lower temperatures.

The breaks occurring immediately above the transformation temperature may be simply an extension of this range, but it seems at least equally likely that they represent a second zone not normally brittle but in which a combination of grain boundary weakness due to impurities, with tri-axial stress, may result in causing apparent brittleness.

Failures occurring while the steel is in the "blue-brittle range" or "range of secondary brittleness" represent a different problem. Failures occurring in welds of low carbon steel in plates of moderate to large thickness are apparently rather generally, in oral conversations, attributed to blue-brittleness in the material. These cracks are usually associated with structures in which the welded material is under a relatively high degree of restraint. They may run either perpendicular to the weld or at right angles to it. One of the best methods of study of the susceptibility of a given base metal or weld metal to such blue-brittleness would apparently be through notch impact tests at various temperatures. A difficulty which the writer finds in giving any further analysis of this type of failure comes from the fact that to date it has been impossible to obtain definite details, either in published articles or through letters, of a single case of weld failure of this kind. This, of course, is not sufficient evidence to disprove their reality. In fact, collateral evidence in the form of studies of shatter cracks in rails are strongly in favor of their existence.

The type of weld failure which is, at the present moment, under most active discussion is that connected with the formation of martensite. In any steel in which the carbon content is high enough to allow the formation of martensite, there is likely to be present in the neighborhood of each weld a zone in which temperatures were carried above the transformation point, followed by a sufficiently rapid self-quenching action so as to develop hardened areas of metal. Through a combination of circumstances, each of these areas is a zone either of rupture or of very high stress concentration. This is due to the fact that the transformation from austenite to martensite with its accompanying increase of about 1% in volume does not occur until after the adjacent material has cooled sufficiently to develop considerable strength. These hard brittle needles of martensite in a sense force themselves into a relatively rigid mass of steel resulting in microscopic cracks within the martensite needles, and in larger quenching cracks likely to follow austenite grain boundaries in their immediate vicinity. Even in the event that failure does not occur during the welding, the presence of the resulting stress concentrations, combined with the microscopic intracrystalline martensite cracks, would tend to promote failure in the near future. The program of tests now outlined by Committee II on Carbon Steels of the Industrial Research Division of the Welding Research Committee should shed very much light on the conditions under which this type of failure may occur.

The fourth type of failure, associated with the "geometry" of the weld, is usually listed as due either to improper design or to improper welding technique. The welding engineer or welder who suffers from it generally experiments with modified design or modified technique until he has solved his trouble and then files away the information so gained in his own memory. Detailed information as to weld failures of this type is consequently difficult to obtain except within the limits of one's own organization and then usually orally. Very little published information could be found in a search which extended over several months, and only two or three cases were received as a result of the request for information printed last fall in THE WELDING JOURNAL. In spite of this lack of information, failures of this type must nevertheless be regarded as of considerable importance, both because of the frequency with which they are likely to give trouble to engineers unskilled in welding design and because of the limitations which skilled welding engineers and welders impose upon their own work in order to prevent their occurrence.

This analysis of the conditions under which thermal expansion and contraction stresses are likely to cause failures in welded structures (rather than warping or other changes in size) brings out rather clearly that, for failures during welding, the problem is one of determining those conditions under which the normally expected plastic flow of the material is inhibited, so encouraging the formation of cracks. Post-welding failures may represent either a delayed result of damage which actually occurred during the welding operation, or may come as a result of a later combination of thermal stresses and live load, with or without shock loads or differential chemical attack. Here there is room for considerable study and discussion as to the rate of decay of the initial entrapped stresses under the combined effect of creep and of the "working effect" of combined loads.

THEORY OF CRACK FORMATION

Under normal conditions the stresses resulting from thermal expansion or contraction in welded joints expend their energy in producing plastic flow as evidenced by the changes in size and in shape produced during welding. These changes are inconvenient but not necessarily dangerous. Unless the pieces being joined are unusually free from external restraint and relatively small in size, an after-effect in the form of trapped stresses approaching the yield point of the material in magnitude will be present. The total amount of shrinkage during cooling from the molten state, which may run somewhat over 1%, is far enough above the 0.1% or 0.2% limit of elastic elongation possible at room temperature (and the lower limits of elastic elongation present at higher temperatures), so that the differential effects produced are almost certain to cause the elastic limit to be exceeded. It is nevertheless much below the 15 to 40% elongation at failure which would be expected in a 2-inch tensile test specimen of weldable steel. High trapped stresses are therefore to be expected, but failure by cracking will occur only if through some mechanism the normal plastic yield of the steel is inhibited.

The conditions under which this is possible will be better appreciated if one thinks of any metal, whether brittle or ductile, as having two clearly differentiated types of strength—a separation or cohesive strength measuring the force in tension required to cause fracture along a plane perpendicular to the applied tension, and a shear strength which measures the shearing stress required to cause plastic flow in the material. Sometimes in place of shear strength the term, flow strength, may be used. This represents the tensile stress which, acting in one direction only, will produce sufficient diagonal shearing stress to cause plastic yield of the metal. If, in any metal, the cohesive strength is greater than the flow strength, an ordinary tension test bar may be expected to yield plastically until, as a result of changes in shape of the bar and of cold work upon the material, the relationship between cohesive strength and flow strength has so changed as to allow separation failure. If, on the other hand, the resistance to flow in the bar is greater than the resistance to cohesive fracture, the bar will break in a brittle fashion.

Unfortunately, the magnitude of the per cent elongation or per cent reduction of area in a tensile specimen is not a valid indication as to how wide a margin initially existed between the cohesive and flow strengths of the material. If this margin is small then the presence of a relatively small amount of three-dimensional tension superposed upon a uni-directional tension may cause a sudden change from a ductile to a brittle form of failure. If, however, another material with identical properties in the usual tensile test happens to have a wider margin between its flow strength and its technical cohesive strength, then it will continue to be ductile even under conditions which would cause a brittle failure with the first material. The paper by McAdam and Clyne on "The Theory of Impact Testing" presented at the Impact Symposium held in June 1938 under the joint sponsorship of the Welding Research Committee and the American Society for Testing Materials, published in the 1938 Proceedings of the American Society for Testing Materials and also in separate form in a volume containing the papers presented at the Impact Symposium, discusses this theory in much greater detail.

The various conditions under which crack failures may occur show up very clearly with use of this theory. In the presence, for example, of three-dimensional tension of equal magnitude in all directions, shearing stresses are absent and failure must occur in a brittle fashion as soon as the technical cohesive strength of the material is exceeded. In the presence of uni-directional tensile stress, a brittle or crack failure will occur only if the technical cohesive strength of the metal is less than its flow strength. In either case, cohesive failure may occur either through the grains or along grain boundaries, depending upon which path is weaker. Plastic flow necessarily involves shearing slip along crystal planes within the grains themselves, accompanied by readjustments along grain boundaries so as to compensate for the differing directions in crystal planes in adjacent grains. In hot tears in castings and in certain types of high temperature breaks in castings, apparently the cohesive strength along grain boundaries is low, so that the separation follows the austenite grain boundaries. In other cases, the cohesive strength along the boundaries may lie intermediate between the flow strength and the cohesive strength through the grains. In this case, under one directional tension, the welded joint will yield plastically, but the addition of a small amount of three-directional tension will cause intergranular fracture instead. Presence of oxides, sulphides or other impurities in the metal, especially those which tend to segregate along grain boundaries, also affect the result by changing the relationship between the flow strength of the grain and the cohesive strength of the grain boundary. Similarly, the geometry of the piece and the technique of welding affect the result by changing the relationship between the three-dimensional and the one-dimensional tensile stresses.

Notch effects resulting either from the shape of the piece or from the presence of impurities cause the development of three-dimensional stress and so promote crack failure in place of plastic yield. Large size notches, such, for example, as are present after placing the first layer of weld metal at the base of a deep V-groove, have a doubled effect. There will be present here both a tendency to crack due to the three-dimensional stresses, present particularly at the root of the weld, and also a tendency to concentrate nearly all the shrinkage strains within the small width of weld metal due to the high strength and rigidity of the adjacent thicker material.

Martensite cracks have a somewhat different origin. The stresses present result in a large measure from the 1% or so increase in volume which occurs as the austenite changes to martensite. This change does not occur until the metal has dropped to a relatively low temperature. Thus, the metal both on the outside and on the weld side of the martensite area has had time to cool down and develop relatively high strength and stiffness. The resulting stresses will be great enough either to cause immediate failure, or, lacking that, the possibilities of

future difficulty. Presence of shrinkage stresses due to external restraint will intensify the trouble.

TO WHAT EXTENT ARE ENTRAPPED STRESSES PERMANENT?

There exists a general belief that entrapped stresses, even if initially high, gradually dissipate themselves under service conditions. If so, this must occur through plastic yield or creep in the material, occurring at service temperatures. Room temperature creep in steel is usually considered to be negligible in magnitude, even as compared to the usual small elastic strains. Experimental evidence obtained by the writer in connection with research work carried on in cooperation with the Research Committee on Mechanical Springs of the American Society of Mechanical Engineers and reported in several papers[1] before that Society indicates, however, that the rate of internal hysteresis and with it the rate of creep is very greatly accelerated following even a small amount of plastic flow in the material of the order of 0.1% or less. Metal in the neighborhood of a weld will nearly always have undergone plastic yield of at least this amount, so that it does not seem possible to rule out creep as an important factor. Presence of service loads acts to increase the stresses and so accelerate this creep. In addition, if at any point the combined working stress and entrapped stress rise above the elastic limit, plastic flow tends to relieve the excess stress present. On removal of the service stresses the residual stress still remaining will tend to equal the difference between the elastic limit and the applied service stress. Any further reduction in residual stresses would have to come as a result of occasional overloading acting upon the material or as a result of room temperature creep in the metal.

AFTER-EFFECT FROM ENTRAPPED STRESSES

The problem of after-effects from entrapped stresses in welds is so closely allied to the problem of after-effects from similar entrapped stresses arising from any other cause, whether by cold rolling or other forms of cold work on metal, or by differential expansion or contraction during heat treatment of steel or by shrinkage fits or force fits used in manufacture, that all of these various cases may well be considered together. Merging the problem in this way, it is possible to obtain much more information on the subject than if after-effects in welds are considered alone. The following tentative statements are probably possible. They are not sufficiently well proved to be treated as conclusions. They are rather to be thought of as hypotheses, put forward with a view to suggesting lines of further study.

1. The effect of entrapped stresses is likely to be negligible under steady static loading, since, when overstressed, plastic yield occurs in a direction tending to reduce the effect of the initial stresses.

2. Under fatigue loading, with an indefinite number of repetitions of stress, failure will occur if the combined effect of creep and plastic flow is not sufficient to bring the peak internal stress below the endurance limit of the material. If, however, peak stress is below the endurance limit (or below the "damage line" for a lesser number of repetitions of stress), then failure will not occur.

3. It has been very well stated that the design of any engineering structure would be impossible unless we could assume that material would behave elastically, and yet that no engineering structure would stand up unless at points of localized stress the material in the structure deviated from perfect elasticity. We rely virtually completely upon plastic flow to alleviate the effect of localized stress concentrations, and of shrinkage stresses. It should be noted that (*a*) plastic flow results from shearing stress, not tensile stress, and that under three-directional tensile stress or stresses approximating it in character, the shearing stress is absent or small in amount as compared to the tensile stress, so that brittle failure will occur even in a normally ductile material; and (*b*) for plastic flow to occur, metal must have some place to flow from and some place to flow to, while still maintaining continuity. In thick sections, the tensile stresses due to shrinkage or to cold working may rise to very high values locally in the interior, the rigidity of the exterior layers being sufficient to prevent plastic flow from occurring.

4. The dynamic effects resulting from sudden blows are still far from being understood. The combination of a relatively small sudden blow acting on a piece with unrelieved three-directional tensile stress or on a piece with high internal stresses at points of thick section, seems to be especially dangerous.

5. Any discussion of after-effects from entrapped stresses is inadequate unless it recognizes that failures may have been the result not of the entrapped stresses, but rather of hidden damage, internal cracks or seams, which were caused by the same forces which produced the entrapped stresses, but which were not discovered until later. This distinction is an important one. A stress relief heat treatment may be amply adequate to care for the entrapped stresses, but it will not cure any cracks of sensible size which have already occurred. A change in welding technique will be needed.

6. It is necessary to note here a possible exception to the statement which has just been made. There are good reasons for belief that in some cases submicroscopic cracks may be formed under the effect of overstress and then actually heal themselves later when the stress is removed, even at room temperature, with a resultant improvement in strength with time. A stress-relief heat treatment may help both by removing part of the internal stress and by imparting greater mobility to the atoms. It would, however, be very dangerous to extrapolate from this into the broader statement that "cracks heal with time."

7. Where a welded structure is likely to be subject to corrosion, presence of entrapped stresses may result in damage of an entirely different character, since (*a*) points of differing stress are likely to be points of differing electrochemical potential, resulting in electrochemical attack, and (*b*) at points of high tensile stress, chemical attack once started results in points of notch stress concentration with resulting rapid failure, both from physical and chemical causes, particularly if there is any "weaving" action in the structure or if alternating loads are present.

RECOMMENDATIONS FOR FUTURE WORK

The analysis just given in large measure carries with it its own recommendations. A difficulty is that the problem is inherently the large question of the effect of entrapped stresses resulting from any cause, rather than simply a problem dealing with welding alone. Any answer to this question would be of very widespread

[1] "Elastic and Inelastic Belavior in Spring Materials," *Trans. A. S. M. E.*, vol. 52, A. M., p. 103; "Elastic Behavior of Spring Materials," *Proc. A. S. T. M.*, vol. 30, part II, p. 546; "Elastic and Inelastic Behavior in Spring Materials, Continued," *Trans. A. S. M. E.*, vol. 53, A. M.-8, p. 99; "Laws of Elastic Behavior in Metals" and "Plastic Behavior in the Light of Creep and Elastic Recovery Phenomena," *Trans. A. S. M. E.*, vol. 56, pp. 555 a nd 559.

value, but obviously also the question is of too wide a scope for any attack by the Welding Research Committee, acting alone. A more feasible program would be one directly connected to the welding aspects of the problem alone.

Specifically, there is strong evidence in favor of the belief that although internal stresses in the neighborhood of welded joints are likely to be large immediately following welding, nevertheless, only rarely are they responsible for any future failures. Definite proof of this statement is highly desirable. Such proof should take four forms: (*a*) collection of information concerning any failures which may occur or difficulties which may arise, each case to be investigated to discover to what extent the difficulty resulted from initial stresses as contrasted to other causes; (*b*) experimental study of the effect of overload and of room temperature creep in welded joints (or other structures) upon the amount of entrapped stress, so as to study the mechanism by which reduction in entrapped stress may occur; (*c*) experimental measurements of internal stresses in welded joints immediately after fabrication and after considerable periods of use, to determine the extent to which these stresses actually have "worked themselves out;" and (*d*) endurance tests of welded joints known to contain internal stresses with parallel tests of similar welded joints after stress relief heat treatment.

Collection of information as to service difficulties has been handicapped by the fact that in many cases industrial concerns are very loath to advertise the existence of these difficulties, and by the fact that in other cases the occurrences do not seem to be significant enough to justify making any permanent record. By cooperative effort it should be possible to establish some arrangement by which these difficulties could, in the main at least, be overcome.

The obvious method of attack is by direct measurement of decrease in internal stresses as in the third method given above. This method has two objections: (1) it is experimentally very hard to carry out, and (2) any results may be criticized as applying only to the particular case under test. In spite of this, experimental work of this nature is highly desirable and should, if possible, be undertaken.

The third method of attack aims to answer the question, "Do entrapped stresses work themselves out in service?" The second one attacks the equally important question, "By what mechanism does it happen?" If it does occur, it must be by some form of plastic flow which relieves the internal strains. This must be either as a result of occasional overloads, which cause local stresses above the elastic limit, or as a result of room temperature creep in the metal. A typical program of investigation would include, first, measurements of room temperature creep in non-welded material in the as-received condition, and after three or four different percentages of permanent deformation ranging possibly from 0.1 to 2%. This should be followed by similar measurements on strips from welded joints taken both along the joint and at right angles to the joint. Creep under constant load should be measured as well as the so-called relaxation stress mechanism, i.e., the way in which creep continues under the gradually diminishing stress which results from the effect of the creep itself. In so far as feasible, these experiments should be supplemented by measurements of decrease of entrapped stress within actually welded construction of as already suggested.

Much work has been done in the past on the fabrication of welded joints under various artificial forms of constraint followed by a measurement of the resulting internal stresses. To the writer this line of investigation seems to be of only limited value. There is ample evidence that the amount of internal stress present will depend upon the details of the particular joint, and that, in any event, it is likely to be high in magnitude. Any quantitative measurements made will, therefore, have reference only to the particular type of construction being investigated and will bear only to a secondary extent on the problem as a whole.

Failures during fabrication constitute another and important part of the same general problem. Stresses of large magnitude are almost certain to occur so that physical measurements of their size are of less importance than a study of the conditions under which they may become dangerous. This seems to involve principally a study of the tendency toward crack failure as contrasted to plastic flow, and so to lead to measurements of behavior under tri-axial tension and combined tri-axial and one-directional tension. Since these fractures usually occur at elevated temperatures, it would seem obvious that the moderate and high temperature properties of the material may be equally valuable with room temperature properties. One of the few ways of gaining information about behavior of this type in the past has been by use of the standard notch impact test. The velocity effect in this test is actually unimportant as compared to the effect of the three-dimensional stress produced at the root of the notch. Varying the width of the specimen is one way of varying the relationship between three-dimensional and one-dimensional stress in this test. Other and more precise methods for the study of two- and three-dimensional tensile stresses are needed and should be developed, such as have recently been discussed in the subcommittee on stresses of the Industrial Welding Research Division.

This paper is not intended or thought of as an inclusive statement. Its intent is mainly to urge, first, a somewhat franker publicity as to the reasons for such weld failures as may occur—they are few enough now so that the welding industry can well stand it—and, second, to urge that to the present effective methods of rectifying individual welding problems one by one as they occur, there be added, as far as time or opportunity allows, an attack on the general basic problems involved.

WELDABILITY

A most unusual and interesting round-table conference on the subject of Weldability was held in Chicago during the Annual Meeting of the AMERICAN WELDING SOCIETY on October 23rd. This conference was arranged under the auspices of the Industrial Research Division of the Welding Research Committee and was presided over by Professor M. F. Sayre of Union College. The general topic of Weldability and what it means was given broad consideration as was, also, methods of testing for weldability. It is expected that an abstract of the important views presented at this conference will be available shortly. Copies may be obtained upon request of the Welding Research Committee, 29 West 39th Street, New York. About a hundred people attended this conference.

WELDED STRUCTURES IN SOME CAST and Wrought Steels*

By J. W. BOLTON† and A. J. SMITH‡

INITIAL procurement of desirable metal microstructure is an essential feature in the production of weldments for severe engineering service. Although a sound weld properly stress-relieved is a necessary feature, avoidance of an initially brittle or otherwise structurally undesirable layer also is required.

In this investigation the behavior of certain cast and wrought metals when welded under relatively severe conditions was studied. Welding was conducted both with and without preheat. An objective was to ascertain what happens during and immediately following the welding operation.

Single bead welding was used, to provide a relatively severe condition, without the "self anneal" effect brought about by laying or layering of several beads. Because of this, also the use of relatively heavy sections and an incomplete (or "three quarter round") weld, conditions are more drastic than usually are found in commercial welding at the initial temperature given.

Questions are: What metals are relatively "foolproof," what preheats are necessary to avoid initially unsuitable structures, what are some of the dangers of indiscriminate preheating, and what are the uses and limitations of some of the common testing methods?

This paper is in itself a highly condensed version of a report by the Research Division of The Lunkenheimer Company.

EXPERIMENTAL PROCEDURE

Welds either were (*a*) laid in grooves in solid bars or (*b*) bars of different base metals were fastened together mechanically, a groove cut along the joint, and weld metal laid therein.

The design of specimens and furnace setup is shown in Figs. 1 and 2. $2^1/_2$ inch diam. bars were butted together and held tightly by the screw A, or in some cases a solid bar was used. A circular groove, round bottom, was cut for welding. The assembly was placed in furnaces,

* Contribution to Welding Research Committee.
† Chief, Metallurgical Division, The Lunkenheimer Co.
‡ Research Metallurgist, The Lunkenheimer Co.

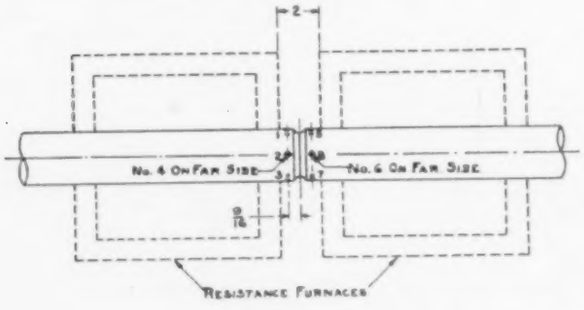


Fig. 1

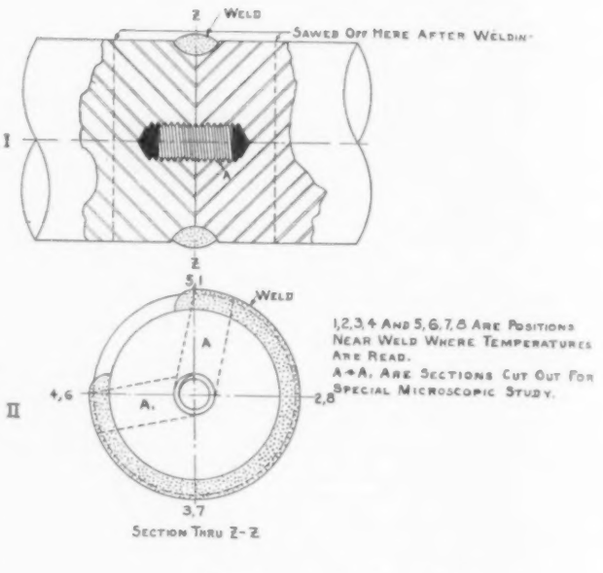


Fig. 2

as illustrated and temperatures before and during welding were read by a number of thermocouples.

The furnaces were built so that temperature uniformity was very good. They were held at a given preheat temperature by automatic controllers. Space between furnaces where welding was done was as small as convenient for laying of weld.

Welding was started at position 1–5 (Fig. 1) and continued, a single bead, $^3/_4$ way around or 270°. Temperatures during welding were recorded.

Figure 2-I shows longitudinal cross section, showing weld and screw. The bar then was cut off about 1 inch each way from center line of weld.

Section Z–Z rotated 90° is shown in Fig. 2-II. Section A & A_1 (Fig. 2-II) was then cut out.

One such section is shown in Fig. 2-III-X. The bottom of the segment was sawed off and discarded. Fig. 2-III-Y shows the segment rotated 90°. Full lines indicate specimen face used for tests described hereinafter.

After sectioning, the test specimens were polished carefully and examined microscopically. They then were repolished and hardness tests made. After hardness testing, specimens were macroetched and photographed.

Table 1

No. Base Metal	Symbol	C	Si	Mn	P	S	Ni	Cr	Mo
1	WCM-15—wrought	0.19	0.14	0.65	0.010	0.033	..	..	0.41
2	CC-25—cast	0.23	0.38	0.66	0.028	0.028	..	..	..
3	CNCM-25—cast	0.26	0.30	0.47	0.029	0.029	0.87	0.50	0.37
4	CCM-25—cast	0.27	0.40	0.70	0.033	0.029	0.10	0.03	0.50
5	WCM-25—wrought	0.27	0.20	0.48	0.009	0.048	..	..	0.41
6	CCM-35—cast	0.38	0.46	0.66	0.028	0.031	No	No	0.48
7	SAE 1040—wrought	0.41	0.19	0.75	0.013	0.047	..	..	..
Electrode									
A	Cellulose coated*	0.07	0.12	0.39	0.012	0.025	..	..	..
B	Mineral coated*	0.08	0.04	0.46	0.029	0.037	..	Tr.	0.41
C	Cellulose coated*	0.13	0.12	0.34	0.020	0.025	0.09	0.01	0.46

* Analyses made on deposited material.

Hereinafter, position 1–5, beginning of weld, is called Position 1, and position 4–6, end of weld, is called Position 4.

It was found that the thermal conductivities of the metal used varied so slightly that no perceptible difference between (*a*) "solid" bars and (*b*) "butted" bars of different base metals could be found. Hence, each material vs. rod combination can be treated as a unit as outlined in Table 2. The actual experimental combinations are also given in Table 2.

Electrodes of $^{5}/_{32}$ inch in diam. were used. Current input of 150 amp. was used for electrodes A and C (cellulose coated) (see Table 1) and 175 amp. for the heavily mineral coated rod B.

MATERIALS USED

The materials used and their analyses are given in Table 1. Cast materials were made by acid electric furnace process. Wrought materials were bar stock. Electrodes were commercially well known and widely used types.

Table 2

Series	Base Metal	Electrode	Welded to	Preheat	Test No.
(1) a	WCM-15	B	CCM-25	No	10
b	WCM-15	B	CCM-25	200° F.	13
c	WCM-15	B	CCM-25	600° F.	14
d	WCM-15	B	CNCM-25	600° F.	15
(2) a	CC-25	A	Stainless	No	1
a	CC-25	A	SAE 1040	No	4
b	CC-25	A	Stainless	200° F.	2
b	CC-25	A	SAE 1040	200° F.	6
c	CC-25	A	Stainless	300° F.	3
c	CC-25	A	SAE 1040	300° F.	9
(3) a	CNCM-25	B	Stainless	No	5
a	CNCM-25	B	WCM-25	No	11
b	CNCM-25	B	Stainless	200° F.	7
c	CNCM-25	B	Stainless	300° F.	8
d	CNCM-25	B	WCM-15	600° F.	15
d	CNCM-25	B	Solid Bar	600° F.	21
(3) A-a	CNCM-25	C	WCM-15	600° F.	16
(4) a	CCM-25	B	WCM-15	No	10
b	CCM-25	B	WCM-15	200° F.	13
c	CCM-25	B	Solid Bar	400° F.	20
d	CCM-25	B	WCM-15	600° F.	14
d	CCM-25	B	Solid Bar	600° F.	18
(5) a	WCM-25	B	CNCM-25	No	11
(6) a	CCM-35	B	Stainless	No	12
(7) a	SAE 1040	A	CC-25	No	4
b	SAE 1040	A	CC-25	200° F.	6
c	SAE 1040	A	CC-25	300° F.	9

GENERAL FINDINGS

For the convenience of the general reader a summary of findings is given here first, and more detailed evidence follows. It is hoped that this departure from conventional sequence will justify itself by making the report more easily followed by the average reader.

RESULTS—NO PREHEAT

The general results and findings are as follows:

(1) WCM-15: The lowest carbon material, the WCM-15 (0.19 per cent carbon) was the only one that showed no trace of a martensitic or near martensitic condition, without preheat, at any position in the weld affected areas.

(2) CC-25: Greatest quenching action was at Position 1, the beginning of laying of bead. At this position no actual martensite was found in the CC-25 (0.23% carbon.) Microstructure shows in spot a near approach to that condition.

(3) CNCM-25: In alloy CNCM-25 (0.26 per cent carbon) a slight martensitic or near martensitic layer was found at Position 1. The thickness of this layer was hardly 0.010 inches. "On prospecting" a single high hardness of 250 Bhn. was found.

(4) CCM-25: One high hardness reading (380 Bhn.) was found in the CCM-25 (0.27 per cent carbon) but definite martensitic areas were not located. A near martensitic condition prevailed.

(5) WCM-25: WCM-25 (0.27 per cent carbon) formed a near martensitic layer although no hardness readings over 256 Bhn. were found.

(6) CCM-35: Definite martensitic condition was found in the CCM-35 (0.38 per cent carbon) although 320 Bhn. was the hardest that could be located.

(7) SAE 1040: Pronounced martensitic formation with a suggestion of retained austenite in the SAE 1040 (0.41 per cent carbon) was found, although high hardness readings could not be located.

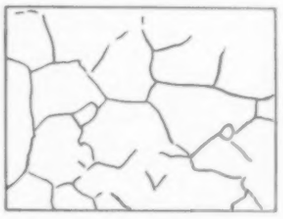


Fig. 3—Grain Size of CC-25 No Preheat

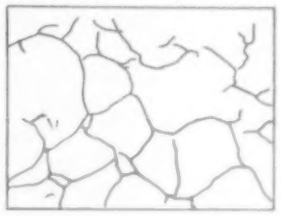


Fig. 4—Grain Size of CC-25 300° F. Preheat

Fig. 5—CNCM-25 at Junction. 200° F. 100 Diam.

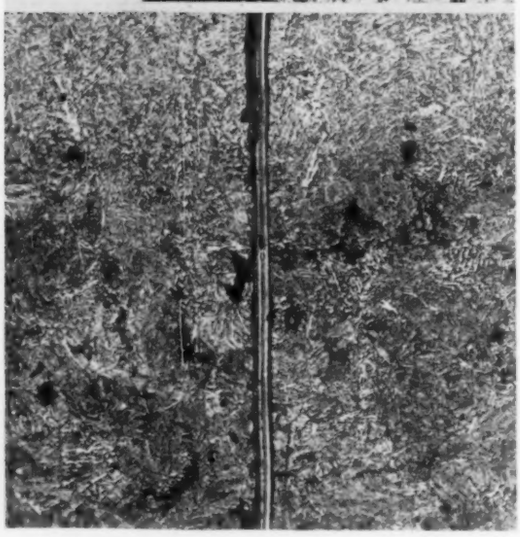


Fig. 6—CCM-25 at Junction. 400° F. Preheat 100 X

The greatest likelihood of martensitic formation is of course just below the weld juncture, and in case of the low alloy steels, within less than 0.02 inches from it.

MARTENSITIC STRUCTURES

For purposes of comparison, brine quenched samples of CC-25, CCM-25 and CNCM-25 were prepared and examined. These were martensitic, and hardnesses were 400 Bhn. for the CC-25 and 440 Bhn. for the CCM-25 and CNCM-25.

It is important to note that even after a drawing treatment the originally martensitic structures retain an appearance resembling martensite. To illustrate this feature 440 Bhn. martensitic CCM-25 was drawn at 500° F., hardness then 400 Bhn., and at 700° F. hardness then 229 Bhn. Although the 700° F. draw (229 Bhn.) sample is reasonably soft, the orientation of the essentially sorbitic structure still is suggestive of the original martensitic arrangement.

The region from the weld juncture to about 0.03 inch below the weld juncture is momentarily molten—we might call this zone a "puddled zone" where metal is molten or highly plastic. At Position 1 this puddled zone (no preheat) naturally cools very rapidly, and structural formations suggest metal cooled rather rapidly from a molten or near molten state. Whether actual brittle hard martensite forms depends upon the hardening propensities of the particular steel, the rate of cooling, and the amount of "draw" brought about as the bead progresses.

GROUPING OR CLASSIFICATION

On basis of relative tendency toward martensitic formation on "cold" welding, first part of bead, Position 1, the materials fall into three groups, as follows:

(1) WCM-15 (carbon under 0.20 per cent)

(2) CC-25
CCM-25 (carbon above 0.22 per cent, below 0.28 per cent)
WCM-25
CNCM-25

(3) CCM-35 (carbon about 0.40 per cent)
SAE 1040

Within group 2 the lowest carbon steel (CC-25) had slightly the least tendency toward martensitic formation, the alloy CNCM-25 slightly the greatest. However, the differences are such that variations in welding technique, or differences in structures to be welded might even reverse the relative positions.

Group 3 definitely forms martensite on first part of bead, Position 1, and precautions to avoid this undesirable condition should be prescribed.

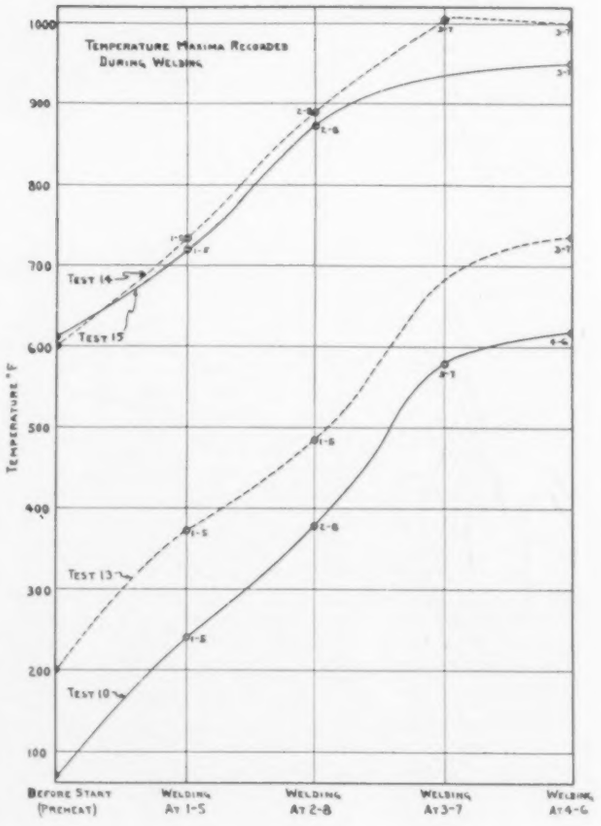


Fig. 7

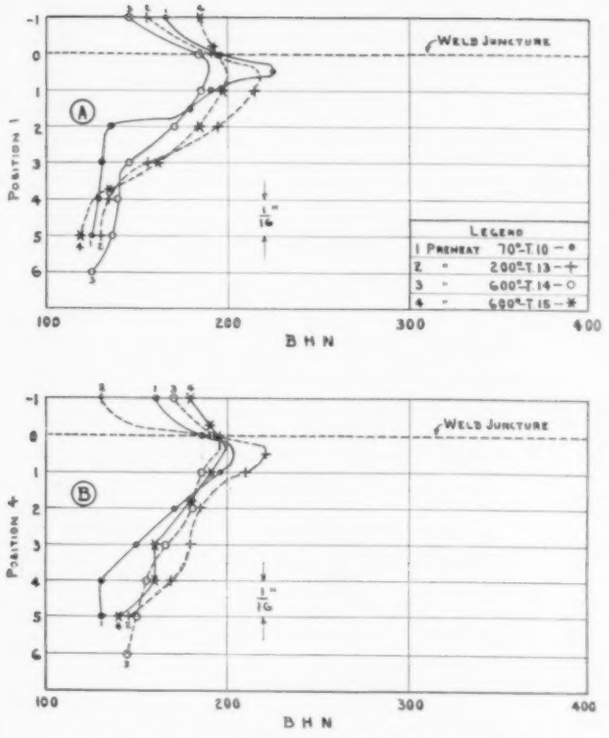


Fig. 8—Hardness WCM-15 Welded with Electrode B

WELDING BEYOND POSITION 1

So far we have dealt exclusively with the very first part of the weld, deposited on "cold" metal of rather heavy section. As the bead progresses, the temperature of the base metal both preceding, at, and after the position of welding increases. This trend was shown by the thermocouple readings.

As illustrative, in tests on Alloy CNCM-25 welded with Electrode B (mineral coated), no preheat, when welding at Position 1–5, temperature of 200° F. was recorded 1/8 inch away from weld. When welding at Position 2–8, a temperature of 450° F. was recorded. When welding at Position 3–7, a temperature of 580° F. was recorded. This reached about 600° F. when weld was 3/4 way around bar, at Position 4–6.

This means that after the first drop is laid "cold" there is "automatic" preheating induced by the heat generated in the welding. This heat tends to draw the first part of the bead—hence welding speed and power input are factors influencing the "hardness" of the first part of the bead. As the bead is being laid, heat precedes it and there is "automatic" preheat already at the point of welding, and a drawing effect from the heat behind it.

Due to the "automatic" preheat effect, no martensitic or near martensitic condition was found at Position 4 (end of weld bead) in any of the specimens in the three groups.**

RESULTS OF PREHEATING

(1) WCM-15: If freedom from martensitic formation were the sole reason for preheating it would not be necessary to preheat this material. Preheating, however, may be desirable to minimize thermal shocks, the possibility of crack formation, and the development of high residual internal stress. Provided that the weldment can withstand thermal shock (and consequent expansion and contraction stresses) without cracking, removal of internal stresses usually is more satisfactorily accomplished by suitable stress relief after welding.

** One disappointment in this work has been the inadequacy of hardness testing methods in evaluation of these weldments. Freedom from brittle structure, such as hard martensite (even though such structure might cover but small spots or thin layers) appears desirable. In these studies many of the hard areas were so small or so localized that they not only are easily missed with a Rockwell penetrator, but when known to be present are not easily located, and when located, the penetrator may "straddle" the spot and an incorrect and low reading obtained. Although charts showing hardness readings with different preheats are shown in detailed data presented later, they are useful principally in showing depth of hardening, and of but limited value for showing isolated or localized "hard spots."

Another limitation is that hardness readings do not show or suggest coarsened grain structures when these undesirable grain structures and undesirable formations are present.

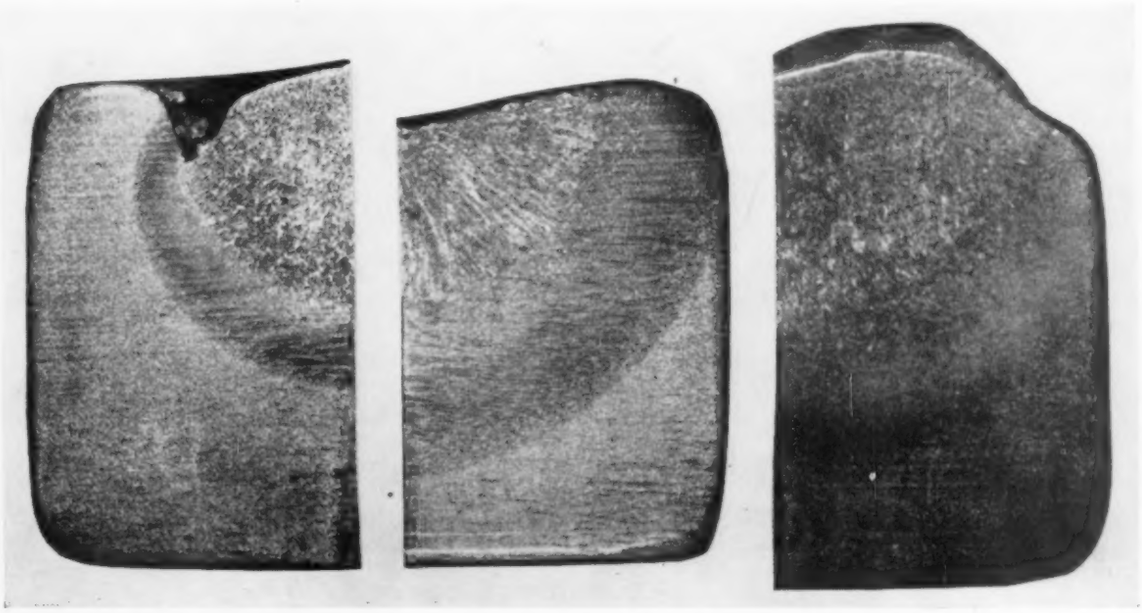


Fig. 9—WCM-15 Pos. 1. No Preheat 3.5 X

Fig. 10—WCM-15 Pos. 4. No Preheat. 3.5 X

Fig. 11—WCM-15 Pos. 1. 200° F. Preheat 3.5 X

Fig. 12—WCM-15 at Junction. No Preheat. 100 X

Fig. 13—WCM-15 at Junction. No Preheat. 500 X

Fig. 14—WCM-15 0.04 Inch from Junction. 100 X. No Preheat

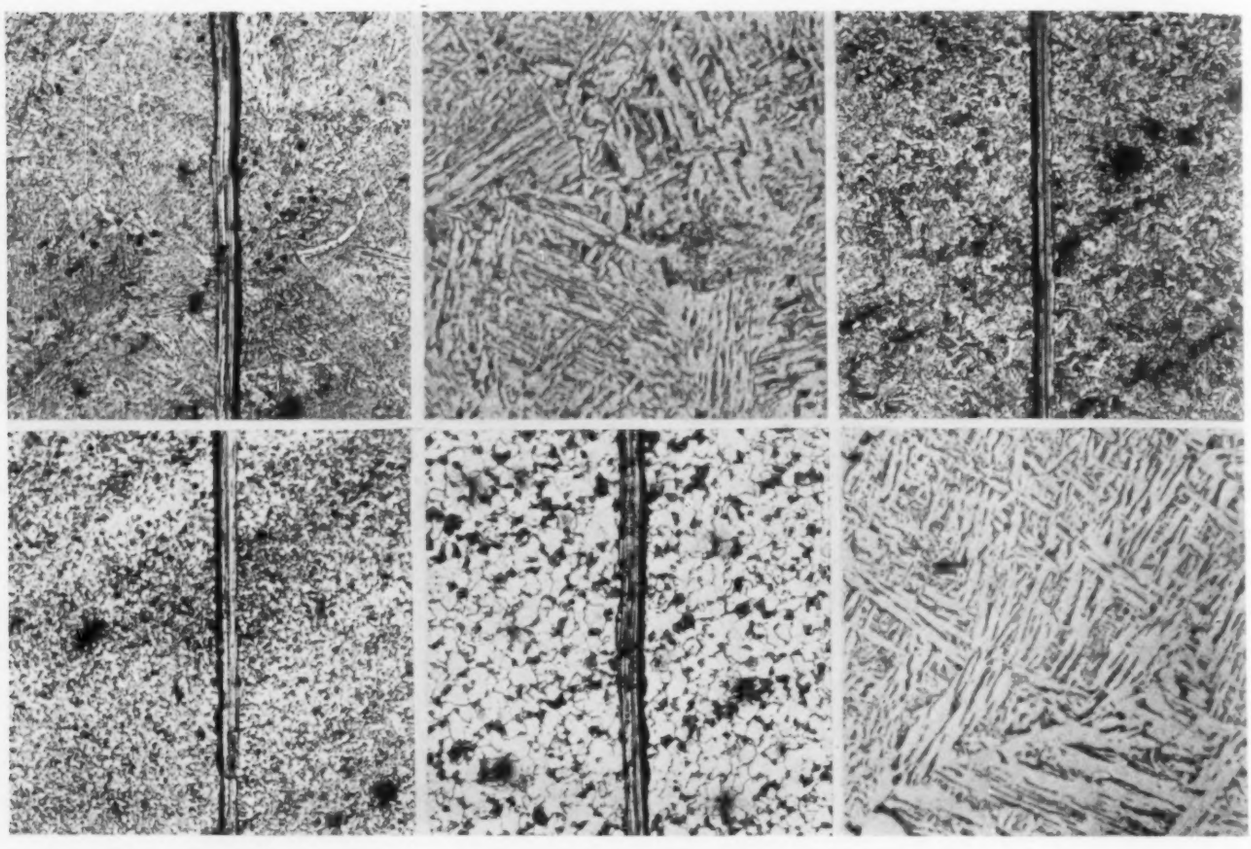


Fig. 15—WCM-15 0.08 Inch from Junction. 100 X. No Preheat

Fig. 16—WCM-15 0.16 Inch from Junction. 100 X. No Preheat

Fig. 17—WCM-15 at Junction. 200° F. Preheat 500 X

When WCM-15 is preheated to only 200° F. a tendency toward a coarsening of grain structures is perceptible. Compare Fig. 17 with Fig. 13. Note Fig. 17 shows evidence of the commencement of coarsening. (Both photomicrographs are at 500 diam.)

The juncture of weld metal and WCM-15, no preheat, 100 diam. is shown in Fig. 12. Figure 18 (100 diam.) shows very coarse granulation brought about by preheat of 600° F. Sketch of grain boundaries is shown in Fig. 19.

From a microstructure view-point, preheating of CCM-15 is unnecessary. Although a perceptible tendency toward coarsening is noted at 200° F. preheat, this is not serious, and may be offset by other considerations. Preheat as high as 600° F. is definitely detrimental. Coarse grained structure not only is of low ductility, but usually is not resistant to impact and vibratory stresses.

(2) CC-25: Even without preheat coarsening is evident (at 100 diam.) in the CC-25 (see Fig. 21). This is perceptibly increased at preheat of 300° F. (see Fig. 24). Figure 3 and 4 are outlines or tracings of grain boundaries near Figs. 21 and 24, respectively.

A little localized preheating would be desirable to insure absolute freedom from any chance of formation of small martensitic areas at beginning of weld. Figure 25 shows well broken up ferrite sorbite structure in base metal near the weld, at 300° F. preheat.[1] Figure 25 should be compared with Fig. 23.

The known ease of welding this class of material without necessity of elaborate precautions, and its generally satisfactory behavior in weldments suggest that preheating should be localized (i.e. as by a torch at the beginning of welding) and if general preheat is employed this should be kept low. Nothing much is to be gained

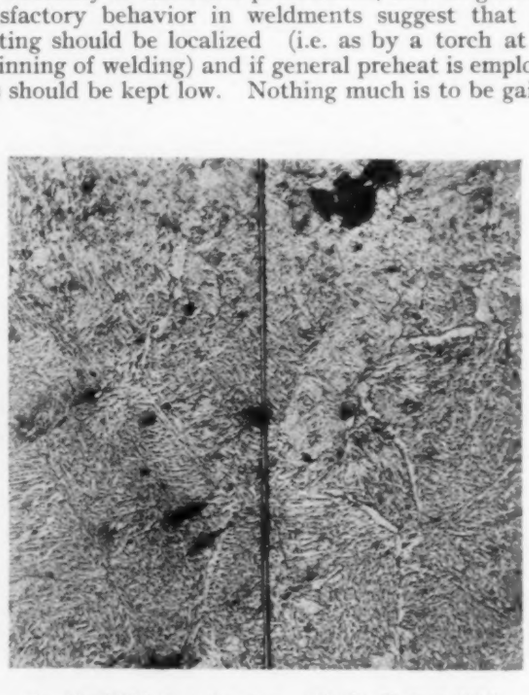


Fig. 18—WCM-15 at Junction. 600° F. Preheat 100 X

[1] Unless otherwise stated all photomicrographs are taken at Position 1 beginning of weld. Portion closest to the weld juncture is placed at top.

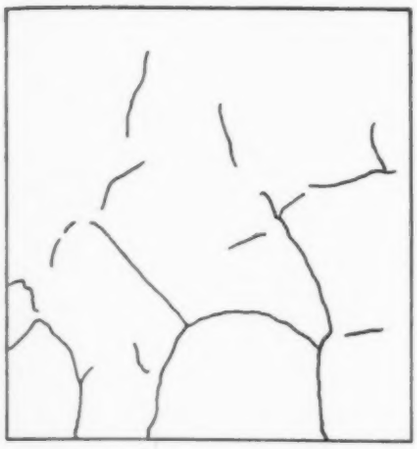


Fig. 19—Grain Size in Fig. 18

by preheating above say 300° F., and it seems likely that higher preheats might result in aggravation of the undesirable coarse grained formation.

(3) CNCM-25: Alloy CNCM-25 was studied at preheats of 200° F. 300° F., and 600° F., as well as room temperature. It will be recalled that with no preheat a martensitic or near martensitic structure was found just at the beginning of the weld. At preheat of 200° F. there is a troostite layer beneath the weld juncture. This at 100 diam. is shown in Fig. 5. (CNCM-25 is at right. Dark or black is opening at mechanical joint. Stainless steel is at left.) The "no preheat" same magnification, is shown in Fig. 31.

At 300° F. troostite did not occur and the structure was largely sorbite-ferrite, at no location exceeding 270 Bhn. Such structure is strong and not brittle. Therefore, a preheat of 300° F. is sufficient to prevent, at this critical position, formation of any brittle spots or layer.

Proceeding to preheats of 600° F., one finds a desirable structural condition, as Fig. 36, 100 diam. No coarsening is apparent. (Compare, for example, with WCM-15, 600° F. preheat, Fig. 18.)

Steel CNCM-25, if preheated to 300° F., shows a desirable structure even at the most critical location. It differs from certain other materials in that it can stand high preheats without granulation and coarse grain formation.

Steel CNCM-25, known as Alloy WH is a Lunkenheimer development and has been embodied in an A. S. T. M. "welding grade" steel specification as A-217-39 T, Grade WC-4.

The CNCM-25 makes an excellent bond with the B electrode (mineral coated) and although under drastic welding conditions (cold weld) some martensitic or near martensitic structures was produced in a small area, no evidence of cracking could be found. Such tendency toward martensitic formation could be easily avoided by preheating the point where welding is begun with a torch to 300° F., or above. As opposed to certain other metals, CNCM-25 can be welded with rather high preheat without formation of undesirable coarse grained structures.

(4) CCM-25: One hard spot was found, Position 1, when studying CCM-25, no preheat. Figure 42 (100 diam.) shows the CCM-25 at 200° F. preheat. A semi-acicular orientation is apparent, but this apparently is not martensitic. A slight tendency toward coarsening is evident.

A well broken up structure at 400° F. preheat is shown in Fig. 6. Some coarsening is evident. The structure is essentially pearlitic.

At preheat of 600° F. the structure is somewhat coarsened.[2]

Some localized preheating of CCM-25, to about 300° F. is advisable. Preheating much over 400° F. is not desirable.

(5) WCM-25: Preheat studies of WCM-25 were not made. Refer to Fig. 56 showing the not preheated sample, at 100 diam., at juncture. Compare this to Figs. 31 and 42. It is the opinion of the writers that WCM-25 should be preheated locally before welding, and that a temperature of 300° F. would be sufficient.

(6) CCM-35: Effects of preheat on CCM-35 were not studied. The material without preheat forms definitely martensitic areas and preheats of 300° F. are suggested. As with CCM-25 too high preheats should be avoided.

(7) SAE 1040: Structure of SAE 1040 near the juncture at 200° F. preheat is shown in Fig. 71 (500 diam.). Martensitic structure is not evident and ferrite divorcement makes the coarse grain size very apparent.

This structure at 100 diam. is shown in Fig. 70. It is very coarse grained. The condition is even worse at 300° F. preheat, as shown in Figs. 74 and 75.

Close examination of Fig. 71 suggests that higher than 200° F. preheat should be prescribed for SAE 1040. However, coarse grain is bad at 200° F. preheat and very bad at 300° F. preheat. The suggestion is that where SAE 1040 is to be used, attempt be made to get stock that does not coarsen so badly. (It is felt that such marked grain growth tendencies could be avoided had steel made with better deoxidization practice been obtained.)

[2] Details on all formations not illustrated in this part of the paper, hardness tests, temperature readings, and studies of metal structure at different distances away from weld juncture are given later on in this paper. This part of the paper is a summary of the more complete data given later.

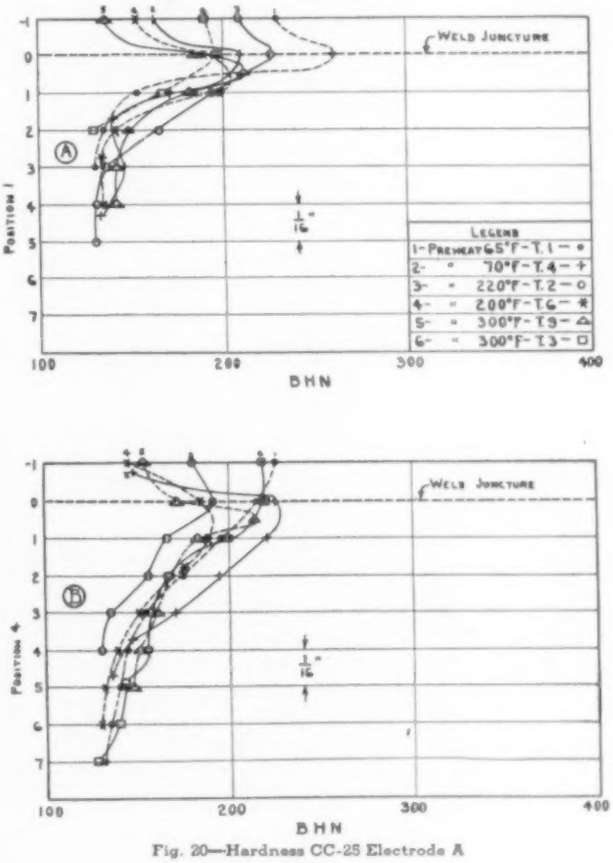


Fig. 20—Hardness CC-25 Electrode A

RECAPITULATION

The results of the preheating experiments, under the conditions stated, may be tentatively summarized as follows—remembering that the summary does not take into account the question of stresses and that it is confined to suggesting means for avoiding suspicious structures at the first part of the bead structure.

Group	Material	Preheat Minimum	Preheat Maximum	Granulation Tendency
1	WCM-15	Room	300° F.	Marked
2	CC-25	200° F.	400° F.	Marked
2	CNCM-25	300° F.	600° F.	Negligible
2	CCM-25	200° F.	400° F.	Appreciable
2	WCM-25	200° F.	400° F.	Probably marked?
3	CCM-35	300° F.	400° F.	Appreciable
3	SAE 1040	300° F.	350° F.	Very bad

Like any generality the summary above should be used with caution and judgment.

At a given welding temperature the rate of cooling and consequent severity of quenching action will follow rather closely the mass or section size of the parts being welded. At the moment of deposit of the first bead, heat will be abstracted more rapidly from the heavier or larger section or sections. These tests were run on $2^1/_2$ inch diam. bars several feet long.

The "no preheat" or room temperatures ranged between 70 and 80° F. If temperature of structures to be welded were substantially lower—say 30° F.—quenching action would be relatively more severe.

It readily is seen that the carbon content of the materials is the most important variable when tendency toward martensitic formation is the criterion. On this basis material under 0.20 per cent carbon could be welded "cold," around 0.25 per cent carbon localized preheat may be advisable, and over 0.35 per cent carbon, general preheating is suggested.

Consideration of these limited tests suggests that the wrought materials may be more susceptible to grain growth than the cast, that the addition of molybdenum somewhat lessens the tendency, and the addition of nickel, chromium and molybdenum (as in CNCM-25) is indeed beneficial in preventing grain coarsening.

In actual field work laying a single bead is rarely practiced. With multiple bead welding the second and succeeding beads "draw" the first bead and materials near the juncture. With this in mind, the writers believe that materials of Group 2 (around 0.25 per cent carbon) ordinarily could be safely welded without any preheating.

So far we have emphasized the effects of preheating in so far as these are related to undesirable structural formation in the first part of the first bead. It has been shown that preheating is advantageous in prevention of martensitic formation, but that in some cases preheating promotes grain formation.

That preheating minimizes thermal gradient and hence is beneficial in minimizing internal stresses, is recognized. These tests do not evaluate that trend,

Fig. 21—CC-25 at Junction Pos. 4. No Preheat 100 X

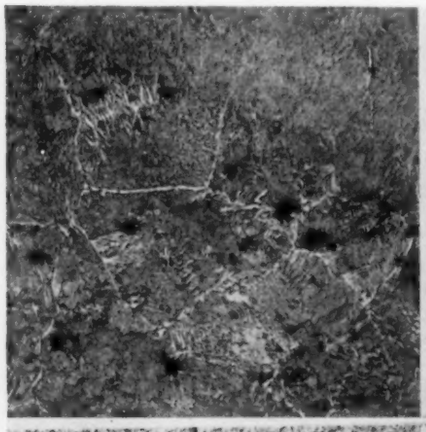
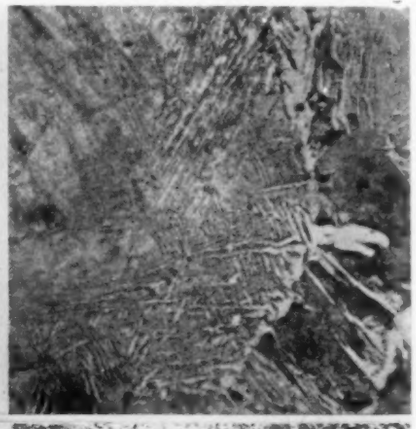


Fig. 22—CC-25 at Junction Pos. 1. No Preheat 100 X

Fig. 23—CC-25 at Junction Pos. 1. No Preheat 500 X



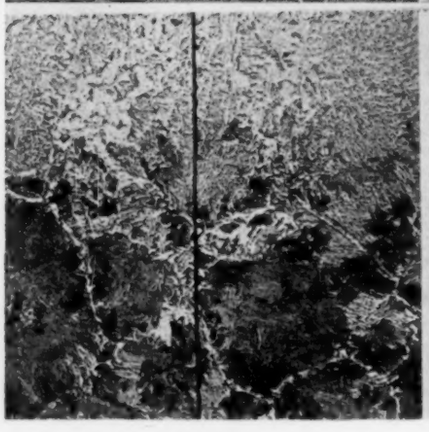


Fig. 24—CC-25 at Junction Pos. 1. 300° F. Preheat 100 X

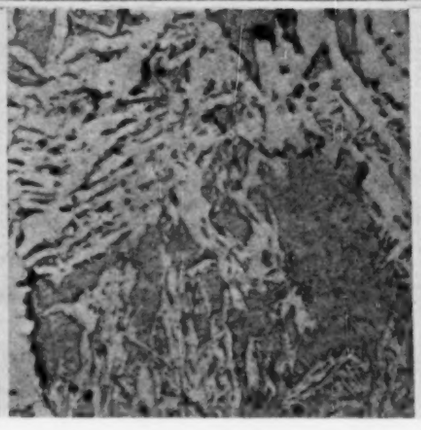


Fig. 25—CC-25 at Junction Pos. 1. 300° F. Preheat 500 X

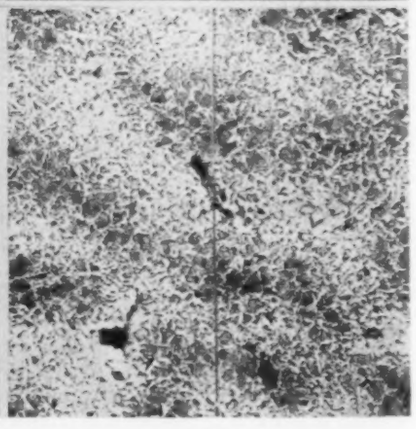


Fig. 26—CC-25 0.06 Inch from Junction 300° F. Preheat 100 X

except that it was observed that in none of the three classes were cracks observed. Preheating increases the depth of the hardened or structurally affected zone, however it minimizes the hardness gradient.

It may be mentioned here that preheating promotes "burning in" of the weld, which frequently is disadvantageous.

EXPERIMENTAL DATA

During welding the rod material being deposited is highly superheated—possibly to 3300° F., certainly above 3000° F. Where the weld metal and base metal meet (the "juncture") the base metal is molten. Some of it may dissolve in the weld metal, and "burning in" will result. Just below the zone of actual liquefaction there is a pasty zone. From the edge of the weld to the bottom of the pasty zone we may call a "puddled zone." Not infrequently "dirt" or inclusions are formed in this zone. From the "puddled zone" temperatures drop rather rapidly, but there is a zone of appreciable width—we may call this the "affected zone" where the metal is raised into the austenitic temperature field. Sufficiently rapid cooling from within this zone would produce martensite. However, with the range of compositions studied, cooling from the lower part of this range usually results in grain refinement and a sorbitic or even pearlitic structure. Somewhat below this zone there is little or no structural change, and the original base metal structure obtains.

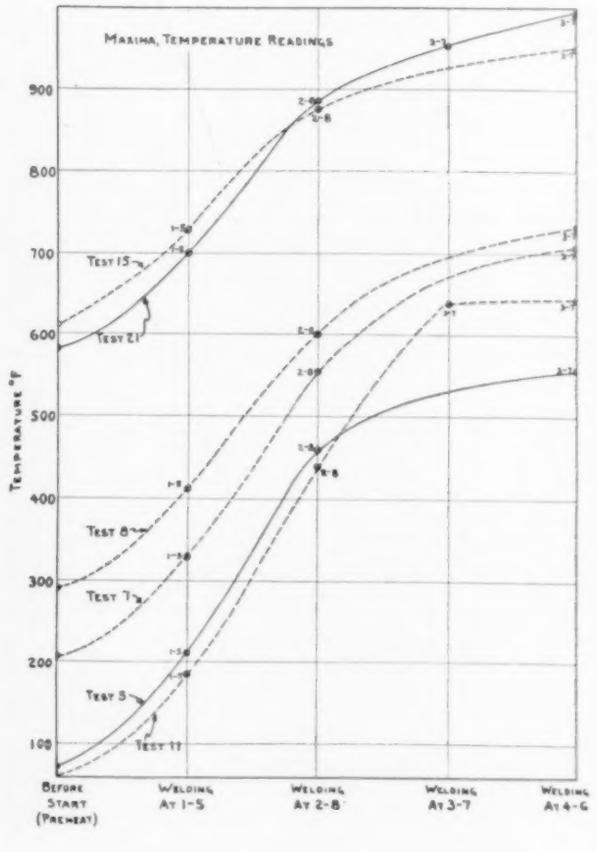


Fig. 27

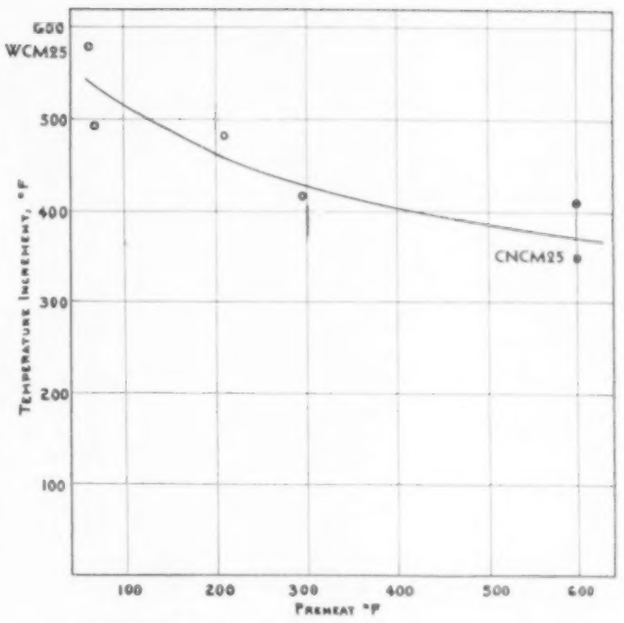


Fig. 28—CNCM-25 Welded with Electrode B to Stainless Steels Except for Points Noted

SECTION 1

WCM-15 (Wrought Carbon Molybdenum) and B Electrode (Mineral Coated)

Preheat, ° F.	Welded to	Test No.
a 70	CCM-25	10
b 200	CCM-25	13
c 600	CCM-25	14
d 600	CNCM-25	15

Temperature Maxima Recorded During Welding

These are shown in Fig. 7. Note that the increments of maximum readings for different preheats are

Preheat, ° F.	Test	Increment, ° F.
70	10	550
200	13	540
600	14	410 } average 380
600	15	350 }

Hardness of Weld and Base Metal

Hardness values are shown in Fig. 8, A for Position 1, B for Position 4. Maximum hardness of 225 Bhn. minimizes fear of a possible martensitic layer, quite unlikely in this steel of low carbon content unless very drastically quenched. Note the greater depth of hardening in Position 4.

Microstructure

A good idea of the influence of position on depth of affected zone is given by macrographs of the not preheated samples of Figs. 9 and 10, respectively Positions 1 and 4. How this is increased by preheat is nicely illustrated by Fig. 11, showing the zone at Position 1 at 600° F. preheat. (Compare with Fig. 9.) The juncture of weld metal and WCN-15, Position 1, no preheat, 100 diam. is shown in Fig. 12. The union is almost indistinguishable. The structure of the WCM-15 at 500 diam. in this location is shown in Fig. 13. The formation

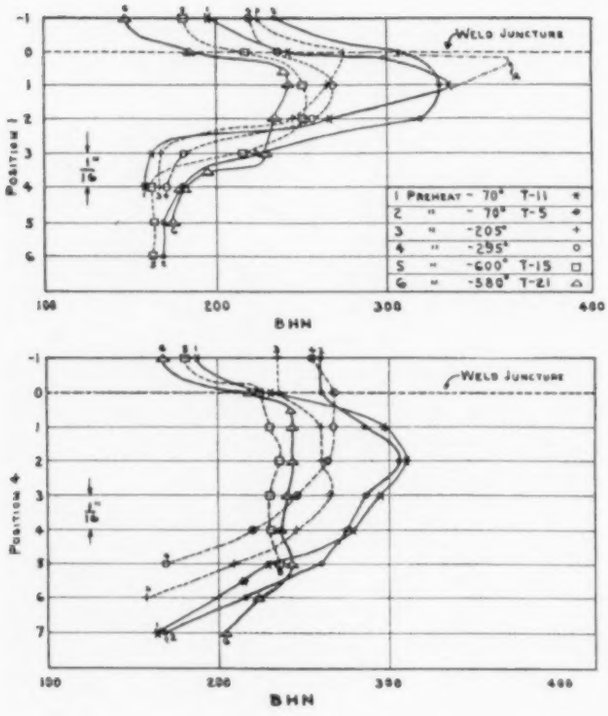


Fig. 29—Hardness CNCM-25 Welded with Electrode B

suggests that transient pre-existent martensite has completely decomposed, but grain coarsening has hardly started yet. Figure 14, 0.08 inch from the junction (100 diam.) shows a zone of intense grain refinement, Fig. 15 at 0.16 inch the transition zone, and Fig. 16 the normal metal structure fully regained at 0.24 inch from the junction. (Note ferrite banding.)

In so far as structural changes are concerned, welding WCM-15 with no preheat should cause no trouble, and as compared to structures of preheated samples, little or no undesirable grain coarsening occurs.

As is evident in Fig. 17 at 500 diam., even at 200° F. preheat there is some coarsening and slightly greater tendency toward this near the juncture. (Compare with Fig. 13.)

Grain size (shown by ferrite grain boundaries) is quite large at 600° F. preheat—test 14, as shown by Fig. 18 at 100 diam., at weld juncture. (Compare with Fig. 12.) Grains are often over 1 inch across, at 100 diam., as is shown in sketch, Fig. 19.

Comment

WCM-15 could be welded satisfactorily without preheat, if microstructure were the only consideration. Preheating much over 200° F.—say as high as 600° F. is definitely detrimental and should be avoided, even on a one bead weld. High preheat causes very coarse grain size—the "horrible example" being Fig. 18. Such grain structures, not best for ductility and impact resistance, are particularly to be avoided when vibratory stresses are likely to be encountered. Such "grain growth" suggests a "live" or incompletely killed, hence unstable, metal.

Quite similar results, except that weld and puddled zones were "dirtier" were found when electrode C (cellulose coated) was used.

SECTION 2. CAST CC-25 CARBON STEEL AND ELECTRODE A (CELLULOSE COATED)

Preheat, ° F.	Welded to	Test No.
a 65	Stainless Steel	1
a 70	SAE 1040	4
b 220	Stainless Steel	2
b 200	SAE 1040	6
c 300	SAE 1040	9
d 300	Stainless Steel	3

Temperatures Recorded During Welding

Due to shorting of several couples it was thought advisable to omit record of these tests.

Hardness

With one exception hardness maxima ran between about 190 Bhn. and 225 Bhn. Greater depth of hardening at Position 4 is noted on Fig. 20B. A single maximum of 260 Bhn. on the weld junction was noted at Position 1, Test 1, no preheat (see Fig. 20A). Prospecting around that point showed no greater hardness at any other location.

(Note: Hardness diagrams are laid out so that dotted line (0) represents weld juncture. Line (−1) represents location in weld $^1/_{16}$ inch above weld juncture. Lines 1, 2, 3, etc. are respectively $^1/_{16}$ inch, $^1/_8$ inch, $^3/_{16}$ inch, etc. below weld juncture, in base metal. Readings made directly with Rockwell, converted to Brinell numbers (Bhn.) as per table p. 127 Metals Handbook, 1939 edition.)

Microstructure

The juncture of A electrode (cellulose coated) and

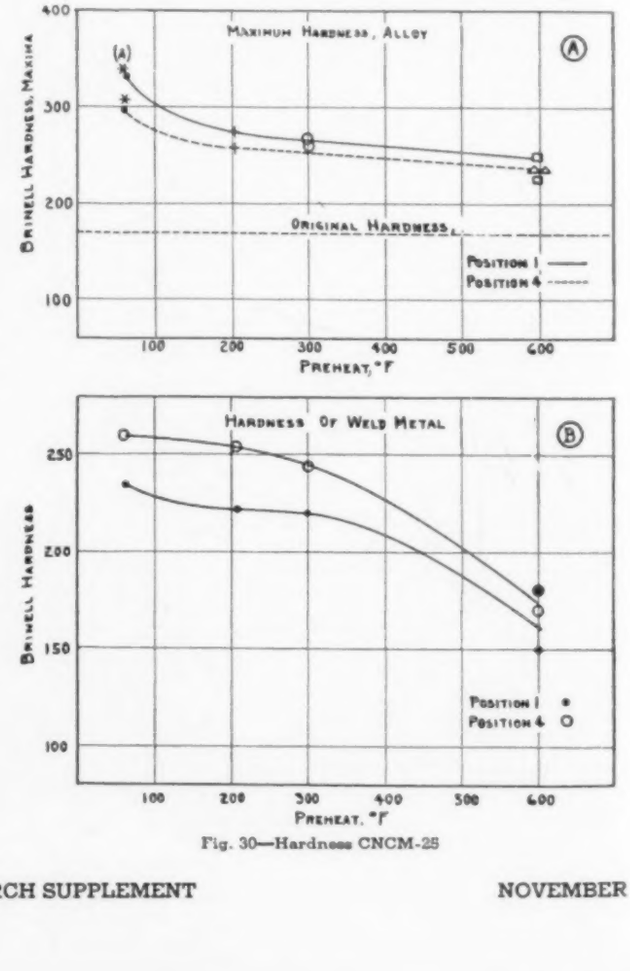


Fig. 30—Hardness CNCM-25

Fig. 31—CNCM-25 at Junction Pos. 1. No Preheat 100 X

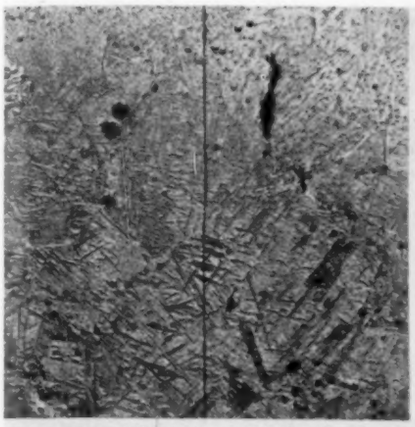


Fig. 32—CNCM-25 at Junction Pos. 1. No Preheat 500 X

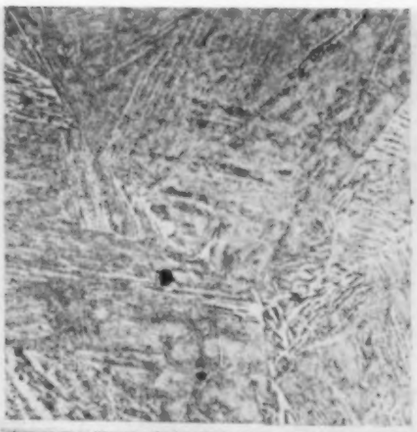
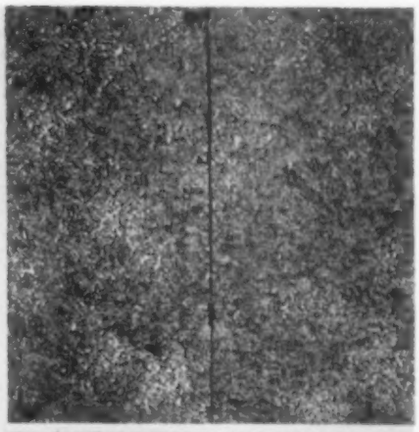


Fig. 33—CNCM-25 0.08 Inch from Junction 100 X. No Preheat



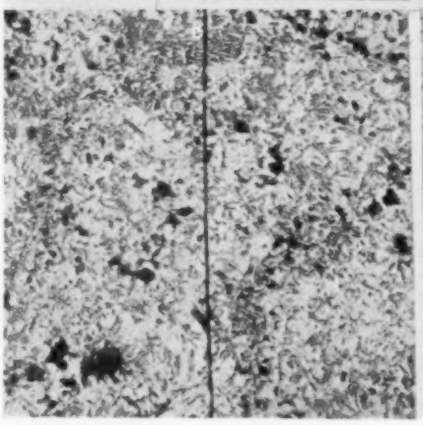


Fig. 34—CNCM-25 0.16 Inch from Junction 100 X. No Preheat

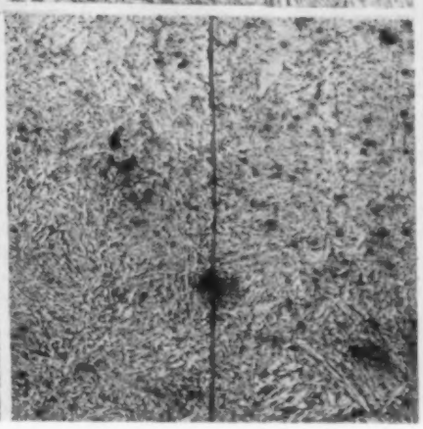


Fig. 35—CNCM-25 Pos. 1. 600° F. Preheat 100 X

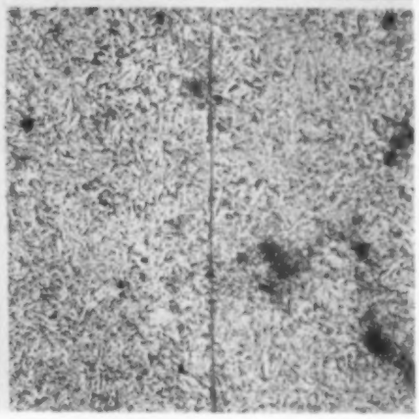


Fig. 36—CNCM-25 Pos. 1. 0.08 Inch from Junction 100 X

CC-25 cast carbon steel, Test 1 (no preheat) Position 1 is shown at 100 diam., Fig. 21. (In this and other photomicrographs the weld metal, or the position closest to the weld, is shown at the top.) The same position is Test 4 (also no preheat) shown in Fig. 22.

The junctures are quite good. Some medium sized inclusions are seen in the base metal of Test 1, and a greater number of finer inclusions in the base metal of Test 4. This metal was molten or pasty during welding as may be seen from the coarse grained structure and beginning of intragranular ferrite divorcement. This zone we may call the "puddled zone"—and the inclusions quite likely are a result of the welding operation rather than inherent in base metal.

The grain size is coarse, in general about No. 2 A. S. T. M., some grains approaching No. 1. Such coarse grain is not desirable, since it is suggestive of low resistance to impact and vibratory stresses. In Test 4 (Fig. 22) many inclusions are to be seen in the grain boundaries, which adds to the undesirability of the large grained structure.

Position 1 in the base metal, is shown at 500 diam., in Fig. 23. Having been raised to, or near, the molten condition and cooled rather rapidly, a residual acicular appearance is retained.

At 0.08 inch from the juncture there is a zone of grain refinement, and at 0.15 inch from the juncture, at Position 1, the normal cast structure is practically regained.

There was little essential difference in the structures developed at 200° F. preheat compared to no preheat, although macrostructure at Position 1 showed substantially greater depth of altered zone, comparing 200° F. against no preheat.

The juncture, at 300° F. preheat, Position 1, 100 diam. magnification is shown in Fig. 24. Coarsening is quite apparent, approaching No. 1 A. S. T. M. grain size. The original austenitic grains show preliminary signs of breakdown to a finer aggregate. This too, at 500 diam., is well shown by Fig. 25.

Figure 26 (100 diam.) shows a zone of intense grain refinement, the ferrite grain approaching 7 or 8 A. S. T. M. 0.08 inch away from the juncture. Note that vestiges of original dendritism remain, as indicated by zones of carbon concentration. At 0.16 inch away from the juncture the original cast structure is regained.

Comment

Cast carbon steel of 0.24 per cent carbon is rather generally considered a relatively "safe" material to weld without preheat. In so far as "hard" structural formations are concerned these tests confirm that opinion. Anticipating results of later tests, we may say, that if produced without actual martensitic formations, the cast alloy steels (CCM-25 and especially CNCM-25) give finer grained "tougher" structures than are found with this cast carbon steel and with the alloy and plain carbon wrought steels covered hereinafter. A coarse grain size, is not desirable.

Cast steel practically always has some residual dendritic or cast structure. While this is minimized by controlled casting and heat treating practices some is yet evident. Cast steels from other sources that we have studied or seen structural data on, frequently have dendritism so pronounced that it affects physical properties adversely.

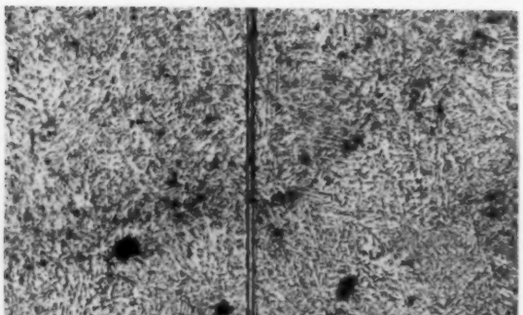


Fig. 37—CNCM-25 Junction Pos. 4. 600° F. Preheat 100 X

SECTION 3. ALLOY CNCM-25 AND MINERAL COATED ELECTRODE B

Preheat, ° F.	Welded to	Test No.
a 60	WCM-25	11
a 70	Stainless Steel	5
b 205	Stainless Steel	7
c 295	Stainless Steel	8
d 600	WCM-15	15
d 580	CNCM-25	21

Temperatures at Locations 1–5, 2–8, 3–7 and 4–6

Maxima are shown in Fig. 27. At location 1–5, 2–8 and 3–7 maxima corresponded with welding position. When welding at position 4–6 temperature maxima were at position 3–7, because 3–7 already were heated up, and beyond 4–6 there was no weld laid. The readings position when welding at 4–6 are

	Pre-heat	60° F.	70° F.	205° F.	295° F.	600° F.	580° F.
Temperature at	4–6	630	550	600	650	925	950
Temperature at	3–7	640	560	690	730	950	995

The small figures (as 1–5, 3–7 etc.) near plotted points on Fig. 27 are the positions at which the maxima appeared when welding at stated position.

Figure 28 gives the increments of maxima above the preheating temperatures. As might be expected the increment decreases as the preheat increases. However, this is not in direct ratio. About 550 degrees is the increment at preheat of 60–70° F., about 350 degrees the increment at preheat of 600° F. Thus an increment of over 500 degrees in preheat decreases the gradient at positions shown only some 200 degrees, hence, at that point lowers the thermal shock but slightly. (It always must be remembered that the thermocouples are $^1/_8$ inch away from the edge of the weld. Also there is considerable thermocouple lag when temperatures are transient, and microscopy gives more direct evidence.)

Hardness Readings

As determined by salt brine quenching experiments, fully hardened martensitic Alloy CNCM-25 is 440 Bhn.

Results of the hardness examination of the weldments are shown in Fig. 29 at Position 1 (beginning of weld) and 4 (end of weld). Location of weld juncture is indicated by dotted line, labelled 0. The hardness of weld metal $^1/_{16}$ inches above the junction is shown along the line −1 above the juncture. Lines numbered 1, 2, 3 etc. are respectively $^1/_{16}$, $^1/_8$, $^3/_{16}$ etc. inches below the juncture. The six curves correspond to the various preheats, as indicated by signs,* etc. It is readily seen that the highest hardness in each case occurs with no preheat. The higher results are in Position 1, whereas at Position 4 the "automatic" preheat by the preceding weld layer prevented attainment of a high hardness.

Microscopic examination indicated that a thin, partially martensitic layer was formed less than 0.005 inch below the juncture at Position 1 and this was hardly 0.010 inch wide in case of no preheat. By means of careful prospecting just below the weld juncture on a sample etched for microscopic examination we were able to get impressions right on this layer—reading 370 Bhn. Inasmuch as the penetrator indentation is nearly 0.02 inch wide the readings probably are on the low side. (Readings did not vary much when the penetrator hit the middle of the martensitic layer.) The existence of this layer is suggested by the dotted line extending to *A* on Fig. 29. (At position 4 the microscope and hardness readings both indicated that the maximum hardness was attained further away from the juncture, and prospecting showed that the chart Fig. 29, Position 4 is correct.)

The relationship of maximum hardness to preheat is shown in Fig. 30A. Maximum reading (as referred to in preceding paragraph) is indicated as (A). At "no preheat" the differences over the preheated samples while not great in Bhn. are significant as we are in a rather critical hardness range. With preheats of 200° F. or

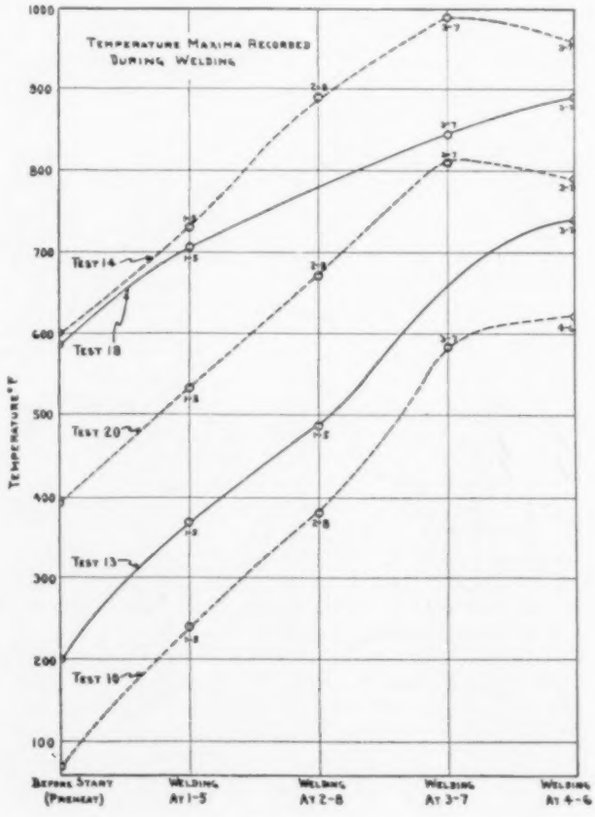


Fig. 38

higher the differences are not great, and we believe that say above 300° F. the hardnesses themselves are in a range where either structural or mechanical troubles need not be worried about. Higher hardness in this range means a considerable increase in strength and a minor loss of ductility.

One need but recall that hardening is a function of a critical cooling speed to understand the marked change in this base between no preheat and a preheat of only 200° F., and of 300° F.

Microstructure

The microstructure at the Position 1 juncture of CNCM-25 and electrode B (mineral coated), Test 11—no preheat, at 100 diam. is shown in Fig. 31. As shown the CNCM-25 has a martensitic or near martensitic structure beginning just below the juncture and extending about 0.010 inch. The general nature of this structure at 500 diam. is shown in Fig. 32, taken on the side nearest the junction.

Figure 33 shows the structure 0.08 inch away from the juncture. Note the complete absence of martensitic structure. This point was for a short time at a temperature probably somewhat above 1400° F. Proceeding to Fig. 34, 0.16 inch away from the juncture we approach the typical cast structure. This is a good check with the hardness readings. Fig. 29 ($^1/_{16}$ inch = about 0.06 inch). A higher hardness at location *A*, Fig. 29 corresponds with Fig. 31. At Fig. 33 (0.08 inch) the hardness is about 330 Bhn., and a reasonably ductile and quite high strength structure is evident—hardness curve* decreasing rapidly in this region as evident from lower half of Fig. 33. Less than 0.16 inch below the juncture the normal cast structure is regained and this is evident from the about 180 Bhn. shown in the chart.

Fig. 39—CCM-25 Pos. 1. 600° F. Preheat 3.5 X

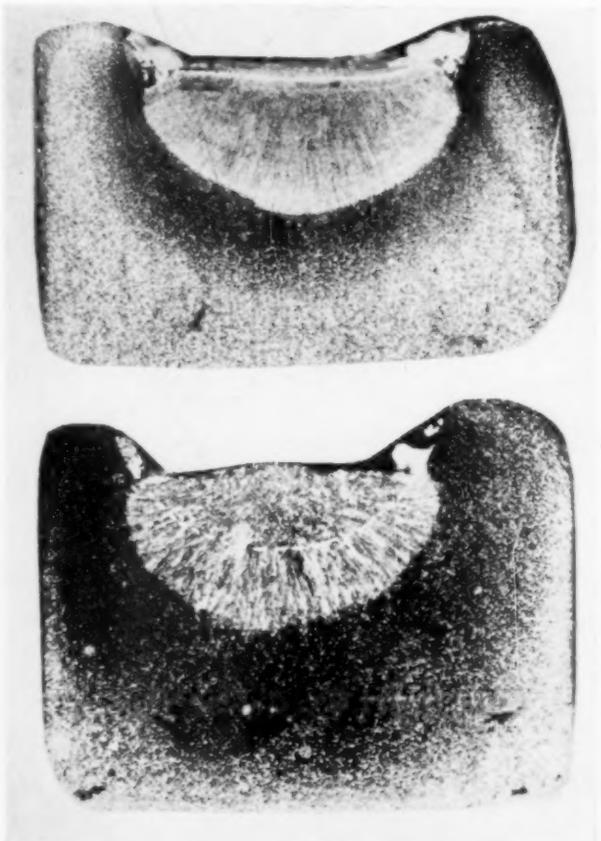


Fig. 40—CCM-25 Pos. 4. 600° F. Preheat 3.5 X

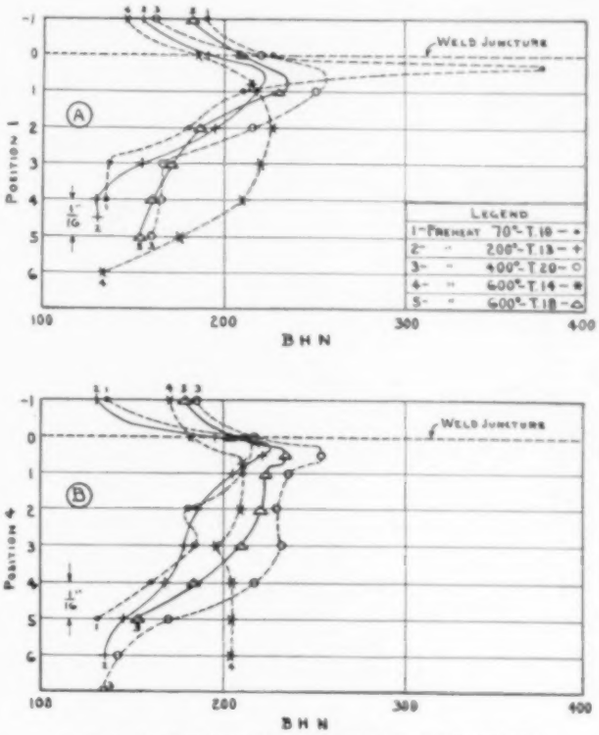


Fig. 41—Hardness CCM-25 Welded with Electrode B

The temperature gradient is from say 3000° F. (estimated) at this juncture to about 1400° F. at say about 0.10 inch away from the juncture—or 16,000° F. per inch within that region. It is quite apparent that the transient temperature plus thermocouple lag makes the thermocouple readings of comparative interest only.

Despite the great temperature gradient, and thin martensitic or near martensitic layer there was no evidence of cracking in any layer at any location. The bond or union is good. There is a layer, perhaps 0.015 inch below the junction, which evidently was above the melting point, and which we may call the "puddled zone." There is a small amount of "dirt" in that zone.

No martensitic layer was seen in Position 4—the "automatic preheating" due to the laying of the weld obviated that. As in all cases so far observed the depth of hardening at Position 4 was deeper than at Position 1, but the maximum hardness not so high. Alloy content in this case induces deeper hardening without however increasing the hardness.

At 200° F. preheat a troostitic condition was found just below the weld juncture, Position 1. At 300° F. preheat the troostitic condition did not obtain. The hardness at no location exceeded 270 Bhn.

At the 600° F. preheat we find (*a*) a granular pearlite formed (*b*) the depth of hardening demonstrated by etch is greater than the lower preheats (*c*) the "puddled zone" is deeper and inclusions are larger and less numerous and (*d*) there is a zone of slightly coarsened grain.

The fusion zone at 600° F. preheat is shown at 100 diam., Fig. 35, and demarkation can hardly be seen.

Figure 36 shows at 100 diam., the structure 0.08 inch below the weld juncture in Position 1. Note the nature of the inclusions, which suggest this might have been in a pasty, nearly molten state The structure is largely ferrite-pearlite.

Normal type cast structure is not found until about 0.24 inch from the juncture.

At Position 4 the weld juncture, 600° F. preheat, Fig. 37, is most excellent. The CNCM-25 structure just below the juncture is granular pearlite of excellent uniformity. This is a type normally of good ductility and strength. A fully normal cast structure at Position 4 is not fully regained at 0.24 inch below the juncture.

A complete evaluation of the depth of structural change as related to position and preheat awaits more exhaustive photomicrograph mapping in all sections. However, the data available in the major report show clearly that at Position 1, not preheated, structural change extends about 0.12 inch below the juncture and to 0.20 or 0.24 inch in case of 600° F. preheat. At Position 4, marked structural change is quite evident for the 600° F. preheat up to about 0.50 inch below the juncture.

Comment

(1) Alloy CNCM-25 makes an excellent "bond" with mineral coated electrode B.

(2) Alloy CNCM-25, under drastic welding conditions may form some martensitic structure. (No preheat and first bead.)

(3) A preheat of 300° F. is recommended. In all probability, elaborate provisions are not necessary. From a structure view-point carefully warming up with a torch should prove satisfactory—perhaps a solder of about 350° F. melting point could be employed to check temperature.

(4) In case of Alloy CNCM-25 a high preheat, say 600° F. does not cause marked grain coarsening.

(5) Intensity of hardening decreases and depth of affected zone increases with increasing preheat.

(6) A stress relief treatment is desirable always. Initial temperature gradients are at best high, and the expansion-contraction effect of "quench" is signified by structural alteration. Other studies suggest that 1100° F. should not be exceeded for purpose of stress relief and avoidance of any possibility of spheroidization insured.

(7) The temperature readings at the thermocouples near the weld are but relative. Microscopy affords by far the most conclusive and accurate "weapon" in ascertaining what really happens during the welding operation. Hardness tests are worth while, but should be checked by microscopy.

SECTION 3-A. CAST ALLOY CNCM-25 AND ELECTRODE C (CELLULOSE COATED)

(*a*) 600° F. Preheat—Test 16.

Test at 600° F. preheat was also run using cellulose coated electrode C.

The general characteristics and structure of the weld were about the same as with mineral coated electrode, although the number of inclusions was appreciably greater.

SECTION 4. CAST 0.25 CARBON MOLYBDENUM AND MINERAL COATED ELECTRODE B CCM = 25

Preheat, ° F.	Welded to	Test No.
a 70	WCM-15	10
b 200	WCM-15	13
c 400	CCM-25	20
d 600	WCM-15	14
d 600	CCM-25	18

Fig. 42—CCM-25 Junction Pos. 1. 200° F. Preheat 100 X

Fig. 43—CCM-25 Junction Pos. 1. 200° F. Preheat 500 X

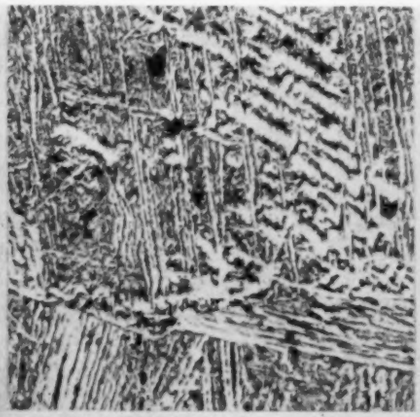
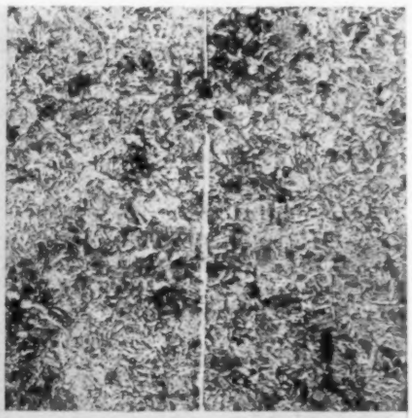


Fig. 44—CCM-25 0.08 Inch from Junction 100 X. 200° F. Preheat



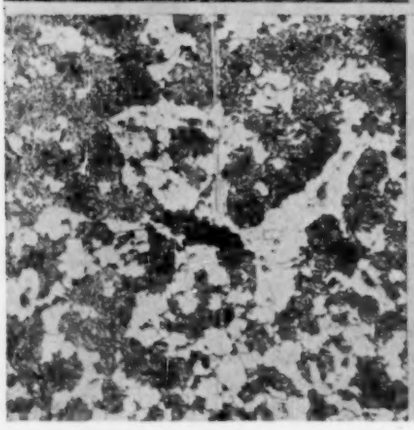


Fig. 45—CCM-25 0.16 Inch from Junction 200° F. Preheat 100 X

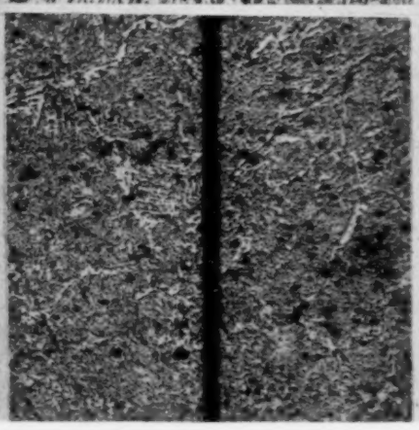


Fig. 46—Weld Deposit on CCM-25 400° F. Preheat 100 X

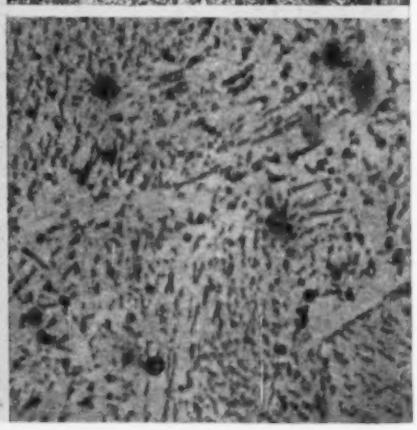


Fig. 47—Weld Deposit on CCM-25 400° F. Preheat 500 X

Fig. 48—CCM-25. 500 X 400° F. Preheat

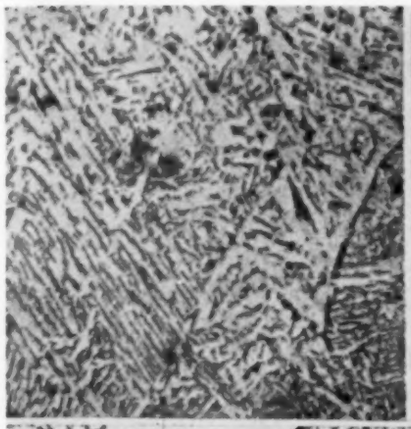


Fig. 49—CCM-25 0.08 Inch from Junction. 500 X

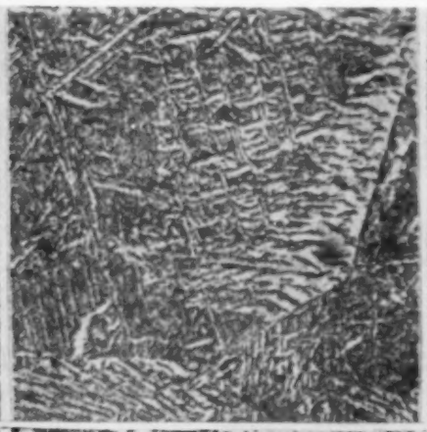
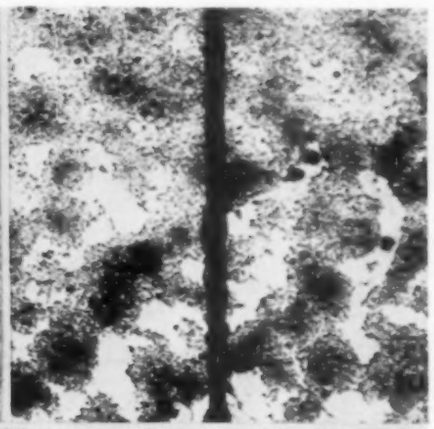


Fig. 50—CCM-25 0.16 Inch from Junction. 100 X



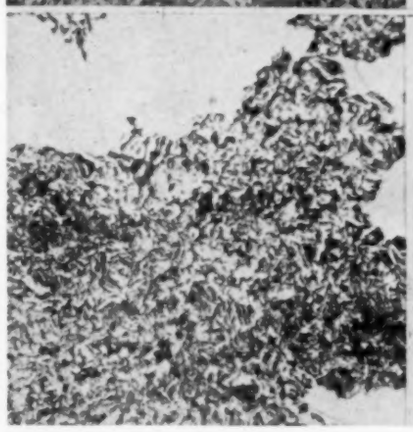


Fig. 51—CCM-25 0.16 Inch from Junction 500 X

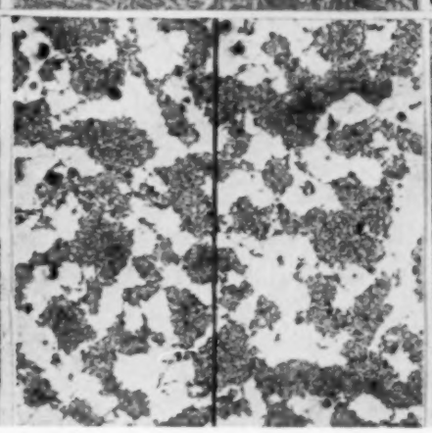


Fig. 52—CCM-25 0.32 Inch from Junction. 100 X

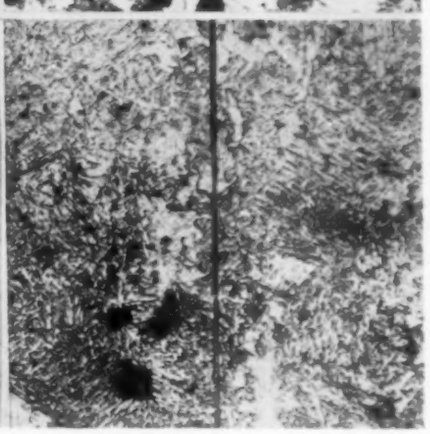


Fig. 53—CCM-25 Junction Pos. 1. 600° F. Preheat 100 X

Temperature Maxima Recorded During Welding

These are shown in Fig. 38. Temperature readings on this series do not show the consistency evident in some other series. The apparent explanation (as is shown by macrographs, Figs. 39 and 40) is that the bead on Test 18 ran quite shallow and burned quite deeply, especially as Position 4 was reached. Test 14 undoubtedly is nearer a proper reading.

Hardness of Weld and Base Metals

Hardness values are shown in Fig. 41, *A* for Position 1, *B* for Position 4. As may be seen from the diagrams the results are quite erratic. No fully satisfactory explanation was to be found for the high hardness in Position 1 of Test 10. A martensitic structure would be expected but none was clearly shown microscopically. With preheats of 200° F. or higher, hardnesses of 200 and 250 Bhn. are found near the juncture which are not high, suggesting a pearlitic structure. Note that hardness values found in Test 13, 200° F. preheat are lower than in any of the others.

Microscopic Examination

Test 10, no preheat was an unsound specimen, a number of very fine pinholes being present. The single high hardness reading indicated a martensitic layer but no such structure was clearly evident.

Test 13, 200° F. preheat on macroetch showed somewhat unsound base metal at Position 1, but sound metal at Position 4. Examination at the juncture, Fig. 42, Position 1 at 100 diam. while it shows a fair joint yet it shows too much difference in structural forms and possibly in carbon concentration to be desirable. As indicated by Fig. 42 and also Fig. 43, at 500 diam. the region was molten and a semi-acicular arrangement of coarse grain size is found. Figure 44 at 100 diam. is 0.08 inch away from the juncture, and in a zone of grain refinement. Figure 45, 100 diam. 0.16 inch from juncture shows, in the lower part of the structure an approach to the original cast structure. The temperature was above the critical, but not high enough for diffusion.

On Test 20, 400° F. preheat, Position 1, macroetch showed sound metal and the joint was good. Weld metal, Electrode B (mineral coated) at 100 diam. 0.04 inch from juncture is shown in Fig. 46. (The black line is a needle scratch, used in many cases for reference mark.) The same location, but at 500 diam. is shown in Fig. 47. A little grain coarsening is evident. Inclusions are relatively few and well distributed when one considers this is a single bead. The presence of nitride as well as carbide (carbon only 0.08 per cent) is suspected because of the comparative dark etching. However, this has not been proven.

At the CCM-25 and weld junction, 400° F. preheat, some grain coarsening of the CCM-25 is evident. The CCM-25 at 500 diam. is shown in Fig. 48. This structure, at 500 diam. and 0.016 inch. from the juncture, is shown retained as in Fig. 49. Note the large grain and ferrite cleavage plates along the boundaries of essen-

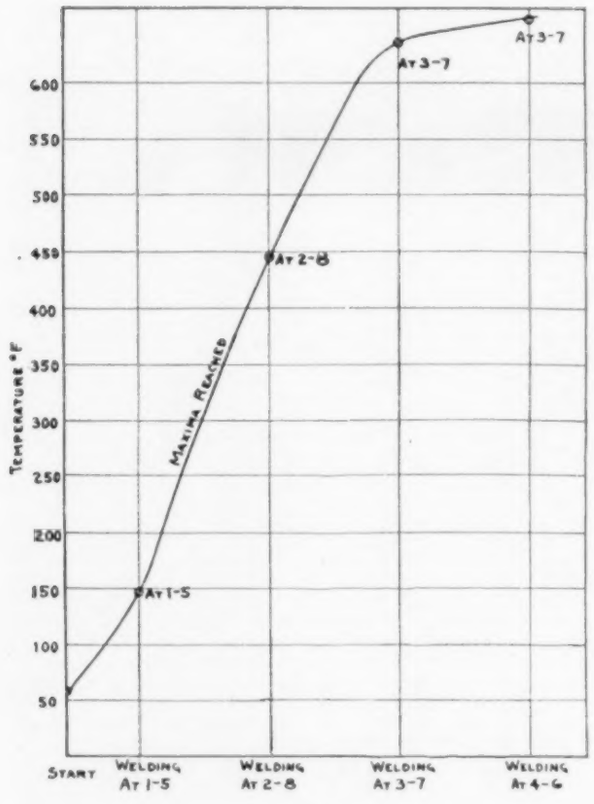


Fig. 54

tially pearlitic grain formation. As is evident from the uniformity of carbide distribution, this region was well above the transformation range. As the weld juncture line is departed from, a gradual transition from coarse grain and further ferrite-carbide divorcement (from transient pre-existent martensite) occurs. In Fig. 50, 100 diam. we see definite transition some 0.16 inch away from the juncture. Figure 51, 500 diam., location lower section of Fig. 50, shows development of more normal type pearlite, finely laminated, as islands in ferrite.

As a matter of interest the transition zone (which is wide) at Position 4 is not found until about 0.28 inch from the juncture. A portion of the zone is shown in Fig. 52 at 100 diam.

Test 14, Position 1, 600° F. preheat indicated sound metal on macroetch. At the juncture, Fig. 53, 100 diam., shows small pinholes and in the CCM-25 an acicular orientation in fairly large grains. The structure at the juncture, at 500 diam. showed that no trace of martensite is present. Intense grain refinement still prevails at 0.24 inch from the juncture and an approach to normal structure is beginning at 0.32 inch.

Macrographs at Positions 1 and 4 of test 18 (600° F. preheat are shown in Figs. 39 and 40). Note the shallow bead at Position 4. Essential features of the microstructure are about the same as for Test 14, with the exception that the depth of heat penetration compares more nearly with the 400° F. preheat (Test 20) than with Test 14 at 600° F. preheat. This sensitivity of relationship between the maximum temperature chart and depth of structural formation change is rather interesting. The heat-max. temperature and total calories are factors as well as preheat temperature itself. It may be well to emphasize that multiple bead welding in commercial practice may indeed be far less severe than the single bead weld of these tests.

Comment

To form a precise, complete and accurate diagnosis of this series on the basis of samples submitted is not possible. Some of the samples submitted were unsound. Unfortunately this unsoundness was microscopic and it was not until deep etch magnified, i.e. ate out, the originally minor unsoundness that reasons for certain discrepancies appeared. On ordinary preparation for microscopy some of the defects were so tiny that they were obscured by polishing. Such unsoundness may not affect the physical properties much. It however "plays hob" with hardness readings by Rockwell, and makes microscopy itself more difficult. A study of the unsoundness indicates that pouring temperature was too low and feeding insufficient for this bar sample.

The series as a whole showed good weld joints—no cracking, or other unsoundness. Change in composition, i.e. carbon gradient, seemed a little more abrupt than might be ideal, but nothing to worry about.

Under the condition of test—single bead—preheat is desirable. Only 200° F. seems necessary to lower the cooling rate below the critical speed. At 400° F. and above some slight coarsening is apparent. This however is not as pronounced as in the cast carbon and in the wrought steels.

SECTION 5. WCM-25 FORGINGS AND MINERAL COATED ELECTRODE B

No preheat—Test 11.

Temperatures Attained During Welding

Maximum thermocouple readings when welding at the different locations are shown in Fig. 54.

Hardness Tests

These are shown diagrammatically in Fig. 55, at Positions 1 and 4. A maximum of 256 Bhn. was found at Position 1, about 0.01 to 0.03 inch from the juncture. Although high hardness is not developed at Position 4 the depth of hardening is deeper.

Microstructure

The macrographs, Positions 1 and 4 showed the differences in hardness depths and the greater depth of hardening and more gradual merging of structures being at Position 4. The joint appeared quite sound.

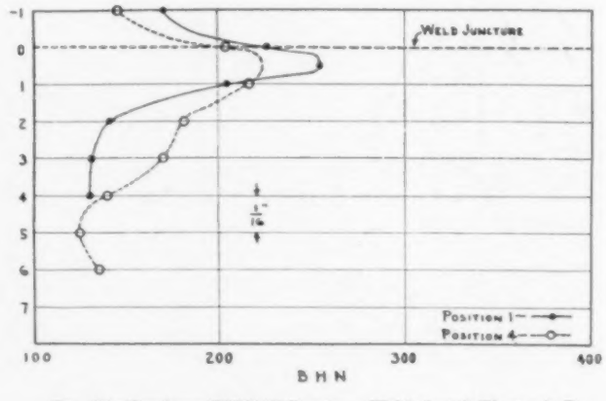


Fig. 55—Hardness WCM-25 Forging, Welded with Electrode B

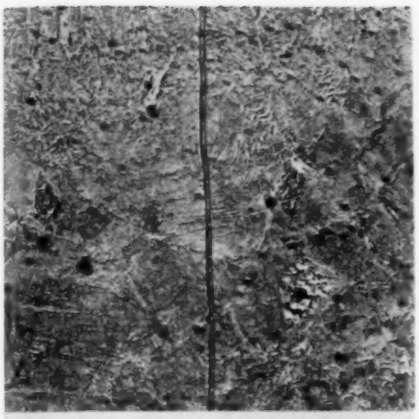


Fig. 56—WCM-25 Junction Pos. 1. No Preheat. 100 X

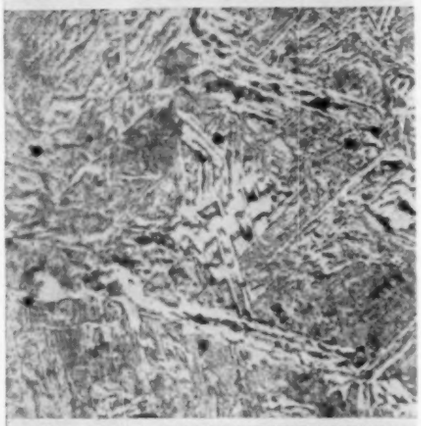


Fig. 57—WCM-25 Junction Pos. 1. No Preheat. 500 X

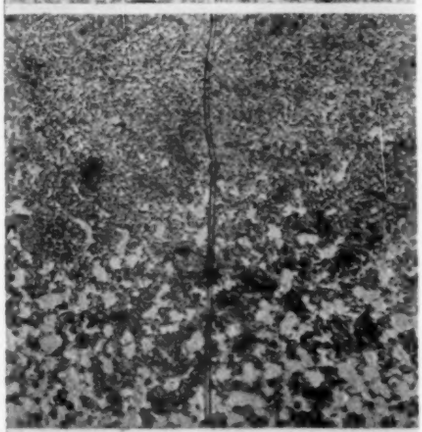


Fig. 58—WCM-25 0.08 Inch from Junction. No Preheat. 100 X

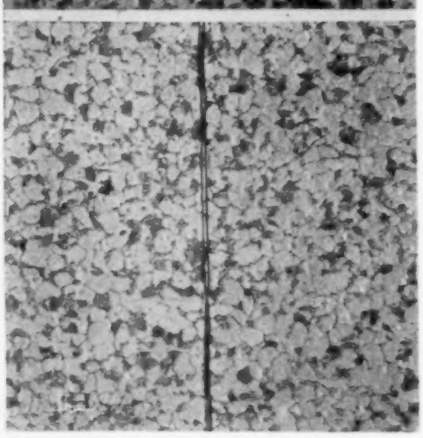


Fig. 59—Normal Structure of WCM-25 0.12 Inch from Fig. 56. 100 X

Figure 56 Position 1 shows the weld juncture at 100 diam. The WCM-25 structure at 500 diam. in this zone is shown in Fig. 57. The orientation of the needle like formation is suggestive of martensite. The rapidity of etching plus inability to obtain a hardness reading of over 256 Bhn. suggests that this structure is a decomposition product of martensite, not martensite.

The area (less than 0.01 inch wide) just below the weld metal should be viewed with some suspicion. While not truly martensitic, it possibly is not as ductile or resistant to impact as might be desired. Slightly more rapid cooling would have produced martensite. Large grain, about 0.02 inch below the weld, is not desirable either.

Proceeding 0.08 inch below the weld juncture the upper part of the photomicrograph Fig. 58 shows intense grain refinement and hardness of 200 Bhn. The structure is changing very rapidly, and as shown at the bottom of Fig. 58 it is approaching the normal structure again. As shown in Fig. 59 the normal ferrite-pearlite structure is regained at 0.12 inch from the juncture.

The inclusions in the puddled zone and in the weld are not numerous and are well distributed.

Comment

Preheat probably should be used in welding WCM-25. The preheat should not be high, as excessively large grain formation is likely with high preheat. 200–400° F. is suggested tentatively. The ferrite banding noted, while not desirable, is not considered seriously detrimental.

SECTION 6. CAST CCM-35 AND MINERAL COATED ELECTRODE B

No preheat—Test 12

Temperatures Attained During Welding

Maxima measured on the thermocouples are charted on Fig. 60. Because of shorting, data were not obtained from thermocouples 3 and 7 while welding at 3–7 the

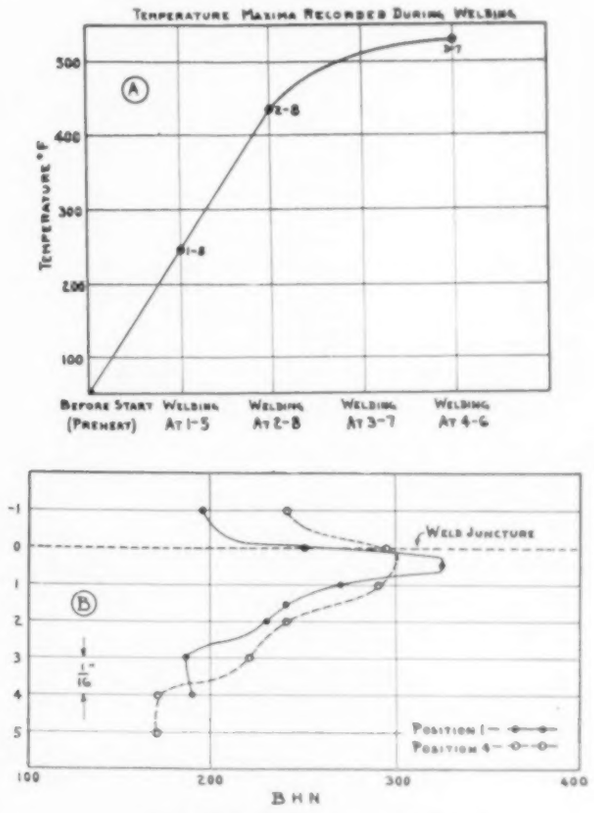


Fig. 60—CCM-25 Welded with Electrode B

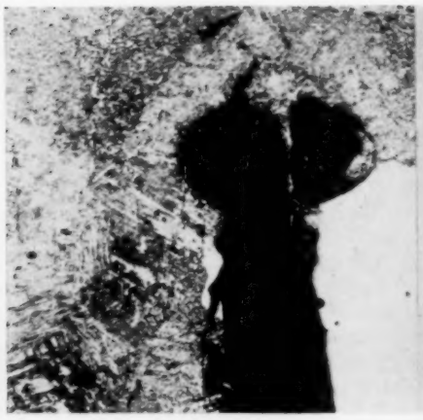


Fig. 61—CCM-35 at Junction. No Preheat. 100 X

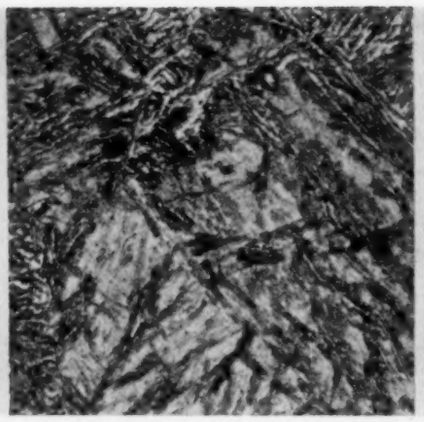


Fig. 62—CCM-35 at Junction. No Preheat. 500 X

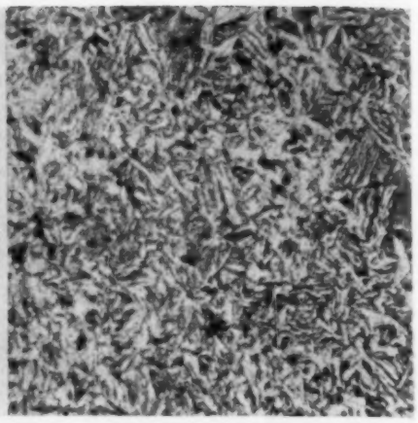


Fig. 63—CCM-35 0.08 Inch from Junction. No Preheat. 500 X

temperatures probably were higher than are indicated by the arbitrary curve drawn from 2–8 to 4–6.

Hardness

The hardness values found in the weld and across the weld-affected base-metal are plotted in Fig. 60. The maximum hardnesses were found practically in the same locations in Positions 1 and 4; a hardness of 320 Bhn. was attained in Position 1 and 295 Bhn. in Position 4.

Microstructure

The weld junction is shown in Fig. 61, at 100 diam. (CCM-35 is at lower left—weld metal above—stainless steel at lower right). There is evidence of an acicular structure. Figure 62 at 500 diam. was taken just below the weld juncture in the CCM-35 and shows a martensitic condition. 0.08 inch away from the juncture there is still a suggestion of some martensite, Fig. 63, 500 diam.

Comment

Fair bonding resulted when welding CCM-35 with mineral coated Electrode B but an undesirable martensitic structure was developed in welding without preheat. With the higher carbon of this steel a possibly higher preheat than is necessary for CCM-25 is recommended. As 200° F. seems a minimum for the CCM-25, 300° F. should be satisfactory for the CCM-35. A good bond with the weld metal, and a tough, reasonably fine grain zone adjacent to the weld should result.

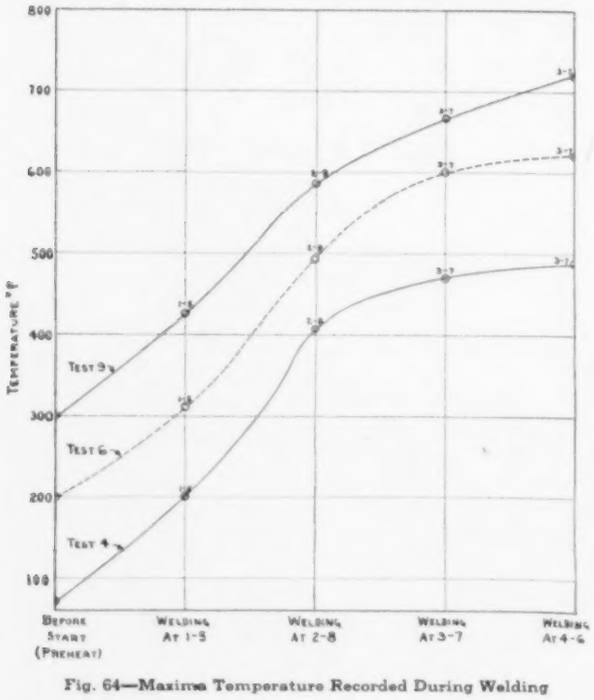


Fig. 64—Maxima Temperature Recorded During Welding

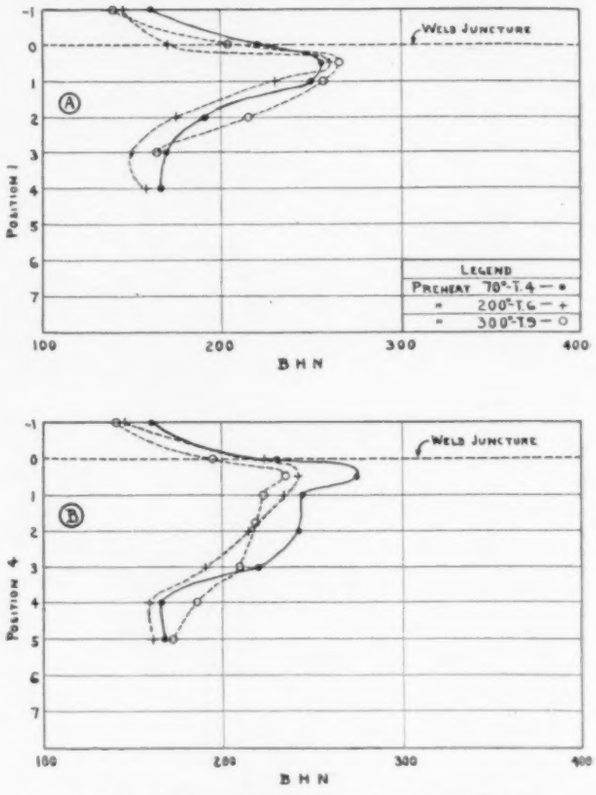


Fig. 65—Hardness SAE 1040 Welded with Electrode A

SECTION 7. SAE 1040 (WROUGHT STEEL) AND CELLULOSE COATED ELECTRODE A

Preheat, ° F.	Welded to	Test No.
A 70	CC-25	4
B 200	CC-25	6
C 300	CC-25	9

Fig. 66—SAE 1040 at Junction. No Preheat. 100 X

Fig. 67—SAE 1040 at Junction. No Preheat. 500 X

Fig. 68—SAE 1040 0.08 Inch from Junction. No Preheat. 100 X

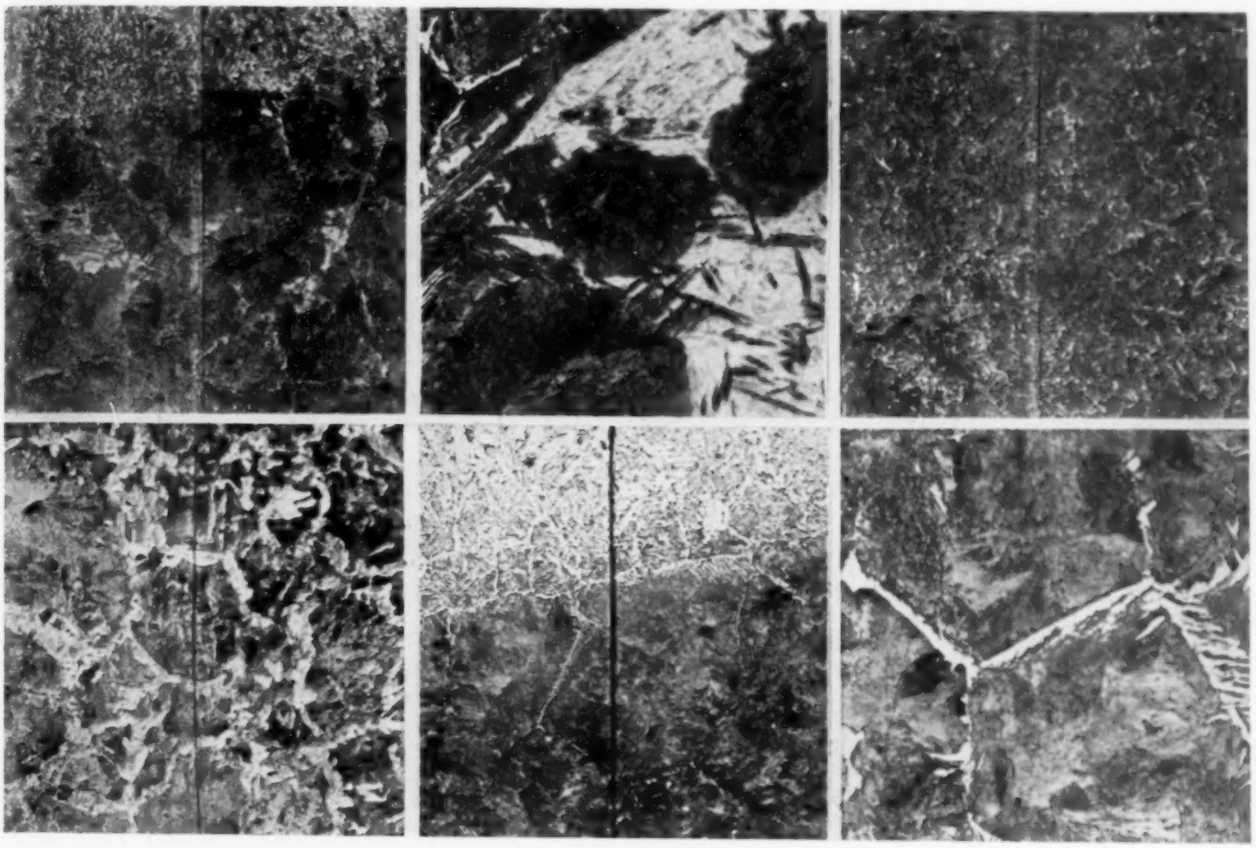


Fig. 69—SAE 1040 0.16 Inch from Junction. No Preheat. 100 X

Fig. 70—SAE 1040 at Junction. 200° F. Preheat. 100 X

Fig. 71—SAE 1040 at Junction. 200° F. Preheat. 500 X

Temperature Maxima Recorded During Welding

These are shown in Fig. 64. As in most cases, maxima are reached at location 3–7 when welding at location 4–6.

Increments, maxima over preheats are:

Preheat, ° F.	Increment, ° F.
70	420
200	420
300	420

This uniform increment is somewhat unusual, in that most other tests indicate that as preheat is increased, increment is somewhat decreased.

Hardness of Weld and Base Metal

Hardnesses, at Position 1 and 4 are shown in Fig. 65. At Position 4 about the same maximum hardness is reached (255–265 Bhn.) irrespective of preheat. A rather high hardness (275 Bhn.) is reached with no preheat. Deeper hardening in position 4, all preheats, over Position 1 is noted. In all cases at the maxima hardness on readings $^1/_{16}$ inch apart, and many readings above and below were taken to be certain that true maxima were found, in so far as is possible with Rockwell test. As shown later, martensite and possibly some retained austenite was found and hardness readings are inadequate.

Microstructure

All macrostructures were sound looking. Aside from different depths of affected zones, depth at Position 4 being greater than at Position 1, and the zones of a given position slightly deeper with increased preheats, the macrostructures are not particularly significant. All welds were filled and nicely flush with tops of samples.

The structure at weld juncture at no preheat, Position 1 is shown in Fig. 66, at 100 diam. Note not only excessive granulation, but especially distinct light patches with an acicular structure within. This is indicative of martensite and possibly austenitic residue and that this is indeed the case is shown in Fig. 67 at 500 diam. Acicular martensite plus troostitic groups are seen. The condition is of course highly undesirable. The inadequacy of hardness readings is painfully evident.

Figure 68, 100 diam. is 0.08 inch from the weld junction, and shows grain refinement. The granular structure of the SAE 1040 base is quite evident at 0.16 in. from juncture as in Fig. 69, 100 diam. This steel is undesirably coarse grained to start, as shown by a grain size of equal to or over A. S. T. M. No. 1.

The juncture at 200° F. preheat, Position 1, is shown in Fig. 70, 100 diam. Coarsening of the grain is evident in the weld metal, but is rather slight. There is a distinct line of demarkation between weld metal and SAE 1040—which is undesirable. The SAE 1040 is very coarsely granular, and without doubt lacking in resistance to impact and vibratory stresses. The coarse

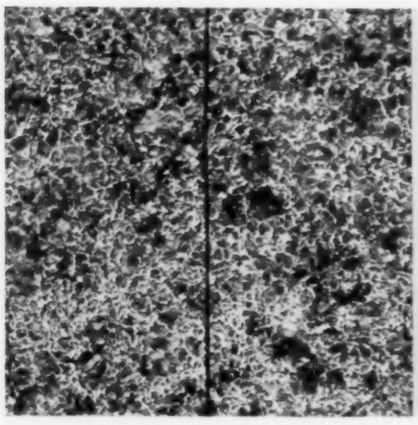


Fig. 72—SAE 1040 0.08 Inch from Junction. 200° F. Preheat. 100 X

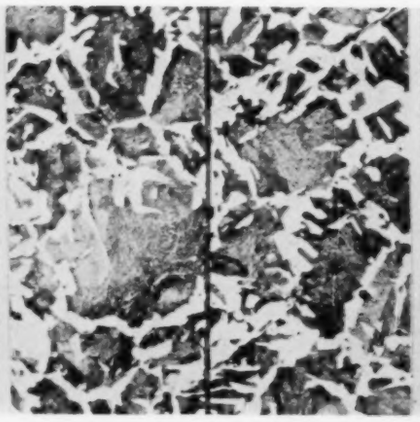


Fig. 73—SAE 1040 0.16 Inch from Junction. 200° F. Preheat. 100 X

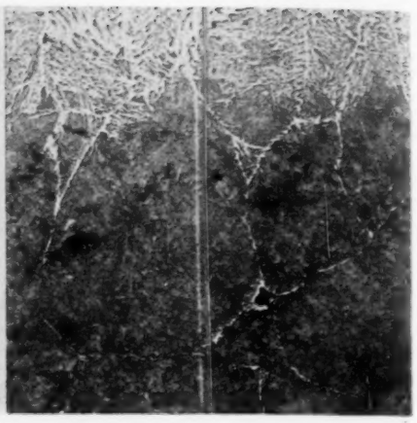


Fig. 74—SAE 1040 at Junction. 300° F. Preheat. 100 X

grain near the juncture is a product of high temperature growth or coarsening. A close-up of the SAE 1040 near the juncture at 500 diam. is shown in Fig. 71. Ferrite divorcement to the grain envelopes is not quite complete. The intragranular structure is largely sorbitic, pearlitic lamination not yet quite clear. Figure 72, 100 diam. shows the fine grained zone at 0.08 inch from the juncture. Fully normal structure is not quite fully regained at 0.16 inch from the juncture, as indicated by Fig. 73, 100 diam.

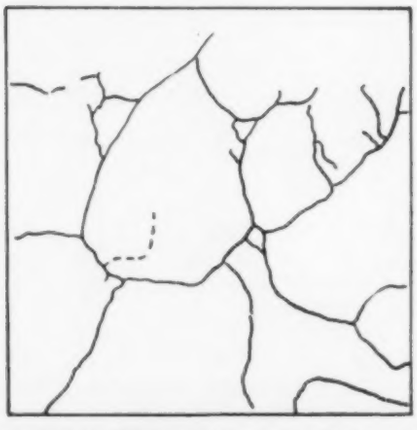


Fig. 75—Grain Size of Fig. 74

Proceeding to the 300° F. Test 9 preheat, Position 1, the structure at the juncture, at 100 diam. is shown in Fig. 74. A sketch of this grain size is shown in Fig. 75. Extremely coarse grain growth of the SAE 1040 is painfully evident as bad or worse than the 200° F. preheat. A "close-up" of the SAE 1040 500 diam. in this location is shown in Fig. 76.

A zone of grain refinement like that shown in Fig. 72 is found at 0.08 inch from the juncture. Fig. 77, 0.16 inch from juncture, was definitely within the gamma temperature range, especially as indicated by structure at upper part of photomicrograph. Note partial reabsorption of ferrite envelopes, and apparent new grain formation within the residue of older grains. To show this change more clearly and completely this photomicrograph is mounted with the part nearest to the weld juncture to the left—i.e. is turned 90° left from normal position on sample.

Comment

It is well recognized that surface hardening is related more directly to carbon content, whereas depth of hardening is to a degree dependent upon alloy content, original austenite grain size and other factors.

The formation of martensite at Position 1 (start of bead) no preheat is not surprising. The fact that a pre-

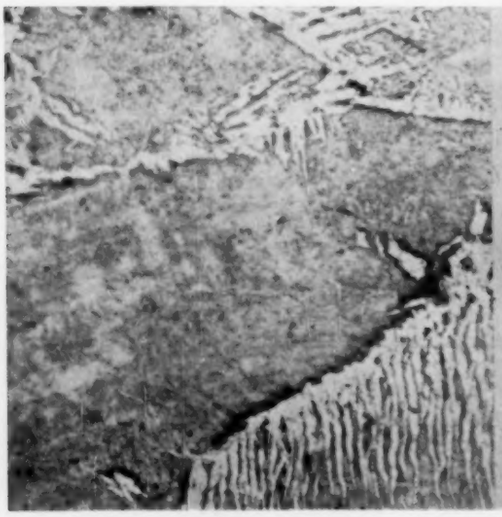


Fig. 76—SAE 1040 at Junction. 300° F. Preheat. 500 X

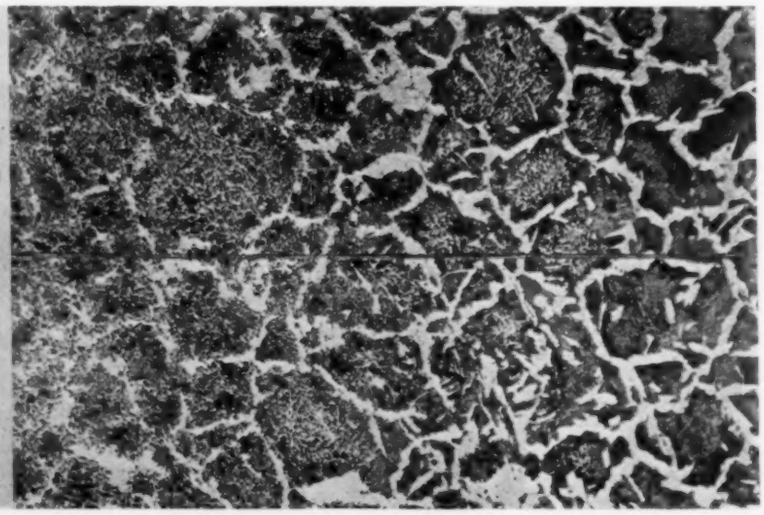


Fig. 77—SAE 1040 0.16 Inch from Junction 300° F. Preheat. (Left) Toward Junction. 100 X

heat of only 200° F. is effective in preventing martensitic formation in this case is significant.

Extreme grain coarsening of this medium carbon steel is distressing. We would expect this condition to lower resistance to shock and vibratory loads, probably lower the ductility somewhat, and per se to have not a great deal of effect on R. T. tensile, although higher yield point is probable.

The abrupt change in etching rates between the SAE 1040 and the cellulose coated electrode A is mainly due to the difference in carbon content.

RECAPITULATION

Under the conditions of test outlined the hardening propensities of the steels followed carbon contents rather closely. With single bead laid "cold" there is no tendency toward martensitic residue in the WCM-15 low carbon alloy. Perceptible tendency is noted in the cast and wrought materials around 0.23 to 0.27 per cent carbon content. It is the opinion of the writers that this tendency would disappear if multiple beads were laid, because of "automatic preheating" of the welding operation. The highest carbon materials (0.38 and 0.41 per cent carbon) require some preheating to avoid initial martensitic formation.

As opposed to its recognized advantages, preheating may in some cases result in undesirable coarse grained structural formations. Steels examined varied widely in this tendency. All weld brittleness cases are not due to formation of hard layers, and grain coarsening tendencies of steels should be tested before selections for welds are made. It is well known that great variations in grain coarsening tendency may occur in steels of a given type and nominal composition. Therefore, the writers findings should not be interpreted to infer that all steels similar in type to those coarsened in the writers experiments will necessarily behave similarly.

Hardness testing alone is not a fully reliable guide or indicator of the structural formation at and adjacent to welds. Although more direct and exhaustive approaches than those employed would be desirable, the opinion is that study of microstructures is an excellent if indirect method of evaluation.

ACKNOWLEDGMENT

The writers are indebted to The Lunkenheimer Company for permission to publish these excerpts from certain welding studies, and to Messrs. Frank P. Rhame and F. H. Hehemann for various suggestions made during prosecution and presentation of the work.

JIG for RESISTANCE WELDING of Thin Gage Stainless Steel Tubing*

INTRODUCTION

THIS report describes the design of a resistance welding jig to manufacture large-diameter seam-welded tubes of thin sheet stainless steel. The present design will produce tubes of any length by introducing the lower electrode into the tube through the unwelded lap behind the electrodes.

A preliminary investigation was made to determine the optimum conditions for the three principal variables of the resistance welding process, namely, pressure, current value and time of current flow. Tubes welded in the jig were then tested in torsion, fatigue, bending and compression, the tests being conducted also with tubes made by a riveted construction for purposes of comparison.

Apparatus

Welding Apparatus.—A resistance welding system consists of a transformer to change the power as supplied so that the optimum current or heating effect is obtained at the weld, a mechanism to conduct current to the weld and apply pressure at the weld, a means of timing the current to insure uniform length of current flow in successive operations, and a method of holding the material being welded.

The welding jig built at the University for the seam welding of thin gage tubing is illustrated in the following photographs and diagrams. Figure 1 shows a rear view of the jug with a short tube in place for welding. Tubes, as prerolled, are started through the back assembly and

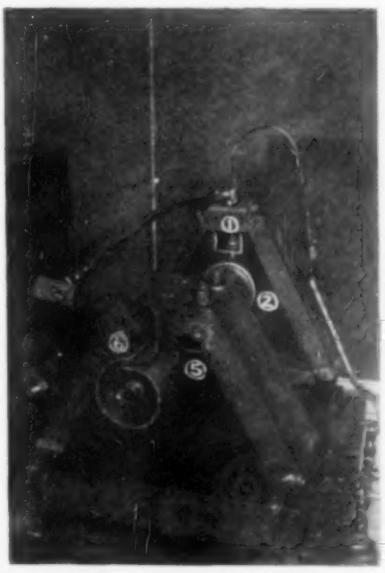


Fig. 1—Rear View of Welding Jig with Tube in Place for Welding, Showing the Flexible Electrode Lead Passing Through the Lap Seam of Tube

* Abstract from report on investigation conducted by W. T. Barker and A. R. Walker at University of California, Department of Mechanical Engineering. Contribution to Fundamental Research Division, Welding Research Committee.

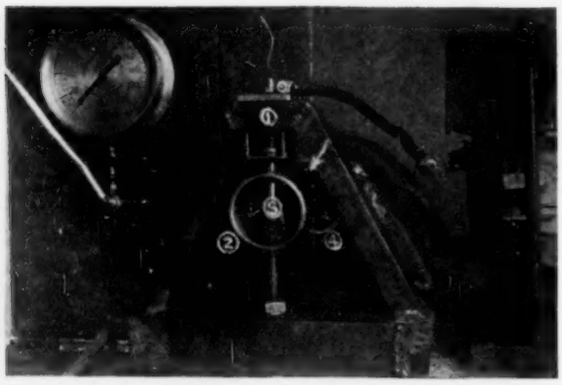


Fig. 2—Front View of Jig, Showing Front Assembly and Seam Guide on Forming Ring Indicated by Arrow

then pulled into the front forming ring where the first weld is made. Leads from the transformer to go the front and back of the jig.

For the application of pressure to the tubes being welded the bellows, integral with the upper electrode, is used as seen in Fig. 2. Current is passed from the transformer through the copper bellows assembly to the upper electrode. The lower electrode receives current through the tubular support from the flexible woven cable which passes through the lap at the seam in the unwelded tube back of the main jig frame. This arrangement is best seen in Fig. 1 at the number (6).

The tubes are held in place for welding by two forming rings shown most clearly at (2) and (4) in Fig. 2. These rings are of the same diameter to allow the production of uniform diameter tubing. At the front forming ring, tubes are held against the ring by three fiber rollers radiating outward from the electrode conductor. To guide the tubes and keep the seam straight, a clip is used on the back forming ring which may be adjusted for the width of seam desired (see the arrow in Fig. 2). The upper part of the seam is held against this guide when a specimen is projected through the jig.

The lower electrode support, in which the electrode conductor is placed, is supported by a bracket shown at (5) in Fig. 1. The question of rigidity enters here where the front and back assemblies must line up very well to provide for easy movement of tubes through the welder. A sacrifice between a rigid and a flexible support bracket is used as shown by the drawing of Fig. 3. While little or no twisting of the lower electrode assembly at the front is allowed, enough lateral movement is obtained so that the three guide rollers adjust the electrode in the center of the front forming ring and take most of the load when a weld is made.

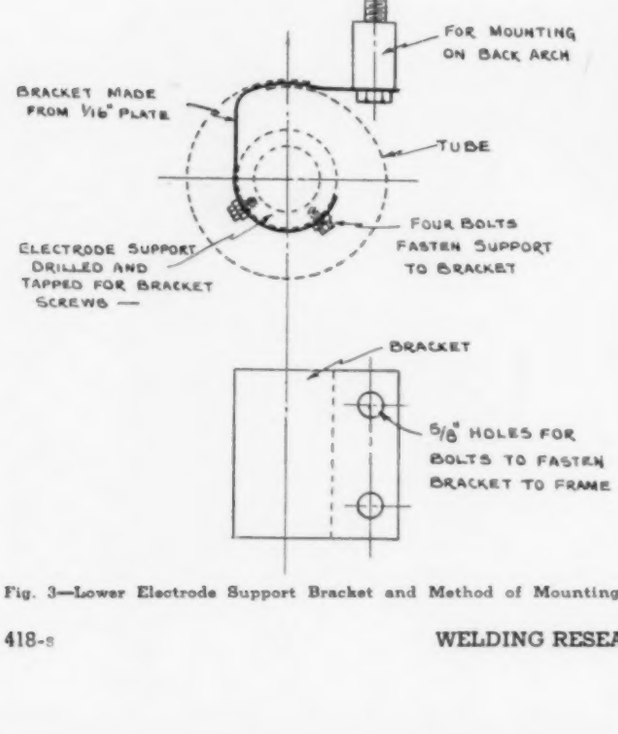

FOR MOUNTING ON BACK ARCH
BRACKET MADE FROM 1/16" PLATE
TUBE
FOUR BOLTS FASTEN SUPPORT TO BRACKET
ELECTRODE SUPPORT DRILLED AND TAPPED FOR BRACKET SCREWS
BRACKET
5/8" HOLES FOR BOLTS TO FASTEN BRACKET TO FRAME

Fig. 3—Lower Electrode Support Bracket and Method of Mounting

The rest of the welding apparatus consists of a mechanical timer and a welding transformer.

The photographs of the welding jig have numbers placed on or next to the important units discussed. The following is a list of these key numbers with the units represented.

(1) Bellows and upper electrode assembly
(2) Front forming ring
(3) Radial guide rollers in front forming ring
(4) Back forming ring
(5) Lower electrode support bracket
(6) Flexible cable passing through the lap seam

Testing Apparatus.—Most of the standard types of tests were used in the testing of calibration strips and test tubes. A standard Olsen 10,000 lb. Wire and Rope Tester was used to test the strips in tension. Compression and bending tests of the tubes were made in the 30,000 lb. Universal Riehle testing machine. For the compression tests, caps were used on both ends of the specimens. Wooden bulkheads fitted firmly in the ends prevented buckling inward and the caps on the outside prevented buckling outward. For bending tests, wooden bulkheads were used at each end and at the center of the simply supported beam with a center concentrated load. At each of these bulkheads, a narrow ring was firmly fitted on the outside of the tube to reduce concentrations of stress at these points of concentrated load. Rocking supports were used to eliminate secondary stresses.

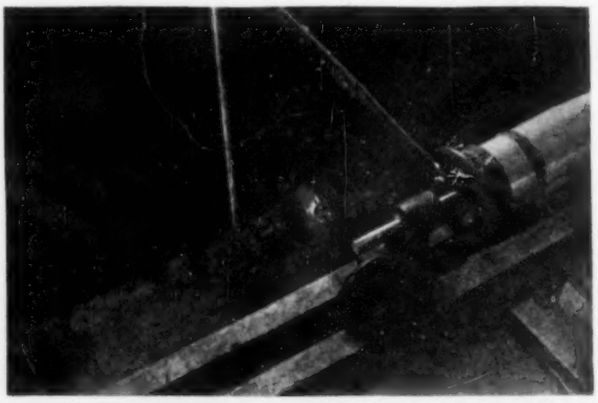


Fig. 4—Apparatus Used for Torsion and Fatigue Tests of Tubes, Showing Arrangement for Fatigue Test

The torsion and torsion fatigue tests were made on the apparatus shown in Fig. 4, which was specially designed and built for these tests. For the torsion tests, loads were applied to a scale on an extending arm at a known distance from the tube center. For the fatigue tests a motor, eccentric and linkage were used to obtain a repeated angular movement of one end of the tube.

CORRELATION OF VARIABLES

There are three principal variables affecting the strength of a weld: electrode current, time of current and electrode pressure. The value and time of current

must vary inversely for any one pressure to obtain good welds. Heating caused by long time of current flow is harmful to the metal and thus the optimum condition is the shortest possible weld allowing good fusion of the metal. An optimum value of pressure would seem to exist. At too low pressures there is surface burning and consequent chance for corrosion. At too high pressures the resistance in the metal is low and complete fusion does not take place. There is also a tendency for reduced section at the spot because of compression of the plastic metal.

Tests on 18–8 Stainless Steel Sheet 0.01 Inch Thick

Time of Current.—The optimum time of current recommended by manufacturers of resistance welding equipment was used as a starting point in the correlation tests for the 0.01-inch sheet first considered. At this value of $1^1/_2$ cycles, the variables, electrode pressure and welding current, were correlated with the strength.

Value of Current.—The secondary current was varied by inserting cast-iron grid resistance in the primary circuit. It was seen from transformer calibration test results that at 0.6 cycles the current was built up to a steady value, so that for $1^1/_2$ cycles (as a good approximation) we considered the current at full value over the entire welding period.

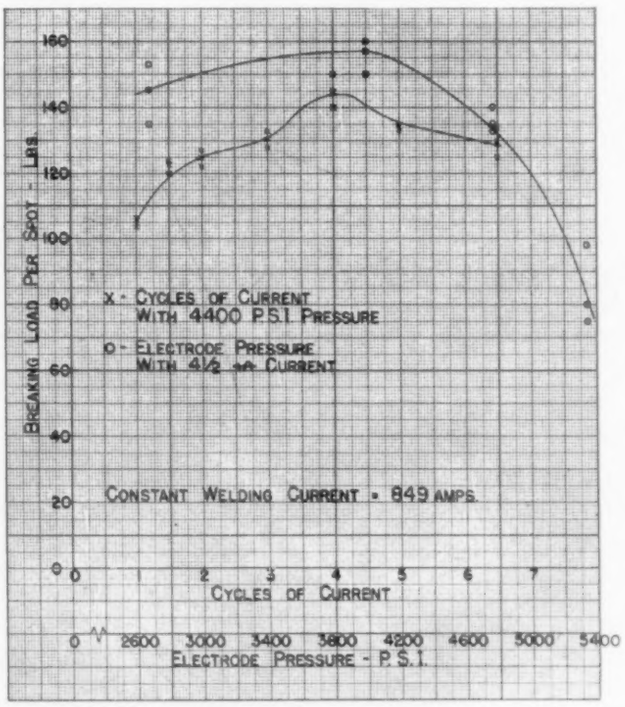


Fig. 5—The Effect of Electrode Pressure and Time of Current on the Strength of Spot Welds in 0.014-Inch Stainless Steel Sheet

Strength.—As suggested by G. O. Hoglund, the breaking load is probably better than the breaking stress as an indication of weld strength, due to the difficulty in estimating the original area of the spot. A solution to this problem might be found by taking some mean of the "diameters" measured at right angles as an indication of the actual area of the spot. However, we considered the breaking load for one spot, tested in shear, as an indication of strength.

The results of these tests are shown in the following tabulations:

Table 1—The Effect of Current Value on Strength of Single Spot Welds in 0.01-Inch Stainless Steel Sheet

Electrode Current (Amp.)	Breaking Load (Lb.)*
849	66.7
832	95.7
823	90.0
810	88.3
798	69.3

* The value shown is the average of the three highest values obtained.

Table 2—The Effect of Electrode Pressure and Current Value on Strength of Single Spot Welds in 0.01-Inch Stainless Steel Sheet

Electrode Pressure Psi	Breaking Load (Lb.) for Indicated Current Value				
	798 Amp.	810 Amp.	823 Amp.	832 Amp.	849 Amp.
3400	30	76	80	90	60
4000	35	85	90	90	65
4400	68	90	95	95	70
4800	80	90	85	102	65
5080	60	65	70	87	65

It may be seen from the tables that the maximum breaking loads were obtained with a current of 832 amperes.

Testing of multiple spots spaced at various distances showed an appreciable increase in strength at spacings of $^1/_8$ to $^1/_4$ inch. These results agreed very well with the practice of a large manufacturing concern, which employs the following spacings for spot welding 18–8 stainless steel sheet:

0.010 inch to 0.015 inch thickness $^3/_{16}$ inch apart
0.020 inch to 0.030 inch thickness $^1/_4$ inch apart

Accordingly, a spacing of $^3/_{16}$ inch was used in making the spot tests on the 0.014 inch stainless steel sheet and in welding the tubes for the mechanical tests.

Tests on 18–8 Stainless Steel Sheet 0.014 Inch Thick

From the tests on 0.01-inch sheet, it was seen that no added resistance was needed in the primary circuit for the welding of 0.014-inch sheet. Greater thickness of metal requires larger current flow. Accordingly, in the correlation tests with the 0.014-inch sheet the transformer capacity limited the current and only the other two variables were considered: time of current flow and pressure on electrodes. The results of these tests are shown graphically in Fig. 5.

As seen from the curves for the 0.014-inch sheet, optimum conditions were approximately: 4.1 cycles of current, and 4000 psi electrode pressure. The typical weld at these optimum conditions had a diameter of 1.05 mm. or 0.0414 inch, as determined by a measuring microscope, and the shear strength was calculated to be as follows:

$$\text{Strength of weld} = P/A = \frac{157}{\pi/4(0.0414)} = 117{,}000 \text{ psi}$$

Microscopic examination of typical welds made under the above conditions of welding revealed that pipes had formed in the center of the welds. This condition, caused by insufficient current and too long a welding time, undoubtedly had a weakening effect on the welds. A long welding cycle causes the hot center of the spot to expand, and vapors force the surrounding metal away, leaving a piped and coarse-grained center. A higher

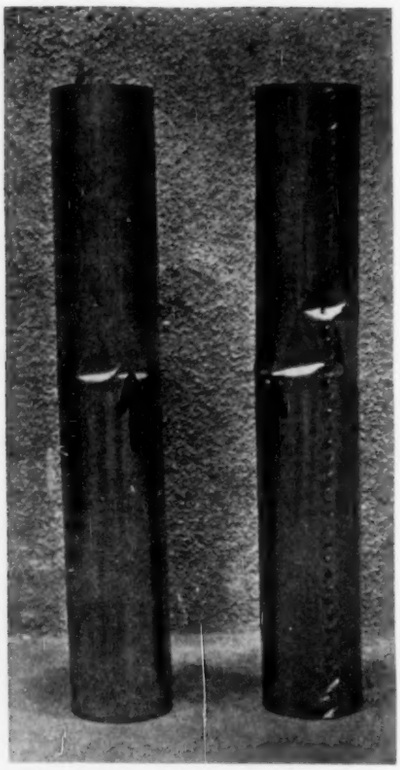


Fig. 6—Comparison of Spotwelded and Riveted Tubes in Bending

current value and a shorter welding time would have resulted in less heat transfer and expansion, and would have produced stronger welds.

Mechanical Testing of Tubing

Torsion, fatigue, bending and compression tests were made with tubing of the following description:

Material	18–8 stainless steel
Tube diameter	4 inches
Wall thickness	0.014 inch
Spacing of spot welds	$^{3}/_{16}$ inch apart

Table 3—Results of Torsion Tests

Length of Tubes (Inches)	Torque Required to Produce Wrinkling (Pound-Inches)	Stress (Psi)
21.25	2570	5200
8.0	2765	5590
5.75	5310	10800*
17.0 (Riveted)	2750	5550

*Failure of this tube resulted in eight wrinkles instead of the usual four.

Table 4—Results of Torsion Fatigue Tests

Applied Stress (Completely Reversed)	Angle of Twist	Cycles to Produce Failure	Location of Failure
60,000 psi	14°	1650	Joint
16,800	1.3°	416,000	Metal*

*Failure started from small nick and depression on surface of tube.

Table 5—Results of Bending Tests

Length of Tube	Gage Length	Load	Stress
36.5 inch	16.5 inch	770 lb.	7200 psi
29	13	1040	7660
24 riveted	10.2	640	3690
24	10.6	1190	7210
16	6.6	1700	6360

The tests were performed with the equipment described previously under the heading "Apparatus," and similar tests were made with tubes of a riveted construction in order to afford a comparison.

The results of the tests are shown in Tables 3 to 6.

Because the bending tests were made under non-standard conditions, the data should not be compared with the results of other bending tests. It was interesting to note, however, that although all tubes were bent beyond the first wrinkling point, in no case did any of the spot welds fail. Representative tested specimens are shown in Figure 6.

The possibility of eccentric loading should be kept in mind in considering the compression test data. In these tests, also, there was not one case of a spot weld having failed, even though one tube was compressed until it had shortened from 16 to 10 inches.

Table 6—Results of Compression Tests

Tube Length	Gage Length	Length-Radius Ratio	Speed of Test (Inch/Min.)	Wrinkling Load	Stress (Psi)
36 inch	33 inch	16.5	0.24	3210 lb.	18,200
32	29	14.5	0.24	4865	27,700
28	25	12.5	0.24	5690	32,300
24	21	10.5	0.24	2695	15,300
19	16	8.0	1.25	2475	14,000
16	13	6.5	0.07	4270	26,700
11	8	4.0	0.06	2820	16,000
8	5	2.5	0.06	3000	17,000
32 R	29	14.5	0.24	2600	14,800

R = Riveted Tube.

THIRD REPORT OF THE
Welding Research Committee*

REPEATED STRESS TESTS ON WELDS OF PLATES ½ INCH THICK

TESTS were made to determine the effect of the following factors on the fatigue strength of the weld:

1. The heat treatment applied to the weld.
2. The alternative use of two grades of parent plate for the weld.
3. The surface condition of the weld.

Particulars of the tests are shown in Table 1 and the results are plotted in Figs. 1–11.

Table 1—Repetition Stress Tests on P Welds*

Annealing temperature, 650° C.; normalizing temperature, 910° C.	Condition	Stress
Unmachined specimens:		
Grade 2 plate†	Annealed	Zero mean stress
" " "	Normalized	" " "
" " "	As welded	" " "
" " "	Annealed	Not zero mean stress
Grade 3 plate†	"	Zero mean stress
" " "	Normalized	" " "
" " "	As welded	" " "
Specimens machined flush on surface:		
Grade 2 plate	Normalized	Zero mean stress
Grade 3 plate	"	" " "
" " "	"	Not zero mean stress
" " "	As welded	" " " "
Specimens machined all over:		
Grade 3 plate	Normalized	Zero mean stress
" " "	Annealed	" " "
" " "	As welded	" " "

* The P welds were made with an electrode containing 0.15 C, 0.57 Mn, 0.10 Si with iron silicate covering containing manganese. The weld metal contained 0.07 C, 0.25 Mn, 0.023 N. Normalized all-weld-metal specimens 0.30 inch diameter had tensile strength = 59,100 lb./in.², 21% elong. in 2 in. 40% reduction of area.

† The tensile strength of grade 2 plate was 61,500 lb./in.², and that of grade 3 plate was 70,500 lb./in.

Effect of Heat Treatment. Figures 1 and 2 show respectively the curves for unmachined specimens of P weld of grade 3 and grade 2 plate in the three conditions: normalized (910° C.), annealed (650° C.), and as welded.

In each case heat treatment after welding has a slightly beneficial effect on the fatigue properties of the weld. The effect, however, is small. As these results differ from those obtained previously for machined welds,* a further series of tests was made on machined specimens of P weld. The results, on the whole confirm those of the previous tests (Fig. 3), in which, for machined specimens of P weld of grade 3 plate, the "as welded" specimens had the best fatigue range.

* Abstract of Report published by The Institution of Mechanical Engineers of Great Britain.

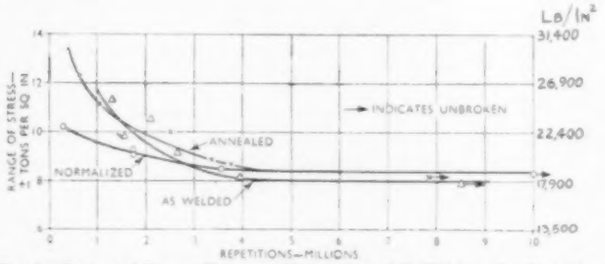


Fig. 1—Repeated Stress Tests on Unmachined P Welds: Grade 3 Plate

Constant bending moment machine; zero mean stress. All tests carried out at room temperature.
○ Normalized at 910° C.
× Annealed at 650° C.
△ As welded

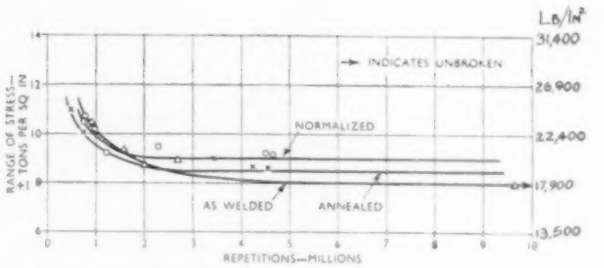


Fig. 2—Repeated Stress Tests on Unmachined P Welds: Grade 2 Plate

Constant bending moment machine; zero mean stress. All tests carried out at room temperature
○ Normalized at 910° C.
× Annealed at 650° C.
△ As welded

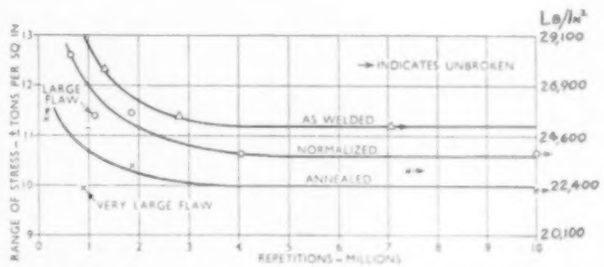


Fig. 3—Repeated Stress Tests on Specimens of Rectangular Section

Specimens of rectangular section machined from plate. Constant bending moment machine. P welds; grade 3 plate; all tests carried out at room temperature

Effect of Grade of Plate Used for the Welds. Figures 4–6 reproduce the curves for unmachined P welds of grade 2 and grade 3 plate in the three conditions: normalized at 910° C., annealed at 650° C., and as welded. Figure 7 gives the curves for specimens of normalized P welds of grade 2 and grade 3 plate with surfaces machined flush.

These curves show that the grade of plate used has made no appreciable difference in the fatigue strength of the weld when tested either with the surfaces unmachined or machined flush. In nearly all cases fracture started at the weld junction and then proceeded through the

* Proceedings Instn. Mech. Engrs., 1936, vol. 133, p. 55, Fig. 43.

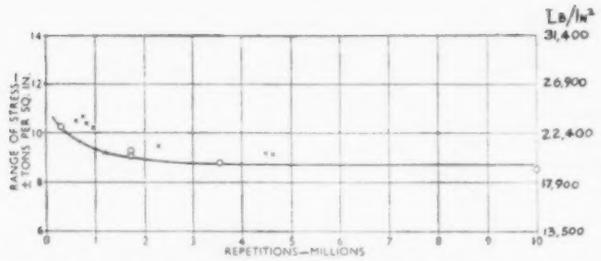


Fig. 4—Repeated Stress Tests on Unmachined P Welds, Normalized at 910° C.

Constant bending moment machine; zero mean stress. All tests carried out at room temperature

× Grade 2 plate ○ Grade 3 plate

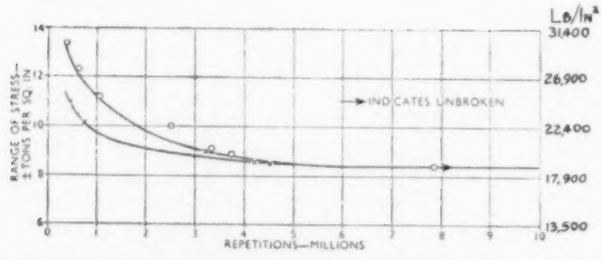


Fig. 5—Repeated Stress Tests on Unmachined P Welds, Annealed at 650° C.

Constant bending moment machine; zero mean stress. All tests carried out at room temperature

× Grade 2 plate ○ Grade 3 plate

plate. The fatigue range has a value below that which would be given by the plate material itself (34,000 lb./in.²), and it would appear that, owing to welding, the resistance to repeated stress of the plate material, particularly near the weld junction, is considerably reduced. These results confirm those from other tests made on welds in which failure has occurred in the plate either immediately near to the weld or at some distance from it.

Although the tests so far carried out show that with the change in the grade of the parent plate there is little or no difference in the fatigue strength of the weld, no general conclusions should be drawn until further results are obtained from tests on plates of various compositions and tensile strengths. The Committee understands that a research on the effect of variation in the composition of parent metal and welding rods on welding properties is being undertaken by another committee.

Effect of Surface Condition. The results given by specimens of normalized P weld, made with grade 3 plate tested (1) with the specimens machined, (2) surfaces of weld machined flush with the plate, and (3) surfaces unmachined, were set out in the Second Report.* For unmachined specimens the fatigue range was ±19,700 lb./in.², for specimens with the surfaces machined flush ±21,500 lb./in.², and for specimens machined all over ±24,000 lb./in.² The results from a further series of tests of welds made with grade 3 plate, in the condition as welded, for the three conditions of surface, are shown in Fig. 8. Tests made on normalized P welds of grade 2

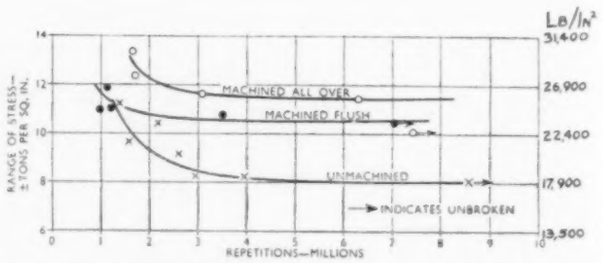


Fig. 8—Repeated Stress Tests on P Welds with Grade 3 Plate in Condition As Welded

○ Machined all over ● Machined flush × Unmachined

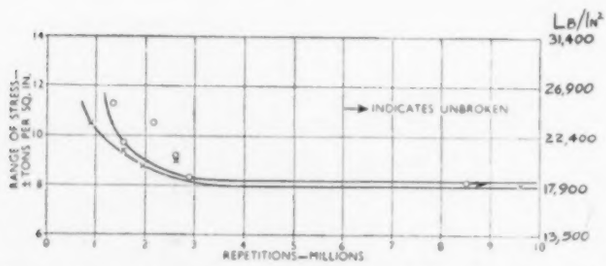


Fig. 6—Repeated Stress Tests on Unmachined P Welds, Condition As Welded

Constant bending moment machine; zero mean stress. All tests carried out at room temperature

× Grade 2 plate ○ Grade 3 plate

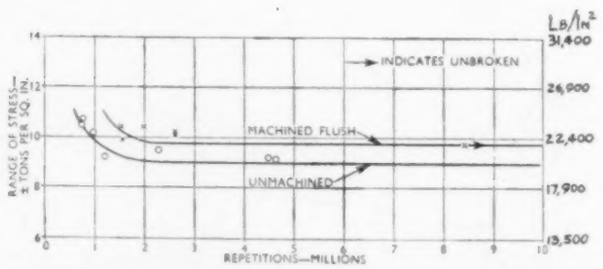


Fig. 9—Repeated Stress Tests on P Welds with Grade 2 Plate, Normalized at 910° C.

Constant bending moment machine; zero mean stress. All tests carried out at room temperature

× Machined flush ○ Unmachined

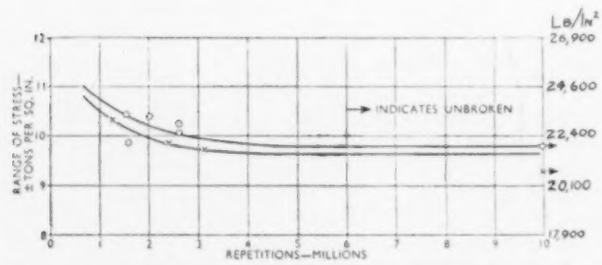


Fig. 7—Repeated Stress Tests on P Welds, Machined Flush and Normalized at 910° C.

Constant bending moment machine; zero mean stress. All tests carried out at room temperature

plate, either machined flush or unmachined (Fig. 9), confirm the results obtained from the tests on P weld of grade 3 plate. It appeared unnecessary to test machined specimens of P weld of grade 2 plate.

Tests at Various Mean Stresses. Tests on unmachined specimens of P weld of grade 2 plate in the annealed (650° C.) condition at a mean stress of 16,100 lb./in.² gave the results plotted in Fig. 10. The total fatigue range for 1,457,000 repetitions was approximately

* Proceedings Inst. Mech. Engrs., 1936, vol. 133, p. 56, Fig. 44.

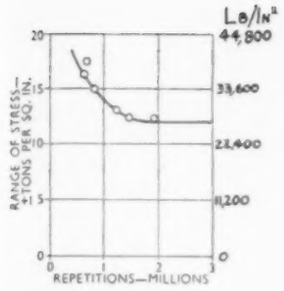


Fig. 10—Repeated Stress Tests on Unmachined P Welds with Grade 2 Plate, Annealed at 650° C.

Constant bending moment machine; mean stress, 16,100 lb./in.². All tests carried out at room temperature

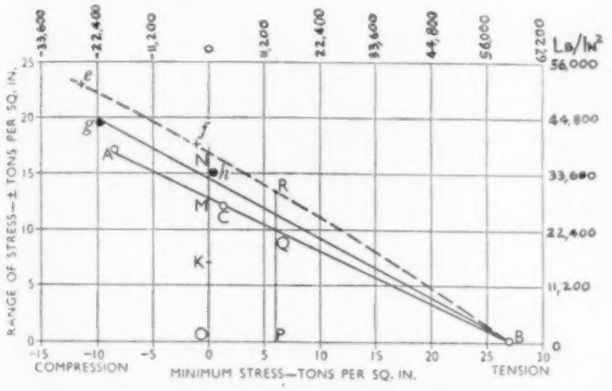


Fig. 11—Repeated Stress Tests on P Welds

Constant bending moment machine. All tests carried out at room temperature.

The curve through the points *e* and *f* shows the approximate safe ranges, corresponding with any given minimum stress for 0.5 × 10⁸ repetitions at ordinary temperatures. For example, if the pressure is raised periodically from zero to a pressure that will give a calculated stress of, say, 15,700 lb./in.² represented by OK, then the additional stress that can be safely resisted for more than 2 × 10⁸ repetitions is 12,300 lb./in.², represented by KM, and for 0.5 × 10⁸ repetitions it is 22,400 lb./in.², represented by KN. If the minimum applied stress due to pressure is, say, 13,400 lb./in.², and while this steady stress is acting, a unidirectional stress is periodically superimposed, then the stress represented by PQ could be resisted for 2 × 10⁸ reversals or the stress represented by PR for more than 0.5 × 10⁸ reversals

26,900 lb./in.² In Fig. 11, in which fatigue range is plotted on a base of minimum stress, it is seen that the two points A and C for annealed P weld of grade 2 plate, corresponding to a mean stress of zero and of 16,100 lb./in.² respectively, lie approximately on a straight line passing through the value of the tensile strength. Figure 11 also shows the results of tests at two mean stresses with specimens of normalized P welds of grade 3 plate which had been machined flush. These also lie approximately on a straight line passing through the value of the tensile strength. The curve, drawn for unmachined specimens of P welds, therefore shows, by the ordinate of the line AB, the safe range of stress that may be imposed upon the particular joint for any given minimum stress. If the total range of stress in the joint is not greater than 28,000 lb./in.², failure of the joint will not take place in less than 2 × 10⁶ repetitions of stress.

Figure 11 of the Second Report* shows that the static breaking strength at 250° C. is much higher than at ordinary temperatures, while Fig. 41 of that Report† shows that the fatigue range is higher at 250° C. than at ordinary temperatures. Further, Fig. 16 of the Second Report‡ shows that the stress to produce a rate of creep not greater than 10^{-6} in. per in. per hour for a weld metal is about 56,000 lb./in.² Thus, from the note appended to Fig. 11, it will be seen that for 0.5 × 10⁶ repetitions the safe range of stress for a P weld of grade 2 plate (annealed at 650° C.), unmachined and at a temperature corresponding to a saturated steam pressure of about 550 lb. per sq. in., is not likely to fall below the curve *ef*B (Fig. 11).

* Proceedings Instn. Mech. Engrs., 1936, vol. 133, p. 22.
† Proceedings Instn. Mech. Engrs., 1936, vol. 133, p. 54, Fig. 41.
‡ Proceedings Instn. Mech. Engrs., 1936, vol. 133, p. 27, Fig. 16.

EFFECT of CERTAIN FACTORS on the Izod Value of Weld Metal*

By L. W. SCHUSTER,†

THE tests, unless otherwise stated, were all made at air temperature, but, to avoid variability due to any temperature change, all specimens for a particular experiment were tested together.

CHANGE IN THE IZOD VALUE DUE TO THE PRESENCE OF UNREFINED WELD METAL

As an outer run of an unmachined weld is of unrefined metal, the bottom of the notch of an Izod specimen may lie in either refined or unrefined metal. Equally, areas of unrefined metal may be present at any depth of the specimen, as, when a run of weld metal is deposited, the whole of the neighboring run or runs of metal may not be refined throughout their full depth, depending upon the quality of the electrode and the particular circumstances attached to the weld. It was desired to determine the relation between the decrease in the Izod value and the depth of unrefined metal at any distance from the surface of a specimen.

A series of welds was made by the use of three thicknesses of plates: 2 inches, 1 inch and $^1/_2$ inch (see Fig. 2).

The scheme adopted is shown by Figs. 1 and 2, and it will be noticed that, by laying a deep upper run at the top of the weld, cutting the specimens out from different depths of the weld and placing the notches in either the top or bottom of the specimen, specimens of widely varying constitution could be obtained from the same weld. A high-grade weld metal was used (electrode M1), as this was considered necessary to give the required degree of refinement of the lower runs in the thicker plates, together with the required consistency in result. As a further step toward consistency, speci-

* An account of a research published in conjunction with the Welding Research Committee of The Institution of Mechanical Engineers. Abstract of a report published by The Institution of Mechanical Engineers.

† Research Engineer, British Engine, Boiler, and Electrical Insurance Company, Ltd., Manchester.

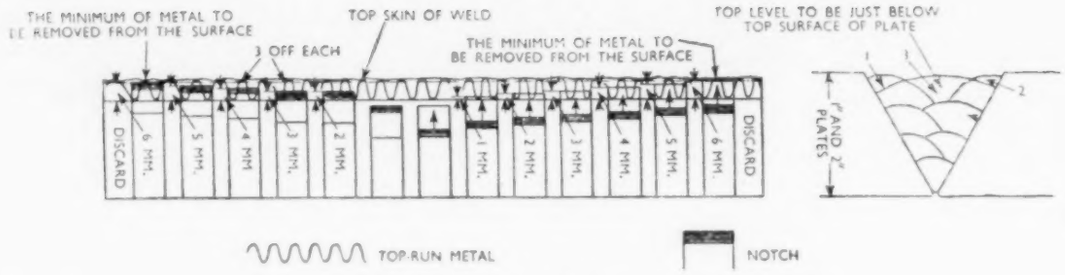


Fig. 1. Method of Cutting Samples from Butt Welds

1-inch and 2-inch plates; notches 2 mm. deep throughout.

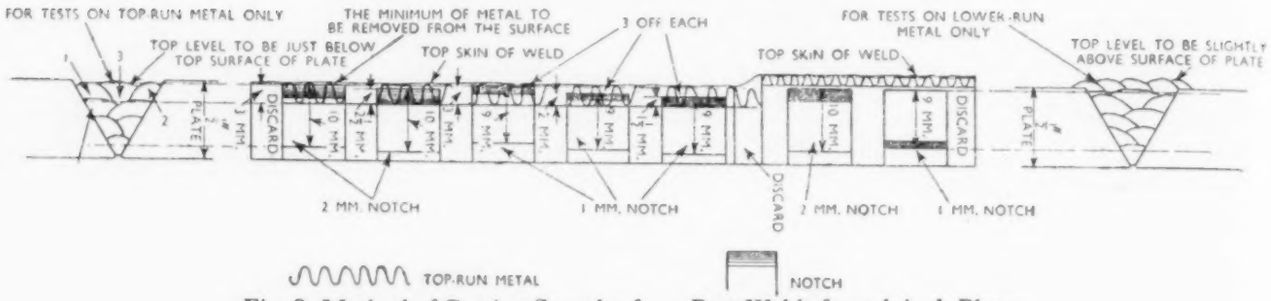


Fig. 2. Method of Cutting Samples from Butt Welds from ½-inch Plates

1-Inch and 2-Inch Plates; Notches 2 Mm. Deep Throughout

Plate Thickness, Inches	Electrode Diameter (Inch) for Lower Runs	Electrode Diameter (Inch) for Last Run
2	0.192	0.232
1	0.160	0.192
½	0.160	0.160

mens were given a low-temperature heat treatment.

To ascertain the effect of the presence of unrefined weld metal, it is necessary to use wholly refined weld metal as a standard of comparison. When, therefore, the test specimens comprised areas of unrefined metal in positions not desired, the results were discarded, and the same applies to specimens containing other noticeable defects.

Discussion of Results.—The average test figures are plotted in the form of curves (Fig. 3), which are sufficiently smooth to allow their use in determining the effect of unrefined metal in welds of this class. The difference in result for the different plate thicknesses is ascribed more to an idiosyncrasy of the particular welds than to the actual change in plate thickness, different sizes of electrode having been used.

Though, in the weld just described, the thickness of the layer of unrefined metal in a given specimen was sufficiently uniform to allow of tolerably accurate measurement, such a state must not always be expected in any weld. For another weld made in a plate 1 inch thick with an electrode (M6) of inferior quality, the rod being of the uncovered cored type, and the diameter being 0.15 inch for the lower runs and 0.24 inch for the top run, the results were by no means satisfactory, and the line of demarcation between the runs was not so clearly defined.

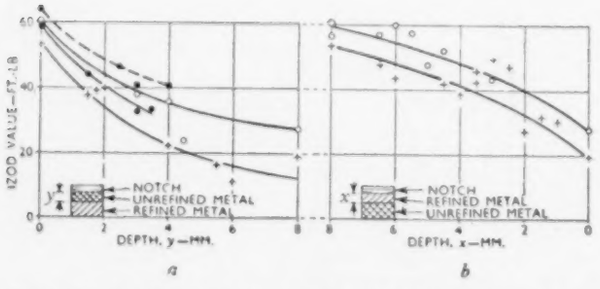


Fig. 3—Effect of Unrefined Metal in an Izod Specimen (Electrode M)

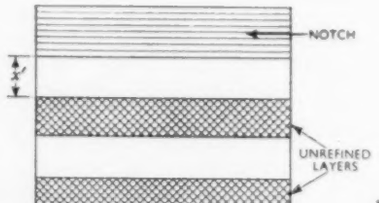


Fig. 4—Relative Positions of Notch and Unrefined Layers

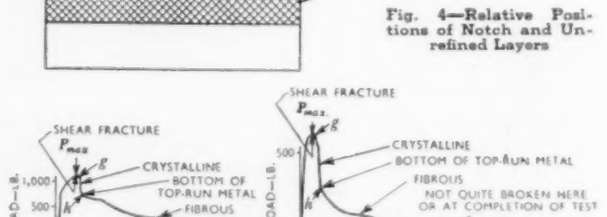


Fig. 5—Weld Metal with a Layer of Unrefined Metal at the Top

(a) Good-quality metal. (b) Low-grade metal for top run, high-grade metal for lower runs

If the notch of a high-class weld lies in refined metal, the curves of Fig. 3 can be used to assess the maximum amount by which the results will be lowered by a sandwich structure. For instance, if one or more strata of unrefined metal are present and the upper surface of the top stratum lies a distance x' from the bottom of the notch (see Fig. 4), the unrefined layers cannot lower the Izod value by more than the decrease obtained from Fig. 3*b*, for which the unrefined metal (situated at a distance x from the notch) extends to the base of the specimen.

The effect of unrefined metal at the surface of a welded joint is shown, for a slow notch-bar test, by the left-hand diagram of Fig. 5, the metal being of good grade. Instantaneous cracking took place through the top run, represented by the sudden fall in pressure at the point *g*. When the crack reached the lower refined metal, i.e., at the point *h*, cracking proceeded gradually. For the diagram at the right the use of an unrefined layer of sound but poor-grade metal, nearly half the net depth of the specimen, caused a similar sudden failure through the top run; the maximum load ($P_{max.}$) and the deflexion attained at this stage were less than before, and the static energy for the complete test fell to 7 ft.-lb., owing to the use of the relatively poor top-run metal; without the top run the refined metal gave an Izod value of about 65 ft.-lb.

THE CAUSES OF CRACKS IN WELDED Aircraft Steels*

By O. WERNER†

SUMMARY

MISUNDERSTANDING will be avoided if, by way of introduction, it is stated that the cracking observed in the gas welding of thin aircraft steel, plain-carbon or alloyed, is distinct from the cracking observed in arc welding thick sections of low alloy structural steel. The following statements hold true for cracks in thin gas-welded steel sheets.

1. The cracks are visible in base metal at the edge of the weld at temperatures of about 700° C.

2. For a given carbon content the probability of cracking increases as the sulphur content is increased. The phosphorus content appears to be of minor significance, if any.

3. The probability of cracking decreases as the temperature at which the steel is refined in the melting furnace is raised.

The temperature of the Ar_3 transformation had no effect on cracking nor was there any difference in tensile strength and ductility at elevated temperatures between steels which cracked and those which did not. It appears that cracking may be ascribed to relatively small amounts of FeO and MnO (approximately 0.01 per cent oxygen) which react with acetylene to form water vapor, which, in turn, cracks the steel. A less pronounced effect of a similar nature is exerted by sulphur in the steel, which forms H_2S. The phenomena are similar to those exhibited in the hydrogen embrittlement of copper.

RESULTS

Tendency to cracking was measured in the rigid clamp test (J. Müller) in which two sheets gripped in a rigid frame are butt welded. The percentage of the length of the weld occupied by cracks is called "crack sensitivity." The specimens were welded by two expert operators whose results were in close agreement. Steels 1 to 14, Table 1, were customary arc-furnace steels. Steels 15 and 16 were laboratory melts. Steels 17 to 32 were made in a basic open hearth furnace using high finishing temperatures and deoxidation by manganese (see P. Kühn, *Westdtsch. Techn. Bl.*, **45** (12), 2–3 (1935), for the process, and P. Bardenheuer and G. Thanheiser, *Mitt. K.-Wilh.-Inst. Eisenforschg.*, **17**, 133–147 (1935), who found that the process yields steel with low total oxygen content).

It is clear that carbon content and method of refining are important factors for crack sensitivity and that sulphur content is less important. However, there was no connection between crack sensitivity and the range of temperature over which the A_3 transformation occurred. It has been argued hitherto that the specially refined steel, which has relatively high manganese content and few inclusions, has a wider critical range than steels refined in the customary way. Hence, the expansion at the critical range occurs at relatively low temperatures at which the steel has improved strength. The explanation cannot be valid, for the hysteresis varied from 105 to 405° C. in steels 17 to 31 with no relation to crack sensitivity.

Static tensile tests at temperatures up to 750° C. showed that sensitive and insensitive steels had prac-

Table 1—Chemical Composition and Crack Sensitivity of the Steels

Method of Steel Production	Steel No.	C	S	Si	Mn	Cr	Mo	Sheet Thickness, Inch	Crack Sensitivity, %
	1	0.27	0.024	0.21	0.46	1.09	0.26	0.039	50
	2	0.29	0.024	0.23	0.62	1.15	0.21	0.032	50
	3	0.29	0.028	0.22	0.60	1.15	0.25	0.032	50
	4	0.24	0.044	0.24	0.48	0.88	0.30	0.020	80
	5	0.25	0.038	0.24	0.47	0.93	0.26	0.059	22
Steels refined in the customary way; mainly electric steels	6	0.24	0.010	0.22	0.56	1.11	0.20	0.059	0
	7	0.28	0.013	0.24	0.57	1.09	0.20	0.059	0
	8	0.25	0.009	0.27	0.59	0.99	0.23	0.039	0
	9	0.22	0.010	0.27	0.59	0.91	0.25	0.039	0
	10	0.26	0.025	...	...	...	...	0.030	25
	11	0.29	0.022	...	...	...	...	0.059	55
	12	0.25	0.021	...	...	...	...	0.039	0
	13	0.34	0.025	...	...	...	...	0.030	45
	14	0.20	0.022	...	...	...	...	0.030	0
	15	0.24	0.024	...	...	...	...	0.039	14
	16	0.21	0.024	...	...	...	...	0.039	0
	17	0.21	0.035	0.19	1.26	...	...	0.039	0
	18	0.26	0.026	0.23	0.90	...	...	0.039	0
	19	0.21	0.036	0.21	1.01	...	...	0.039	0
	20	0.34	0.031	0.21	1.40	...	...	0.039	81
Basic open-hearth steels specially refined by means of deoxidation with manganese	21	0.50	0.028	0.30	1.22	...	...	0.039	61
	22	0.22	0.029	0.21	0.85	0.63	0.46	0.039	0
	23	0.36	0.026	0.28	1.16	...	...	0.039	7
	24	0.37	0.032	0.18	1.21	...	...	0.039	53
	25	0.41	0.039	0.24	0.95	1.02	0.38	0.039	88
	26	0.23	0.024	0.21	0.93	...	0.37	0.039	0
	27	0.41	0.030	0.22	1.18	...	...	0.039	73
	28	0.28	0.037	0.20	1.07	...	0.39	0.039	76
	29	0.33	0.027	0.25	1.18	...	...	0.039	0
	30	0.23	0.023	0.21	0.84	0.89	0.33	0.039	0
	31	0.28	0.026	0.34	1.10	1.05	0.33	0.039	0
	32	0.36	0.034	0.24	0.91	...	...	0.039	0

* Extended abstract of "Ueber die Ursachen der Schweissrissigkeit bei Flugzeugbau-Stählen," published in *Archiv Eisenhüttenwes.*, 12, (9), 449–455, discussion 456–458, March 1939. The paper is a contribution from the German National Materials Testing Laboratories in Berlin-Dahlem, and was presented at a meeting of the Subcommittee on Weldability on Dec. 12, 1938.

† Staatlichen Materialprüfungsamt Berlin-Dahlem, Germany.

ticaly the same strength and ductility. Hence, the cracks cannot be due to reduced capacity for deformation at elevated temperatures.

The belief expressed by Eilender and Pribyl that segregation (banding) increases crack sensitivity was not supported by the evidence. A steel with pronounced banding was insensitive to cracking whereas a similar steel with practically no primary segregation was sensitive. In agreement with Eilender and Pribyl it was found that crack sensitivity of a sensitive steel 0.04 inch thick was reduced to zero by annealing in air for 1 hr. at 1000 or 1100° C. However, the cause of the decrease was decarburization. If the sheets were annealed in a vacuum for 1 hr. at 710°, 860°, 950°, 1060° or 1150° C., which did not change the composition, the crack sensitivity (steels 23 and 24) was not affected. In fact, the higher temperatures increased the crack sensitivity, perhaps because the grain size was increased. Eilender and Pribyl's sheets were annealed a longer time (24 hr.) at a higher temperature (1270° C.). It may be that this severe treatment permitted reaction of FeO and MnO with the silicon and carbon in the steel.

Since the nitrogen content of the steels determined by the Wüst-Duhr method was low and bore no relation to crack sensitivity, nitrogen was believed to have no effect on crack sensitivity.

The oxygen content of the steels, Table 2, was determined by the method of G. Thanheiser and E. Brauns (*Archiv Eisenhüttenwes.*, **9**, 435–439 (1935–36)). Detailed comparison shows little relation between crack sensitivity and total oxygen content. However, the insensitive specially refined steels had 0.009% less oxygen, on the whole, than the sensitive arc-furnace steels. If 0.009 is added to the sulphur contents of the arc-furnace steels in Table 1, their crack sensitivities are practically the same as those of equivalent, specially refined steels. It appears, therefore, that a part of the total oxygen content influences the crack sensitivity.

Total oxygen content includes SiO_2, Al_2O_3, FeO and MnO, of which the first two are difficult to reduce and the last two are easy to reduce by means of hydrogen and carbon. Although Bardenheuer and Bottenberg believed that hydrogen caused crack sensitivity by dissolving in the steel at high temperatures, separating at inclusions, and forcing the steel apart at low temperatures, it is likely that the effect of hydrogen in increasing crack sensitivity is similar to the effect of hydrogen in embrittling tough pitch copper. For example, copper containing 0.03 per cent Cu_2O is cracked by heating in reducing gases at 450° C. It is believed that FeO and MnO can exert similar effects in steel.

It is unlikely that hydrogen initially in the steel is the source of cracking, because at room and elevated temperatures hydrogen diffuses out of steel at a rate that has not been determined. Consequently, the hydrogen must be evolved by the reaction: $6Fe + C_2H_2 \rightleftharpoons 2Fe_3C + H_2$. According to R. Schenck, and coworkers (*Z. anorg. allgem. Chem.*, **164**, 147 and 169 (1927)), equilibrium in the reaction: $3Fe + CH_4 \rightleftharpoons Fe_3C + 2H_2$ is displaced completely to the right at 800° C. A similar result would be anticipated for the iron-acetylene equilibrium.

The atomic or molecular hydrogen that is liberated dissolves as atoms in the iron and reacts with FeO and MnO to form water vapor, which is only slightly dissociated at the zone in the base metal heated to 700–900° C. The water vapor collects at inclusions and forces the steel apart. The oxygen content (FeO and MnO) below which cracking does not occur must be below about 0.01%.

A similar action may be exerted by sulphur, but the degree of dissociation of H_2S at 700–900° C. is greater than that of water vapor. As a result the steel may contain a larger quantity of sulphur (0.05% S at 0.20 C) than oxygen without cracking. Phosphorus and arsenic also may form hydrides during oxyacetylene welding. If the heat of formation in kg.-cal. of the hydride is considered a measure of its dissociability, the following series is formed:

$$OH_2 + 68.38;\ SH_2 + 7.29;\ PH_3 - 5.8;\ AsH_3 - 44.2$$

The permissible contents in steel with 0.2% C are: 0.005% O_2 (as FeO and MnO); 0.04% S; 0.08% P; 0.25% As.

The simultaneous presence of more than one of these impurities reduces the permissible content of each. The permissible contents of O_2 and S are based on present information. Up to 0.05% P has no detectable effect. The value for arsenic was quoted by E. Houdremont, H. Bennek and H. Neumeister (*Archiv Eisenhüttenwes.*, **12**, 91–101 (1938–39)), whose steels, however, contained sulphur and phosphorus.

It would have been desirable to perform fractional oxygen analyses on the steels, but lack of apparatus prevented more than a determination of SiO_2 and Al_2O_3 in the specially refined steels, Table 3, by the residue method using $CuSO_4$ (E. Goutal, *Rev. Métall.*, **7**, 6–10 (1910), and F. Willems, *Archiv Eisenhüttenwes.*, **1**, 655–658 (1927–28)). Apparently the specially refined steels contain neither FeO nor MnO. The literature showed that even in arc-furnace spring steel containing 1.06 Si, 0.014% O_2 was combined with Fe and Mn. An aluminum-killed basic electric steel contained about 0.010% O_2 combined as FeO and MnO. On the whole, electric steels refined in the usual way contained 0.0054–0.015% O_2 combined as FeO and MnO. The conclusion seems to be supported that the difference in total oxygen content, about 0.01%, between the electric and basic open-hearth steels, Table 2, represents the oxygen combined as FeO and MnO.

The present research leads to the following conclusions.

Table 2—Total Oxygen Content of Sensitive and Insensitive Steels

Method of Steel Production	Steel No. (see Table 1)	Crack Sensitivity, %	Total Oxygen Content, %	Average Total Oxygen Content, %
Basic open-hearth steels specially refined by means of deoxidation with manganese.	17	0	0.0093	0.0072
	18	0	0.0127	
	19	0	0.0081	
	20	31	0.0081	
	21	61	0.0080	
	22	0	0.0150	
	23	7	0.0064	
	24	53	0.0077	
	25	88	0.0070	
	26	0	0.0084	
	27	73	0.0049	
	28	76	0.0093	
	29	0	0.0054	
	30	0	0.0072	
	31	0	0.0094	
	32	0	0.0092	
Steels refined in the customary way, mainly electric steels.		30	0.0203	0.0167
		25	0.0284	
		30	0.0224	
		14	0.0273	
		10	0.0115	
		0	0.0085	
		0	0.0127	
		0	0.0093	
		25	0.0100	

Table 3—Determination of the Oxygen Combined as FeO and MnO in Some of the Specially Refined Basic Open-Hearth Steels of Table 1

Steel No.	Total Oxygen Content by Hot Extraction Method, %	Oxygen Combined as SiO_2 and Al_2O_3 by Residue Method, %	Difference Representing Oxygen Combined as FeO and MnO, %
17	0.0093	0.0090	+0.0003
18	0.0127	0.0125	+0.0002
23	0.0064	0.0048	+0.0016
29	0.0054	0.0060	−0.0006
30	0.0072	0.0068	+0.0004
31	0.0094	0.0082	+0.0012

1. The cracks are caused by gaseous H_2O or H_2S which appears in base metal near the weld at high temperatures. The restraint afforded by the rigid clamp test is not requisite for the occurrence of cracks. Possibly cracks are favored by distortions parallel to the weld as a result of non-uniform heating and cooling as the weld progresses.

2. The greater the amount of martensite and troostite in the heat-affected zone; that is, the higher the carbon content, the lower is the capacity of the steel for deformation and the greater is the probability of cracking. On account of loss of carbon, the weld itself has a comparatively low carbon content and a comparatively high capacity for deformation. Therefore, cracks do not occur in the weld.

3. Finishing the steel in the basic open-hearth furnace at high temperatures removes FeO and MnO, if sufficient Si and Al are present. In the special refining process iron and manganese are reduced from the slag in the last stages of refining. As a result the slag contains little FeO and MnO and, on account of the high temperature, is unusually fluid. Fluidity favors attainment of equilibrium and promotes removal of impurities from bath to slag. It should be pointed out that the special refining treatment may be accomplished by any process yielding complete deoxidation and attainment of equilibrium. Small experimental heats have relatively low sensitivity to cracking, because equilibrium is attained more rapidly than in large heats. The atomic hydrogen welding process reduces cracking to a small extent because the heat-affected zone is relatively small.

DISCUSSION

The paper was discussed by W. Eilender and H. Pribyl and by H. Cornelius. The first named believed that the following factors should have been studied or stated:

1. Method of deoxidizing the steels
2. McQuaid-Ehn grain size
3. Reduction of area of transverse specimens (more informative than elongation) in tensile tests at elevated temperatures
4. Better methods for determining oxygen content of steel
5. Better methods than the Oberhoffer etch for detecting segregation, particularly of carbon and alloying elements
6. Higher temperatures for diffusion annealing.

It was believed that the water vapor hypothesis was improbable because a reducing flame is not used in welding aircraft steel. The differences in crack sensitivity of steels 17-32 were explicable in terms of differences in chemical composition alone. A critical test of the hydrogen and segregation hypotheses would be provided by a study of coarse-grained steels free from segregation but containing large quantities of hydrogen.

Dr. Cornelius also found it difficult to accept the hypothesis proposed by Werner. The specially refined steels were characterized by unusually high manganese content, which is known to decrease crack sensitivity. The total oxygen contents reported by Werner for the electric steels are high. Hot-extraction analyses of 10-ton arc-furnace melts of chromium-molybdenum aircraft sheet by the German Aircraft Experiment Station yielded only 0.005 to 0.006% total oxygen.

Experiments have been made on the effect of thin copper coatings on the crack sensitivity of thin sheets. In qualifying some rods for welding completely insensitive sheets of chromium-molybdenum steel 0.04 inch thick, Table 4, it was found that the copper-coated rods caused cracks in the Focke-Wulf rigid clamp test, Table 5, Tests 1 and 4. No cracks appeared if the sheets were not clamped, Tests 2 and 5, nor if the copper coating was removed from the rods by means of emery cloth, and the rigid clamp test was used, Tests 3 and 6. If the sheet was coated with copper by dipping for 5 seconds in copper sulphate solution, cracks appeared whether or not the rod was coated with copper. These and the remaining tests in Table 5 show that thin sheets are sensitive to cracking if, during cooling in the Focke-Wulf rigid clamp test, small amounts of copper come in contact with the stressed sheet. The cracking caused by copper is another instance of intergranular embrittlement of steel by molten metals. The appearance of the cracks differs in no way from the appearance of customary welding cracks in chromium-molybdenum steel.

Rigid clamp tests were made on Sheet II with Rods 3

Table 4—Composition of Rods and Sheets Used by H. Cornelius

	No.	Surface	Diameter or Thickness, Inch	Composition, % C	Si	Mn	P	S	Cr	Mo
Rod	1	Blue annealed	0.059	0.08	0.07	0.5	n.b.	n.b.	..	..
Rod	2	Copper coated	0.079	0.08	0.07	0.5	n.b.	n.b.	..	..
Rod	3	Copper coated	0.059 and 0.079	0.08	0.07	0.4	0.015	0.022	Spur	Spur
Rod	4	Bare	0.059	0.18	0.25	0.6	n.b.	n.b.	0.75	0.16
Rod	5	Copper coated	0.059 and 0.079	0.14	0.25	0.6	0.013	0.004	0.66	0.18
Sheet	I	Bare	0.039	0.25	0.31	0.59	0.015	0.007	1.00	0.21
Sheet	II	Bare	0.079	0.26	0.26	0.59	0.015	0.006	1.05	0.19
Sheet	III	Bare	0.035	0.21	..	0.48	0.023	0.032	0.05	..

n.b. = not determined.

and 5, 0.10-inch diameter. Neither bare nor copper-coated sheets welded with bare or copper-coated rods developed cracks because the sheet was 0.08 inch thick and the cooling of the weld and its vicinity in the clamp was faster than with the thinner sheets I and III. On the other hand, the thicker sheet as a whole cooled more slowly than the thinner sheet. Therefore, tensile stress perpendicular to the weld occurred in the thicker sheet only at temperatures below the melting point of copper. Red shortness due to copper occurs in the Focke-Wulf test only in sheet thinner than a critical value between 0.08 and 0.04 inch. These tests should not be interpreted as militating against the use of copper-coated rods for aircraft welding. Ordinarily the copper from the rod dissolves in the weld puddle. The copper on Rods 3 and 5, on the contrary spattered on the base metal. Probably several instances of cracking in the past that have been attributed to sulphur, in reality were due to copper.

Table 5—Results of Focke-Wulf Rigid Clamp Welding Tests on Materials in Table 4. Tests 2 and 5 Were Made on Unclamped Specimens

Test No.	Rod No.	Rod Diameter, Inch	Sheet No.	Surface: Welding Rod	Surface: Sheet	Cracking in % of: Width of Sheet	Cracking in % of: Thickness of Sheet
1	3	0.059	I	Copper coated	Bare	44, 48, 50, 36	50 to 100
2	3	0.059	I	Copper coated	Bare	0, 0, 0, 0, 0	0
3	3	0.059	I	Copper coating removed	Bare	0, 0, 0, 0, 0	0
4	5	0.059	I	Copper coated	Bare	52, 48, 12	50 to 100
5	5	0.059	I	Copper coated	Bare	0, 0, 0, 0 0	0
6	5	0.059	I	Copper coating removed	Bare	0, 0, 0, 0	0
7	2	0.079	I	Copper coating removed	Bare	0, 0, 0, 0	0
8	2	0.079	I	Copper coated	Bare	0, 0, 0, 0, 0	0
9	2	0.079	I	Copper coating removed	Copper coated	100, 60, 80, 90, 50	30 to 100
10	2	0.079	I	Copper coated	Copper coated	90, 90, 50, 60, 100	50
11	1	0.059	I	Blue annealed	Bare	0, 0, 0, 0	0
12	1	0.059	I	Blue annealed	Copper coated	50, 90,	30 to 50
13	4	0.059	I	Bare drawn finish	Bare	0, 0, 0	0
14	2	0.079	III	Copper coated	Bare	0, 0, 0	0
15	2	0.079	III	Copper coating removed	Bare	0, 0, 0, 0, 0	0
16	2	0.079	III	Copper coating removed	Copper coated	60, 100, 80, 60	30 to 70
17	1	0.059	III	Blue annealed	Copper coated	100, 60, 75, 50, 100	30 to 50

THE STRENGTH OF SPOT WELDS*

SPOT-welded specimens of the type shown in Fig. 1 were tested in static tension to determine the effect of the dimensions of the joint on its strength. The specimens were made of mild steel 0.04 to 0.28 inch thick (tensile strength = 53,000–67,000 lb./in.^{2}). Tests were made on similar specimens joined by plug welds (metal arc process) and by mild steel bolts or rivets. The dimensions of the different joints were varied widely so as to cause failure in each of four fundamental ways:

1. By shear through the inner plate or outer plates.
2. By compression of the metal around the joint, with or without buckling.
3. By tension across the metal on either side of the weld or bolt,
4. By shear through the spot weld or bolt.

The results, Table 1, show that spot welds have an unusually high shear strength, higher indeed than the tensile strength of the unwelded steel. The results for the specimens that failed in shear are plotted in Fig. 2.

* Abstract of Les Soudures par Résistance Électrique; La Résistance Mécanique des Points Soudés et des Tôles Assemblées by E. Warnant, L. Vandeperre, A. Joukoff and P. Lucas; Bulletin No. 2, Laboratoire de Connaissance des Matériaux, University of Brussels, 1939, 85 pp.

Table 1—Stresses (Lb./In.^{2}) at Failure of Spot and Plug Welds and Bolted Joints in Mild Steel

Type of Joint	Type of Failure 1	2	3	4
Spot welded	34,000–38,000	151,000–164,000	92,000–103,000	54,000–95,000
Bolted	29,000–36,000	192,000–215,000	.....	42,000–59,000
Plug welded	29,000–35,000	139,000	60,000– 80,000	40,000–47,000
Riveted	35,000–39,000	.....	.....	47,000–52,000

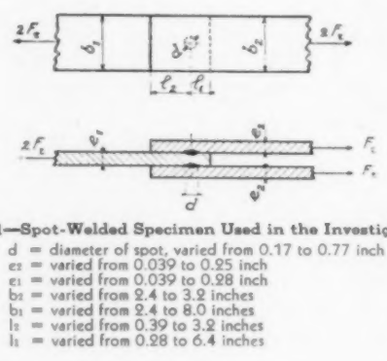


Fig. 1—Spot-Welded Specimen Used in the Investigation

d = diameter of spot, varied from 0.17 to 0.77 inch
e_2 = varied from 0.039 to 0.25 inch
e_1 = varied from 0.039 to 0.28 inch
b_2 = varied from 2.4 to 3.2 inches
b_1 = varied from 2.4 to 8.0 inches
l_2 = varied from 0.39 to 3.2 inches
l_1 = varied from 0.28 to 6.4 inches

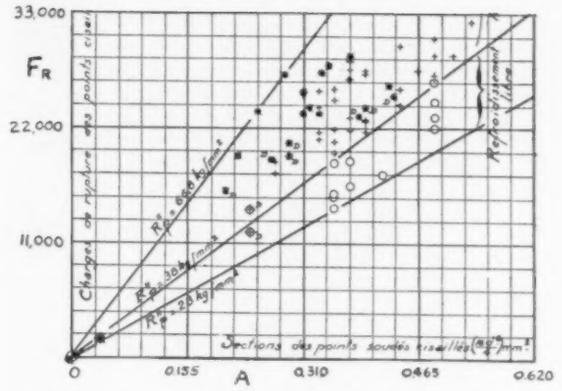


Fig. 2—Tests in Which Joint Failed by Shear Through the Spot

O = pressure released immediately after weld had been made. All others cooled under pressure
D = defective, gas holes or shrinkage cavities on sheared section
F_T = breaking load, lb.
A = cross section of sheared spot = $\pi d^2/4$
d = diameter of spot
Upper diagonal line = 95,000 lb./in.²
Middle diagonal line = 54,000 lb./in.²
Lower diagonal line = 40,000 lb./in.²

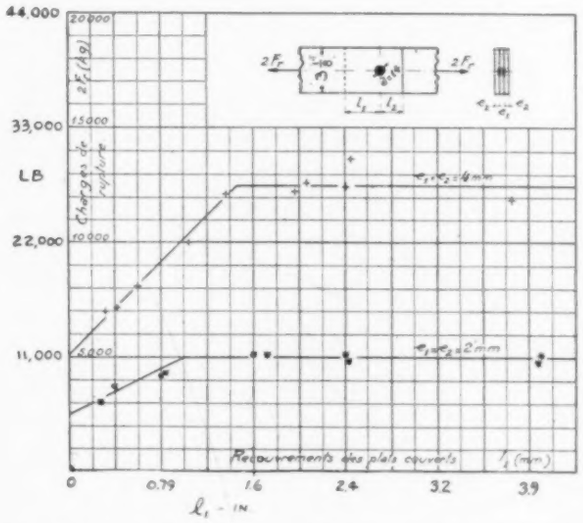


Fig. 3—Effect of Overlap (l_1) on Breaking Load (2 F_T) of Spot-Welded Joints

+ — $e_1 = e_2 = 0.16$ inch
* — $e_1 = e_2 = 0.079$ inch
d = 0.55 inch

In general, the shear strength is proportional to the cross section of the spot, but there is considerable scatter in the experimental values.

Two zones are shown, the lower corresponding to welds that were cooled after release of pressure, and the upper to welds that were cooled under electrode pressure (no details of the electrode pressure, welding time and other factors are given in the Bulletin). The welds cooled under pressure were the stronger. Nevertheless, cooling under pressure is not always effective in preventing gas holes in the weld, especially if the duration of welding is excessive. It is thought that the unusually high shear

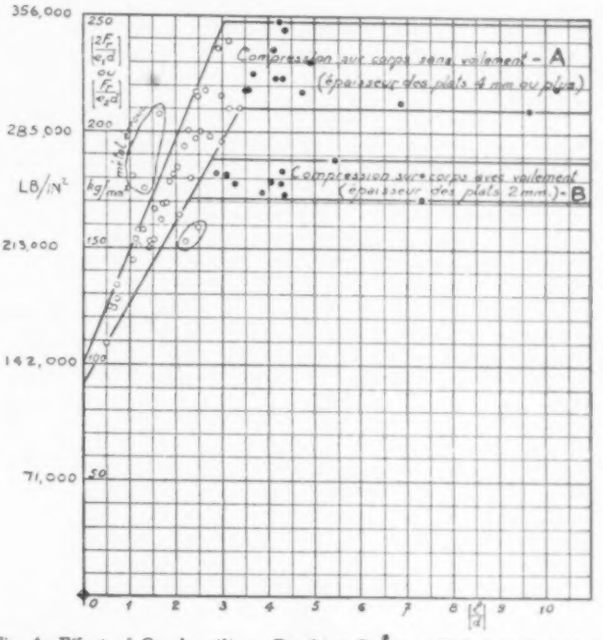


Fig. 4—Effect of Overlap (l) on Breaking Stress (2 $F_T/\pi d$ or $F_T/\pi d$) of Spot-Welded Joints

$2F_T$ = breaking load
d = diameter of welded spot
e_1 = thickness of middle plate
e_2 = thickness of outer plates
A = failure by compression without buckling (plates 0.16 inch thick or more)
B = failure by compression with buckling (plates 0.079 inch thick)

strength of spot welds cooled under pressure is related to internal stresses, as well as to the suppression of pipe and gas cavities.

The conditions governing failure in the plates are illustrated in Figs. 3 and 4. As l_1 is increased, Fig. 3, failure occurs by shear through the inner plate (or outer plates depending on dimension) at increasing loads. Beyond a critical value of l_1 the inner plate (or outer plates) fails by compression at a load independent of l_1 or l_2 and depending only on thickness. If the plates are thin, 0.08 inch in Fig. 4, compression failure is accompanied by crumpling or buckling of the plate. There is no buckling if the plates are thick.

WELDABILITY RESEARCH PROGRAM

The Welding Research Committee of the Engineering Foundation has received requests from a number of sources in the welding industry for information as to the weldability of plain carbon steels of varying carbon and manganese contents.

Some twenty thousand dollars is needed to carry out this program and as soon as financing is complete a commitee will be appointed to formulate details of the program and methods of carrrying it out.

The program will include, in addition to methods of testing for weldability, data for steels throughout a range of carbon and of manganese, on the following:

1. Effects produced in the heat-affected region of the base metal as revealed by changes in hardness, toughness, microstructure and tendency to cracking for (*a*) fusion welding, and (*b*) gas cutting.
2. Variation in the physical properties of welded joints made in the various steels.
3. Variation in the physical properties of steel plates throughout the selected range of chemical analyses.
4. Influence of plate thickness (within a range of 1/4 inch to 1 inch) on items (1), (2) and (3).
5. Influence of important variables in welding procedure, such as preheating temperature, on items (1) and (2).
6. Influence of heat treatment on items (1), (2) and (3).
7. Comparison of the weldability of steels of inherently coarse grained type and inherently fine grain type.

Contributions to this proposed investigation are now being sought.

WELDING TUNGSTEN STEELS

A Review of Literature to July 1, 1937

By W. SPRARAGEN* and G. E. CLAUSSEN†

This report is prepared under the auspices of
THE LITERATURE DIVISION OF THE ENGINEERING FOUNDATION
WELDING RESEARCH COMMITTEE

TABLE OF CONTENTS

	Page
Summary	430-s
Welding Plain Tungsten Steels	431-s
Welding Tungsten High Speed Steel	431-s
Resistance Butt Welding	432-s
Flash Welding	432-s
Oxyacetylene Welding	432-s
Arc Welding	433-s
Atomic Hydrogen Welding	433-s
Other Methods	433-s
Mechanical Properties	434-s
Metallography	434-s
Welding Low Chromium Tungsten Steels	435-s
Welding Ni-W-Cr Steels	435-s
Acknowledgments	435-s
Bibliography	435-s
Suggested Research Problems	436-s

Summary

Welding Plain Tungsten Steels

Practically no tungsten is lost in passing through the arc in welding. The manual or automatic flash welding of a tool steel containing 0.99 C, 0.76 W presented no difficulties. The critical cooling velocity of the steel determines whether martensite will appear. The higher the welding temperature, rate of cooling, and carbon and alloy content, the greater will be the amount of retained austenite. If the alloy steel is welded to a plain carbon steel, the alloy steel may harden but the plain carbon steel may consist of sorbite or pearlite in the heat-affected zone.

The oxygen cutting of tungsten steels is not difficult, if the tungsten content is not above 10% with or without 5% Cr at 0.8% C. Steel with 17% W can be oxygen cut only if it is preheated and the oxygen pressure is increased. Steel with over 20% W cannot be oxygen cut even with preheat.

Welding Tungsten High Speed Steel

Resistance Butt Welding.—The chief application of resistance butt welding to high speed steel is the welding of high speed steel tips to plain carbon or alloy steel shanks in the preparation of lathe and other tools. The butt-welded tools perform as well in service as tools forged entirely from high speed steel.

High speed steel requires more heat than mild steel to reach the welding temperature. In addition, high speed steel has a high electric resistivity. Consequently, the high speed steel should project further from the grips. The upset on both sides of the weld must be uniform. The tool may be annealed after welding or may be heat treated immediately after welding and without cooling in order to prevent cracking of the high speed steel.

Oxyacetylene Welding.—The chief difficulty in oxyacetylene welding high speed steel is cracking, to avoid which the steel must be preheated bright red.

Arc Welding.—One authority recommends preheating to 100–200° C. (200–400° F.). The beads are woven the full width of the surface to be built up. The finished tool should be reheated (2 hr. per inch at 540° C. (1000° F.)) and cooled in still air after welding for maximum hardness and toughness. If high speed steel is applied to high-sulphur, free machining steel the deposit will be porous. Deposits on low-sulphur steel are not porous.

Atomic Hydrogen Welding.—In atomic hydrogen welding tool steels containing 0.7 C, 0.24 Si, 0.2 Mn, 5 Cr, 22 W, some experts first annealed the steel at 850° C., then preheated it to 800–900° C., and welded it with a rod of the same composition.

Other Methods.—Another process consists of inserting a strip of copper between the mild steel and high speed. The joint is heated to hardening temperature, about 1200° C., in a reducing atmosphere. A strip of brazing metal or copper 0.04 inch thick with or without borax need simply be placed between the parts which are held together by wire. Copper- or bronze-welded high speed steel cannot be heat treated. In forge welding, the bright high speed surface is coated with borax and brought to a white heat; a mixture of borax and iron filings is sprinkled over the surface after which the iron shank is hammer welded to the high speed.

Mechanical Properties.—A resistance welded high speed steel containing 0.60–0.70 C, 0.25 Si, 0.25–0.35 Mn, 17–18 W, 3.75–4.25 Cr, 0.90–1.10 V had a tensile strength of 104,000 to 123,000 lb./in.^{2}; the unwelded steel had a tensile strength of 115,000 lb./in.^{2}

Welding Low Chromium Tungsten Steels

Low chromium-tungsten steels are widely used as filler rods for hard facing by arc or oxyacetylene processes. The hardness of the deposit may be 435 to 580 Brinell. To get good penetration in welding, the electrode should have a higher melting point than base metal. The reverse is true in surfacing, for which a bare electrode operated on the positive pole and containing 1.10 C, 0.30 Mn, 0.20 Si, 1.5 W, 1.0 Cr was found to be best. Deposited on rails the weld metal hardens to 500 Brinell, but is brittle. For building up rail ends of a wide variety

* Secretary, Welding Research Committee.
† Research Assistant, Welding Research Committee.

of compositions by the oxyacetylene process, some authors favored a rod containing 1.2 C, 0.9 Cr, 1.7 W, 0.2 Mn, 0.12 Si. A reducing flame was better than neutral because there was less danger of overheating.

WELDING PLAIN TUNGSTEN STEELS

Since tungsten is used almost entirely in tool steels, the effect of tungsten on the welding of steel has not been studied extensively. Tungsten has a high specific gravity: 19.3, and a high melting point: 3400 ± 50° C. In air pure tungsten oxidizes at 600 to 700° C.; the highest oxide evaporates at atmospheric pressure at about 900° C. Low-tungsten steels have a reputation for rapid decarburization at high temperatures, despite the fact that a tungsten-iron carbide is formed. Above 1% W the upper critical point of steel is rapidly raised. Like most alloying elements, tungsten decreases the thermal and electric conductivity of steel, which affects the welding of high speed steel to which a separate section is allotted in this review. The effect of tungsten on the welding of stainless steels is dealt with in the review of literature on those steels. Hard facing and the welding of non-ferrous tool materials, such as stellite, are not discussed in the review.

Older writers[1] were of the opinion that tungsten injured the forge welding capacity of steel. For that matter every other alloying element overcame the enterprise of some of the old-time forge welders. Howe[2] could find no one who could forge weld steels containing 1.6 C, 2.11 Mn, 6.38 W or 1.99 C, 0.19 Mn, 7.81 W, although both steels could be resistance butt welded without difficulty. On the other hand, Burgess and Aston[3] stated that iron containing up to 20% W could be forge welded and resistance butt welded. Forge welds were also made in steels containing 5 to 15 W, 0.4 to 0.6 C, and 0.4 C, 3 Mo, 13 W. Lebrun[4] used an electrode for tipping tools that deposited steel containing 0.3 C, 2.9 Si, 1.6 Cr, 0.4 Mo, 8.0 W. According to Kautny,[5] tungsten tends to segregate in welds on account of its high specific gravity. Yet tungsten has no special tendency to segregate in any kind of tungsten steel ingots.

The recovery of tungsten in arc welding with a bare electrode 0.16 inch diameter is nearly complete, according to Dahl and Sandelowsky,[6] Table 1. Although carbon is the main factor in hardness, tungsten also increases the hardness. Table 2 summarizes the results of Losana and Jarach[7] on the recovery of tungsten from coated mild steel electrodes (for details see Table 24, review of literature on Welding Nickel Steels, American Welding Society Journal, **17** (5), 1-17 (1938). The ferrotungsten contained 74.86 W, 0.84 Mn, 1.34 Si, 1.88 C, 0.042 P, 0.038 S. On account of its high specific gravity, the ferrotungsten had to be finely ground to insure its uniform distribution in the coating. The recovery of tungsten was nearly complete unless the carbon content of the coating was high. Manganese had little effect on recovery of tungsten.

Table 1—Recovery of Tungsten from a Bare Electrode. Dahl and Sandelowsky[6]

	C	Si	Mn	W
Electrode	0.94	0.06	0.24	1.7
Deposit	0.70	trace	0.08	1.5 (380 Brinell)

The manual or automatic flash welding of a tool steel containing 0.99 C, 0.76 W presented no difficulties to Merz and Eschelbach,[8] whose welds consisted of lamellar pearlite (no details). They found as a result of their experience in welding tool steels that the critical cooling velocity of the steel determines whether martensite will appear. The higher the welding temperature, rate of cooling, and carbon and alloy content, the greater will be the amount of retained austenite. If the alloy steel is welded to a plain carbon steel, the alloy steel may harden but the plain carbon steel may consist of sorbite or pearlite in the heat-affected zone. Their conclusions are verified in the following section on High Speed Steel.

The oxygen cutting of tungsten steels is not difficult, according to Wiss,[9] if the tungsten content is not above 10% with or without 5% Cr at 0.8% C. Steel with 17 W can be oxygen cut only if it is preheated and the oxygen pressure is increased. Steels with over 20% W cannot be oxygen cut even with preheat. Pure tungsten must be preheated to a very high temperature before it can be flame cut (no details). Firth and Knox[10] were able to oxygen cut a steel containing 0.30 C, 10 W with great difficulty.

WELDING TUNGSTEN HIGH SPEED STEEL

Information on the welding of tungsten high speed steel (0.7 C, 18 W, 4 Cr, 1 V) is confined almost entirely to descriptions of methods of welding the steel to plain carbon steel. Among these methods, resistance welding has received the most attention.

Table 2—Recovery of Tungsten from Coated Mild Steel Electrodes. Losana and Jarach[7]

No.	(Mn)	(Si)	(C)	(W)	(W)[1]	[Mn]	[Si]	[C]	[W]	[W]/(W)[1]
1	0.28	1.96	3.82	11.92	4.7	0.05	0.05	0.08	4.36	0.94
2	0.42	2.21	3.94	21.54	8.5	0.12	0.14	0.17	8.94	1+
3	0.66	2.40	3.72	29.86	11.9	0.18	0.17	0.22	11.22	0.94
4	0.84	2.68	3.64	38.94	15.5	0.31	0.34	0.31	14.46	0.93
5	4.96	2.02	3.46	11.50	4.2	0.50	0.09	0.13	5.12	1+
6	5.10	2.27	3.62	20.78	8.3	1.04	0.28	0.28	10.08	1+
7	5.21	2.70	3.58	37.52	14.9	1.09	0.42	0.73	17.84	1+
8	9.86	2.00	3.54	11.38	4.5	1.50	0.12	0.20	5.02	1+
9	10.21	2.33	3.72	20.54	8.2	1.49	0.47	0.26	7.98	0.98
10	9.73	2.46	3.56	29.80	11.8	1.55	0.51	0.64	10.32	0.87
11	4.82	2.10	7.48	11.08	4.4	0.92	0.42	0.37	3.94	0.90
12	4.73	2.31	7.31	20.31	8.1	0.99	0.51	0.42	7.31	0.90
13	4.90	2.48	7.62	29.21	11.6	0.98	0.63	0.44	8.02	0.69
14	10.36	1.98	7.43	10.82	4.3	1.36	0.33	0.33	3.18	0.74
15	10.02	2.19	7.50	19.94	7.9	1.56	0.47	0.51	6.21	0.79
16	9.88	2.51	7.26	28.90	11.5	1.61	0.70	0.56	7.68	0.67

( ) = weight % constituent in coating.
[ ] = weight % constituent in deposit.
(W)[1] = percentage of tungsten calculated on basis of core rod of electrode.

Resistance Butt Welding

The chief application of resistance butt welding is the welding of high speed steel tips to plain carbon or alloy steel shanks in the preparation of lathe and other tools. The butt-welded tools perform as well in service as tools forged entirely from high speed steel, as Sanderson[11] recently pointed out.

As Ober[12] points out, high speed steel requires more heat than mild steel to reach the welding temperature. In addition, the high speed has a high electric resistivity. Consequently, the high speed should project farther from the grips. The upset on both sides of the weld must be uniform. If either steel has telescoped into the other, welding has not been correctly done. The weld must be furnace cooled. In common with all other investigators, Ober states that fracture of the joint under load occurs in the heat-affected zone of the high speed steel. In order to force out impurities from the surfaces to be welded, Seibert[13] recommends preheating under pressure almost to the fusion point before applying push-up. By gripping the high speed steel close to the weld in a copper die, a German welder[14] overcomes the low electrical resistance of the high speed and reduces the depth of the heat-affected zone.

Viall[15] emphasized the fact that no flux is required. The plain carbon steel should project twice as far from the jaw as the high speed. He illustrates grooving technique for large welds. Tables of areas, power and time for butt (and spot) welding were prepared by *Welding Engineer*[16] (1916). The tool should be heat treated immediately after welding and without cooling in order to prevent cracking of high speed. According to J. P. Gill (private communication, April 1939) it is not customary to heat treat the tool immediately after welding. Usually the tools immediately after welding are returned to a furnace maintained at about the annealing temperature for the steel involved. The tools are usually allowed to remain in this furnace until such time as the furnace becomes loaded or the day's work has been completed, at which time the furnace is slowly cooled to anneal the material. The material is then heat treated in the regular manner. The cracking, which occurred in the high speed steel a short distance from the weld, was believed by Thum[17] to be due to shrinkage stresses, which could be eliminated by annealing the tool immediately after welding and cooling in lime before heat treating. There was an alloy zone 0.0006 inch thick at the junction of the high speed steel tip with the plain carbon steel shank.

The switching off of current at intervals to permit equalization of temperature between plain carbon and high speed steel was suggested by Heaton.[18] The push-up allowance in resistance butt welding high speed to 0.7% C steel is $^5/_{16}$ to $^7/_{16}$ inch for drills up to $^3/_4$ inch diameter, $^5/_8$ inch for drills $1^1/_2$ inch diameter, according to *Industry & Welding*,[19] who believed the plain carbon steel should project only slightly farther beyond the die than the high speed. After welding and without cooling, the drills are straightened at 800–820° C. and flutes are forged at 1200° C. The *AEG*[20] believed that the high speed steel should be annealed before resistance butt welding in order that the weld can be ground.

The time occupied in resistance butt welding high speed to plain carbon steel was found by Kollbrunner[21] to be $1^1/_2$ minutes for parts 0.47 inch diameter, 15 minutes for parts 0.39 x 1.97 inches. The loss by burning is 0.04 to 0.16 inch and the weld should be annealed at 800° C. immediately after welding. Special resistance butt-welding machines[22] have been designed for welding high speed tips to plain carbon shanks. The economics of the process was dealt with by Hartmann,[23] among many others.

Steele and Martin[24] (1921) stated that the Cyc-Arc stud welder can weld high speed to itself, manganese bronze or plain carbon steel (porous weld in mild steel). The depth of the heat-affected zone was 0.04 inch in the high speed steel.

Flash Welding

In his useful article on flash welding high speed to plain carbon steel, Eckman[25] states that a flash welder, a lead pot at 840° C. (1550° F.) and a box filled with mica flake are necessary. The high speed is heated in the lead pot for a distance of $1^1/_2$ to 2 inches back of the surface to be welded and is cooled in the mica. The mild steel shank is preheated dull red in the pot in order to offset its lower electric resistance and to act as a reservoir of heat to prevent too rapid cooling after welding. The welded tool is placed in the lead pot 1 inch beyond the weld on the high speed section and cooled in mica. Flash is controlled by the operator; no quantitative information is available.

Croskell[26] observed that fracture of drills made of high speed flash welded in an incorrect way to plain carbon steel shanks always occurred in the high speed steel outside the fusion zone but in the heat-affected zone when the drills were dropped from a height of 3 feet on an iron plate. The difficulty was overcome by sawing instead of shearing the surfaces of the high speed to be welded, by using a finishing temperature above 900° C. in hot rolling the high speed, and by heat treating the weld (no details). Sauer[27] also observed fracture in the heat-affected zone of high speed flash welded to plain carbon steel. Eyles[82] stated that the mild steel should project twice as far from the die as the high speed and that the length of metal flashed away is about one-half the diameter. With a 40 kw. machine, welds in cross sections of 0.3, 1.0 and 2.0 sq. in. can be made in 5, 15 and 30 seconds, respectively. Otto[28] mentioned having flash welded iron to high speed steel.

Oxyacetylene Welding

Biernacki[29] used a plain carbon steel filler rod for oxyacetylene welding diamond-shaped high speed steel tips (0.7 C, 4.4 Cr, 1.3 V, 0.7 Mo, 20 W) to plain carbon steel lathe tool shanks. The welds contained blow-holes. Walker[33] recommended a reducing flame and warned against the heavy tungsten and chromium oxides (no details) which usually form. A rod with free flowing characteristics and containing vanadium and cobalt was used with flux. The rod was deposited in thin layers and was melted into base metal, not added drop by drop. To prevent drop deposits the scarf angle should be at least 90°. Vincent[31] showed that an oxidizing flame produced porous deposits with an 18–4–1 filler rod. Two Russian investigators[81] found that a neutral flame yielded porous deposits, and a highly reducing flame produced brittle, high carbon deposits in surfacing tools with high speed steel rods containing 0.59–1.3 C, 18 W, 3.5–3.8 Cr, 0.25 V. The deposit was forged, then heat treated to 62 C Rockwell.

A tip 1 or 2 sizes smaller than for mild steel gave good results.[31] In 1916 McKernan[32] preheated the high speed tips to 1000–1100° C. (1800–2000° F.) before welding them to the plain carbon shank with a Norway iron filler rod. Among the methods described by Viall[33] (1921) was one involving the coating of the high speed with a heavy layer of special soft iron before welding. Burning had to be guarded against. Another method utilized a 3% nickel steel rod without flux. *Welding Engineer*[34] used the nickel steel rod for building up a layer of weld metal $^1/_8$ inch thick on the high speed. Cast-iron flux was necessary for the surfacing but was not

employed during subsequent welding with the nickel steel rod. C. G. Bainbridge (private communication, March 1939) found that the rod containing 3–3½% Ni was not so satisfactory as a plain-carbon steel rod containing 0.8–1.0 C, 0.3–0.6 Mn for shanks up to 1 x ⅝ inch, without cracking. The neutral flame should be directed on the mild steel shank to avoid overheating. The scarf should be double Vee, and the welds should be made as quickly as possible. After being welded, the joint is hammered at 800° C., then reheated to 950° C. in a furnace and cooled in lime.

Two methods of welding high speed to mild steel were suggested by Dawson. In one method,[35] a carborundum mold was built around the mild steel part and a high speed rod was used. The carbides were found to be uniformly distributed in the weld. In the other method,[36] the high speed steel rod is melted into a slot cut into the end of a low carbon steel shank. The deposit formed the cutting edge. There was some loss of Si and Mn during welding. According to Miller,[37] the chief difficulty in oxyacetylene welding high speed steel is cracking, to avoid which the steel must be preheated bright red. Bliss[38] and Brown[39] both state that stellite and most hard facing alloys can be applied to any steel with the exception of high speed steel, which cracks.

Arc Welding

M. C. Smith[40] recommends an electrode containing iron, carbon, silicon, manganese, chromium and vanadium, but no tungsten for welding high speed steel. The weld is made from both sides if the plate is over ¼ inch thick. The weld metal picks up tungsten from base metal (no details). The Lincoln Procedure Handbook for Arc Welding[41] recommends preheating to 100–200° C. (200–400° F.). The beads are woven the full width of the surface to be built up. The finished tool should be reheated (2 hr. per inch at 540° C. (1000° F.)) and cooled in still air after welding for maximum hardness and toughness. If high speed steel is applied to high-sulphur, free machining steel the deposit will be porous. Deposits on low-sulphur steel are not porous. No details of the electrode are given. Lapidus[80] avoided cracks in hard-facing mild steel with high speed steel electrodes by preheating the mild steel to 800° C. and returning it to the furnace immediately after facing. *Welding Engineer*[42] used a Norway iron electrode to weld high speed to plain carbon steel, and first coated the high speed with soft iron. For the same purpose Auel[43] used an electrode containing 0.05 C, 0.10 Mn, 0.01 Si.

Iron core electrodes with a coating adjusted to produce a high speed steel deposit were described by Smith[44] (1926), Lacy[45] (1929), and Haramiishi[46] (1930).

Atomic Hydrogen Welding

In atomic hydrogen welding tool steels containing 0.7 C, 0.24 Si, 0.2 Mn, 5 Cr, 22 W, Goleniewicz and Cegielski[47] and Rosenberg[48] first annealed the steel at 850° C., then preheated it to 800–900° C., and welded it with a rod of the same composition. Blow-holes in preceding passes must be filled by later passes, which makes the welding tedious. The welds were annealed and then carburized, because the weld metal contained 0.36 C, 0.25 Si, 0.18 Mn, 4.3 Cr, 23 W. The welds had a fine dendritic structure and were free from oxides and porosity. Repairs in tool steel containing 0.8–0.9 C, 3.5–4.5 Cr, 1.2 V, 21 W were also made by Goleniewicz[49] using atomic hydrogen welding. A hardness of C 62 Rockwell was obtainable in the welds, and micrographs revealed perfect junction. Up to 10% Co had no effect on atomic hydrogen welding, according to Rosenberg. Annealed steel containing 0.6 C, 18 W was atomic hydrogen welded by Weinman[60] without preheat. Serata[61] used the atomic hydrogen torch to deposit high speed steel (18–24 W, 3–6 Cr, 1–15 Co, 0.5–2.0 Mo, 0.6–3.5 V, with or without 1.5–2.0 Zr) on worn tools. The optimum thickness of deposit for cutting efficiency was 0.08 inch.

Other Methods

The press welding of high speed to plain carbon steel has often been described. The high speed is heated to 900° C., according to Oertel and Grützner,[52] wire brushed to remove scale and covered to a depth of ⅛ inch with a mixture containing 2000 gm. borax, 4000 gm. iron powder, 100 gm. aluminum powder or MnO_2. The two parts to be welded are placed together and are heated until the flux melts. They are then squeezed in a vise and cooled in air, which provides the requisite hardness. Hammer blows instead of vise pressure would cause cracks. The same procedure is described by Pattermann[53] for high speed steel containing 10% cobalt, who uses a flux containing 42½% borax, 8½% Na HCO_3, 8½% Na_2CO_3, 8½% NH_4Cl, 21% iron filings free from rust and oil, and 11% ferrosilicon, and heats the parts 4 min. at 1250° C. until the iron filings sparkle. Like Pattermann, Snelling and Richards[54] stated that cobalt high speed is nearly always used as small plates welded to the tips of carbon steel shanks. It is unusually difficult to avoid cracks in heat treating tools made completely of cobalt high speed steel. In contrast with usual practice, shank and tip are heated together with a filling of welding material to 820–830° C. in a preheating furnace. After being preheated, the tool is heated to 1250–1300° C. and squeezed in a vise until the temperature has fallen below the melting point of the welding material, which consists of borax mixed with powdered manganese steel, silicon steel and high speed steel. Upon removal from the vise, the tool is cooled to 90–100° C., which hardens it, and is tempered at 500–600° C. for 2 hr. In 1916 Lutz[58] described essentially the same process. A German welder[56] recommended the removal of the joint from the vise at 1000 to 1100° C. The plain-carbon steel shank should have a tensile strength of 100,000 lb./in.², according to Vorbach.[83]

In Highduchek's[57] process of welding plain carbon steel shanks (0.50–0.63 C, 0.60–0.90 Mn, 0.15 Si, 0.04 P) to high speed, cobalt high speed or Stellite J or No. 3, a seat is drop forged in the shank upon which special flux is spread. The tip is placed on the flux and preheated to 850–875° C. (1550–1600° F.). The tip is then pressed firmly into its seat and rapidly heated to 1230–1320° C. (2250–2400° F.), at which pressure is applied to force out the slag. The tool is cooled in an air blast or oil for hardening from welding heat. After being drawn at 560–620° C. (1050–1150° F.) the tool is C 63 Rockwell.

Another process[1,58] consists of inserting a strip of copper between the mild steel and high speed. The joint is heated to hardening temperature, about 1200° C., in a reducing atmosphere. Kurrein[59] found that the necessary pressure in the copper brazing process is 5700 lb./in.², but other writers[60] showed that pressure was not necessary. A strip of brazing metal or copper 0.04 inch thick with or without borax needs simply be placed between the parts which are held together by wire. The flux used by Simpson[61] consisted of charcoal, brown sugar and water, the process being used to produce armor plate consisting of mild steel surfaced with high speed steel. Micrographic examination showed that the copper diffused completely into the steels. Kurrein[59] pointed out that the copper brazed tool cannot be reheat treated.

A number of special fluxes[62] that were proposed for welding high speed to mild steel at 1100° C. probably depended on brazing metal. A micrographic study of copper brazed joints of high speed to mild steel by Burstyn[63] showed that the copper may not alloy with the two steels or it may alloy completely, depending on the process (no details). Taylor[79] embedded high speed steel teeth in plain-carbon steel milling cutters by means of type metal.

The forge welding of high speed to plain carbon steel is described by Drescher[64] and Mars.[1] The bright high speed surface is coated with borax and brought to a white heat; a mixture of borax and iron filings is sprinkled over the surface after which the iron shank is hammer welded to the high speed.

Micrographs of Armstrong welds[65] made by electroplating a layer of electrolytic iron on pickled high speed (0.68 C, 17.53 W, 3.96 Cr, 1.05 V) and plain carbon steel (0.10 C, 0.47 Si, 0.36 Mn) followed by clamping together and hot forging or rolling showed considerable diffusion of carbon into the iron, particularly after oil quenching from 1290° C. (2350° F.). There was even greater carbon diffusion in a similar weld in Co-Mo high speed (0.77 C, 4.16 Cr, 17.27 W, 1.77 V, 11.16 Co, 0.93 Mo). There was no trace of burning in the hardened and drawn weld in which the backing metal was a low alloy steel containing 0.60° C. Grimshaw found two medium-carbon steels, one a Mn-Mo, the other a Cr-W-V, that are not damaged by the hardening treatment for high speed steel. No pressure is required to weld the electroplated iron to the high speed steel. At 925° C. (1700° F.) diffusion is rapid. In rolling the assembly of backing metal and high speed steel, both electroplated with iron, the weld zone is an alloy steel, responding to heat treatment, rather than electrolytic iron, on account of diffusion. In tension tests fracture occurs in the low alloy steel, not in the weld zone. P. A. E. Armstrong also states (private communication, March 1939) that the electroplated backing metal and high speed steel can be resistance welded under pressure if the surfaces are aligned fairly close. After the composite high speed steel has been rolled, it is cooled slowly in ashes in the same way as high speed steel.

The thermit welding of high speed steel tips $^3/_4$ x 2 x 6 inches long to machinery steel wheel lathe tool shanks is described by Martin.[66] Viall[23] describes a number of similar applications.

Mechanical Properties

According to Eckman,[67] a resistance welded high speed steel containing 0.60–0.70 C, 0.25 Si, 0.25–0.35 Mn, 17–18 W, 3.75–4.25 Cr, 0.90–1.10 V had a tensile strength of 104,000 to 123,000 lb./in.^{2}; the unwelded steel had a tensile strength of 115,000 lb./in.^{2} Vincent[31] found that deposits from a bare electrode containing 0.69 C, 0.13 Mn, 0.165 Si, 3.94 Cr, 18.2 W, 0.99 V, which were allowed to cool in air had a Brinell hardness of 577 to 640. Lathe and planer tests on built-up tools were fairly satisfactory. According to P. A. E. Armstrong (private communication, March 1939), a tool consisting of a high speed tip welded to a backing metal of different composition, such as low-alloy steel, often outlasts tools made wholly of high speed steels, because the damping effect of the composite tool is greater.

Metallography

Merz and Eschelbach[8] had difficulty flash welding steel containing 0.67 C, 3.67 Cr, 0.46 V, 17.06 W. The cause was traced to the eutectic which was found in the welds and could not be converted by heat treatment. A flash weld between this steel and plain carbon steel (0.82 C) resulted in a zone of grain boundary penetration of high speed into plain carbon steel that was less than 0.04 inch wide. The intergranular material was rich in alloying elements and remained martensitic after cooling, whereas the plain carbon steel became "sorbitic." Heat treatment of the weld was difficult because the hardening temperature of the high speed burned the plain carbon steel.

Jevons and Wheeler's[68] study of a resistance butt weld between high speed and mild steel disclosed globules of oxide (burning) in the mild steel and penetration of high speed steel along the grain boundaries of the mild steel, much the same as in Merz and Eschelbach's specimens. Drescher[64] also observed intergranular penetration in similar resistance butt welds. According to J. V. Emmons (private communication, September 1939), there should be no burning of plain carbon and low alloy steel shanks when hardening the welded high speed steel tips. There is a certain degree of overheating which weakens the shank steel somewhat, but may not render it useless. Overheating is more pronounced with the 18% tungsten types of high speed steel, which are hardened at about 2375° C., than with the molybdenum—tungsten types, which are hardened at about 2200° F.

In 1919 Czako[69] studied the microstructure of welds between a steel containing 9 W, 4 Mo, 4 Cr and steel of the same composition or (1) 12% Cr steel, (2) 12% Mn steel, (3) 0.5 Cr, 3 Ni steel, (4) 1 Cr, 2 Si steel, (5) 1.6 C steel, (6) gray cast iron. The parts to be welded were heated to a red heat in a coke fire, brushed and sprinkled with a few grams of special flux not containing iron or borax. A pressure weld was made after the flux melted. In all welds, except the 12% Cr steel weld, there was scarcely any difference between the structure of weld and base metal, but welding appeared to be complete.

Table 3—Composition and Hardness of Deposits on Mild Steel Made with Cr-W Filler Rods. Zeyen[75]

Method of Welding	Number of Superposed Beads	Composition of Deposit C	Si	Mn	Cr	W	Brinell Hardness One Bead	Three Superposed Beads: Beads Deposited in Rapid Succession	Three Superposed Beads: Each Bead Allowed to Cool Completely
Unwelded rod	..	1.12	0.28	0.84	1.09	1.50			
Oxyacetylene	1	0.75	0.18	0.74	1.01	1.41	345	451	477
Oxyacetylene	3	0.92	0.21	0.76	1.07	1.49			
Bare electrodes (d.c.)	1	0.55	0.04	0.43	0.81	1.02	285	363	383
Bare electrodes (d.c.)	3	0.68	0.10	0.55	0.96	1.44			
Coated electrodes (d.c.)	1	0.56	0.14	0.71	0.83	1.34	373	477	507
Coated electrodes (d.c.)	3	0.70	0.18	0.78	0.97	1.46			
Coated electrodes (a.c.)	1	0.60	0.18	0.76	0.90	1.38	381	451	528
Coated electrodes (a.c.)	3	0.71	0.25	0.82	1.02	1.50			

In the 12% Cr steel weld there was a broad zone of different structure from either base metal.

WELDING LOW CHROMIUM TUNGSTEN STEELS

Low chromium tungsten steels are widely used as filler rods for hard facing by arc or oxyacetylene processes, as Sommer[70] points out. The hardness of the deposit may be 435 to 580 Brinell. According to Tewes,[71] to get good penetration in welding, the electrode should have a higher melting point than base metal. The reverse is true in surfacing, for which a bare electrode operated on the positive pole and containing 1.10 C, 0.30 Mn, 0.20 Si, 1.5 W, 1.0 Cr was found to be best. Deposited on rails the weld metal hardens to 500 Brinell, but is brittle. Hoffmann[72] agreed with Tewes and found that if the electrode is deposited on 14% Mn steel, the junction zone remains austenitic, the hardness being 225–281 Brinell. When the electrode is deposited (130 amp. for 0.16-inch electrode) on rails containing 0.65 C, 0.45 Mn the hardness is 339 to 428 Brinell. For building up rail ends of a wide variety of compositions by the oxyacetylene process, Gorthon[73] and Golling[74] favored a rod containing 1.2 C, 0.9 Cr, 1.7 W, 0.2 Mn, 0.12 Si. A reducing flame was better than neutral because there was less danger of overheating.

The recovery of alloying elements from Cr-W filler rods was shown by Zeyen[75] to be very high, Table 3. The rods were 0.20 inch diameter. The surface was lightly ground for the hardness measurements. It was found that the recovery of carbon was higher with the Cr-W rod than with plain carbon steel rods of the same initial carbon content. The single beads of bare electrodes did not show as good recovery as triple beads. Some Cr and W were lost in the slag, some in dilution with underlying mild steel. The dilution factor was negligible in the three layer deposits. Zeyen[76] also showed, Table 4, that recovery is slightly better if the electrode is negative than positive. The recovery was better with coated electrodes a.c. than d.c. The three layer deposits were much harder than the single beads. Cooling each layer before depositing another increases the hardness. The deposits appeared to have a martensitic structure, the grain structure being finer in the arc deposits than in the oxyacetylene. Recoveries reported by other investigators are summarized in Table 5. The great difference in results may be due to the fact that Rapatz deposited his electrode on a copper plate to avoid dilution effect. Observation of a steel containing 0.50 C, 2.50 W, 1.25 Cr while an atomic hydrogen torch was melting a path across the surface convinced C. A. Liedholm (private communication, April 1939) that the steel was fluid, and displayed no tendency to undercut, to evolve gases, to eject sparks or to crack.

The microstructure of flash welds in a steel containing 0.34 C, 1.15 Cr, 3.23 W, 0.76 Si consisted of coarse martensite, according to Merz and Eschelbach.[8] Small cracks were observed especially where decarburization by oxides had occurred. Annealing 6 hr. at 750° C. produced a fine pearlitic structure with an angular appearance suggesting its origin in the initially martensitic structure.

Table 4—Effect of Polarity on Recovery from Bare Cr-W Electrodes (150–155 Amp.) Zeyen[76]

	Electrode	Positive Pole	Negative Pole
C	1.11	0.52	0.62
Si	0.11	0.05	0.06
Mn	0.32	0.16	0.17
W	1.60	1.52	1.55
Cr	0.8	0.6	0.6

Table 5—Recovery from Bare Cr-W Electrodes

	Electrode	Deposit	Electrode	Deposit
C	1.00	0.61	1.19	0.97
Si	0.15	0.074	0.18	0.15
Mn	0.30	0.26	0.40	0.34
W	1.50	0.95	1.57	1.52
Cr	1.00	0.78	0.95	0.95
Investigator:	Csilléry and Péter[78]		Rapatz[77]	

WELDING NI-W-CR STEELS

Butt welds (70° V.) in heat treated armor plate 0.39 inch thick containing 0.60 C, 3.60 Ni, 1.30 W, 0.62 Cr, about 500 Brinell hardness, were made by R. Smallman-Tew (private communication, May 1938) with a backing plate $^1/_4$ inch thick and special covered electrodes. The tempered zone near the weld was 378 Brinell minimum; the hardened zone was 600 Brinell maximum. Flame cutting (cutting oxygen at 33 lb./in.^{2}, preheating oxygen at 33 lb./in.^{2}, cutting speed = 70 ft./min.) with path of cut preheated to dull red to prevent cracking created a hard zone (550–585 Brinell) $^1/_4$ to $^3/_8$ inch from the cut, and a tempered zone (405–470 Brinell), $^1/_2$ to $1^1/_2$ inches from the cut. The local hardening resulting from flame cutting was removed by subsequent welding.

ACKNOWLEDGMENTS

Contributions to this review have been received from:
P. A. E. Armstrong, Latrobe Electric Steel Company.
C. G. Bainbridge, British Oxygen Company Ltd., London, England.
G. Chambers, Foote Mineral Company.
J. V. Emmons, Cleveland Twist Drill Company.
J. P. Gill, Vanadium-Alloys Steel Company.
C. A. Liedholm, Jessop Steel Company.
R. Smallman-Tew, London, England.
E. W. P. Smith, Lincoln Electric Company.

BIBLIOGRAPHY

1. Mars, G., *Die Spezialstähle*, Second Edition, F. Enke, Stuttgart (1922); Jüptner, H. v., *Grundzüge der Siderologie*, Leipzig, 337–347 (1902).
2. Howe, H. M., *Metallurgy of Steel*, New York, Vol. 1, 82 (1890).
3. Burgess, C. F., and Aston, J., *Electrochem. & Met. Engg.*, **7**, 436–438 (1909).
4. Lebrun, M., *Welding Jnl. (London)*, **32**, 294–297 (1935).
5. Kautny, T., *Autog. Metallb.*, **17**, 139–141, 150–154 (1924).
6. Dahl, O., and Sandelowsky, S., *Stahl und Eisen*, **50**, 1639–1642 (1930).
7. Losana, L., and Jarach, M., *Metallurgia Italiana*, **27**, 703–706 (1935).
8. Merz, A., and Eschelbach, R., *Elektroschwg.*, **5**, 41–45 (1934).
9. Wiss, E., *Autog. Metallb.*, **22**, 46–53, 58–66, 74–79 (1929).
10. Firth, L. G., and Knox, L. B., *Proc. Intnl. Acet. Assn.*, **35**, 52–57 (1934).
11. Sanderson, L., *Mech. World & Engg. Record*, **104**, 123–125 (1938).
12. Ober, W. T., *Machinery (N. Y.)*, **38**, 769 (1932).
13. Seibert, W. J., American Welding Society Journal, **2** (6), 20–25 (1923).
14. *Autog. Metallb.*, **18**, 47 (1925).
15. Viall, E., *Electric Welding*, McGraw-Hill Book Co., New York (1921).
16. *Welding Engineer*, **1** (4), 26–28 (1916).
17. Thum, E. E., *Chem. & Met. Engg.*, **19**, 301–308 (1918).
18. Heaton, T. T., *Proc. Instn. Mech. Engrs.*, 49–71 (1919).
19. *Industry & Welding*, **5**, 26–27, Oct. 1933.
20. *A E G Mitt.*, **19**, 204–206 (1923).
21. Kollbrunner, E., *Werkzeugmaschine*, **36**, 437–438 (1932).
22. Mebes, K., *Dinglers Polytech. Jnl.*, **340** (4), 37–41 (1925).
23. Hartmann, P., *Centralblatt Hütten u. Walzwerke*, **23**, 62 (1919).
24. Steele, L. J., and Martin, H., *Jnl. Instn. Elec. Engrs.*, **60**, 136–157 (1921).
25. Eckman, M. L., *Machinery (N. Y.)*, **37**, 625 (1931).

26. Croskell, A. C., *Welding Jnl. (London)*, **31**, 205–208 (1934); *Inst. Weld. Engrs. Proc.*, **7**, 5–16 (1933).
27. Sauer, J., *E T Z*, **38**, 485–487 (1917).
28. Otto, J., *A E G Mitt.*, 308–310 (1925)
29. Biernacki, J., *Przeglad Mechaniczny*, **2**, 572–574 (1936).
30. Walker, G. L., American Welding Society Journal, **5** (7), 40–48 (1926).
31. Vincent, T. K., *Welding*, **3**, 22–25 (1932).
32. McKernan, E. J., *Iron Age*, **97**, 902 (1916).
33. Viall, E., *Gas Torch & Thermit Welding*, McGraw-Hill, New York, 418–421 (1921).
34. *Welding Engineer*, **2** (9), 34 (1917).
35. Dawson, J. R., *Trans. Amer. Soc. Steel Treating*, **4**, 467–487 (1923).
36. Dawson, J. R., *Welding Engineer*, **7** (1), 32–37, 40 (1922).
37. Miller, S. W., American Welding Society Journal, **3** (9), 17–18 (1924).
38. Bliss, L. G., *Foote Prints*, **7** (1), 12–25, June 1934.
39. Brown, R. E., *Metal Progress*, **28**, 136–140, 144, Oct. 1935.
40. Smith, M. C., *Welding Engineer*, **19** (11), 18–19 (1934); *Amer. Machinist*, **80**, 722–724 (1936).
41. *Procedure Handbook for Arc Welding*, Lincoln Electric Co., Fourth Edition, 1936.
42. *Welding Engineer*, **1** (1), 18 (1916).
43. Auel, C. B., *Amer. Machinist*, **44**, 855–856 (1916).
44. Smith, H., *Blast Furnace & Steel Plant*, **14**, 513–514 (1926).
45. Lacy, E. D., *Metallurgia*, **1**, 65–68 (1929).
46. Haramiishi, M., *Welding Jnl. (London)*, **27**, 174–177 (1930); American Welding Society Journal, **9** (6), 33–46 (1930).
47. Goleniewicz, J., and Cegielski, H., *A E G Mitt.*, 184–185 (1932).
48. Rosenberg, F., *Werkstattstechnik u. Werksleiter*, **31**, 490–492 (1937); *A E G Mitt.*, **30** (3), 137–138 (1938).
49. Goleniewicz, J., *Przeglad Techniczny*, **73**, 19–22 (1934).
50. Weinman, R. A., *General Electric Review*, **32**, 532–537 (1929).
51. Serata, K., *Trans. Soc. Mech. Engrs. Japan*, **2**, 244–253 (1936).
52. Oertel, W., and Grützner, A., *Die Schnelldrehstahle, Verlag Stahleisen*, Düsseldorf, 55 (1931).
53. Pattermann, O., *Werkzeugstähle*, Kladno, 352–353 (1937).
54. Snelling, R., and Richards, E. T., *Werkzeugmaschine*, **42**, 137–140 (1938).
55. Lutz, W., *Werkstattstechnik*, **10** (10), 1916; *Z. ÖIAV*, **68**, 899 (1916).
56. *TZ. prakt. Metallb.*, **53** (5), 33–34 (1920).
57. Highduchek, J. M., *Machinery (N. Y.)*, **40**, 341–342 (1934).
58. *Autog. Metallb.*, **17**, 17–19 (1924).
59. Kurrein, M., *Werkstattstechnik*, **20**, 372–376 (1926).
60. *Amer. Machinist*, **44**, 123–124 (1916).
61. Simpson, W. S., *Iron Trade Rev.*, **49**, 286 (1911).
62. *Werkstattstechnik*, **8**, 337–338 (1914).
63. Burstyn, W., *Das Löten*, Springer, Berlin, 41 (1927).
64. Drescher, C. W., *Stahl und Eisen als Werkstoff*, **4**, 81–88 (1928).
65. Grimshaw, L. C., *Metal Progress*, **31**, (5), 504–507, May 1937; *Trans. A. I. M. E.*, **120**, 363–382 (1937).
66. Martin, T. O., *Reactions*, **9**, 52–54 (1916).
67. Eckman, M. L., American Welding Society Journal, **7** (5), 31–48 (1928).
68. Jevons, J. D., and Wheeler, M. A., *Welding Ind.*, **4**, 356–360, 396–400 (1936).
69. Czako, N., *Z. Ver. deut. Ing.*, **63**, 166–168 (Plate 180) (1919).
70. Sommer, F., *Z. Ver deut. Ing.*, **73**, 1764–1768 (1929).
71. Tewes, K., *Verkehrstechnik*, **47**, 193–198 (1930).
72. Hoffmann, W., *Verkehrstechnik*, **48**, 6–8 (1931).
73. Gorthon, B., *Teknisk Tidskrift*, **66**, A491–A502 (1936).
74. Golling, F., *Autog. Metallb.*, **29**, 166–171 (1936).
75. Zeyen, K. L., *Iron & Steel Inst. Weld. Symposium*, **II**, 617–633 (1935).
76. Zeyen, K. L., *Kruppsche Monatsh.*, **12**, 121–139 (1931).
77. Rapartz, F., *Stahl u. Eisen*, **51**, 245–253 (1931).
78. Csilléry, D. v., and Péter, L. v., *Elektroschweg.*, **7**, 46–53, 64–70 (1936).
79. Taylor, W. H., *Amer. Machinist*, **32** (1), 1–6 (1909).
80. Lapidus, W., *Novosti Tekhniki*, **6** (32), 20–21, Nov. 1937.
81. Ivannikov, D. G., and Shinitzina, S. S., *Vestnik Metallopromyshlennosti*, **18** (4), 22 (1938).
82. Eyles, A., *English Mechanics*, **17**, 471 (1935).
83. Vorbach, K., *E T Z*, **37**, 289–290 (1916).

SUGGESTED RESEARCH PROBLEMS

1. The microstructure and distribution of hardness in welds made between high speed steel and different plain-carbon and low alloy steels should be investigated. Welds made by different processes: resistance butt, flash, pressure, gas, arc, should be studied in different conditions of heat treatment: annealed, quenched, tempered.

2. Blow-holes frequently are observed in fusion welded high speed steel, but the cause is not known. Is the process at fault, or is it a question of composition and freedom from impurities of the filler rod, as one expert suggests? Are the blow-holes filled with CO or gaseous tungsten oxide, WO_3?

3. It would be interesting to apply the McKee weldability test (see *Metal Progress*, **35** (1), 55–58, January 1939) to high speed steels. In the McKee test an atomic hydrogen torch is passed over the surface, leaving a narrow path of molten base metal behind. Observation is made of the molten metal to detect sparks—and evolution of gas. The fluidity is appraised, and the bead is examined for cracks and undercut. The effect of heat input on the test has not been studied. For example, will a steel having poor weldability according to the McKee test with an atomic hydrogen torch, exhibit the same characteristics under an oxyacetylene flame that is neutral to high speed steel? Segregated carbides in high speed steel may exert considerable effect.

4. It has been stated that a tool or cutter consisting of a high speed steel tip welded to a backing metal of different composition, such as plain-carbon steel, often outlasts a tool made wholly of high speed steel, because the damping effect of the composite tool is greater. No measurements appear to have been made of the damping effect of different types of welded tools, presuming that damping effect has a bearing on tool life, machine vibration (accuracy) and surface finish of the material being cut.

MANGANESE IN RUTILE TYPE OF Coated Steel Electrodes*

By ERNEST G. ENCK†

INTRODUCTION

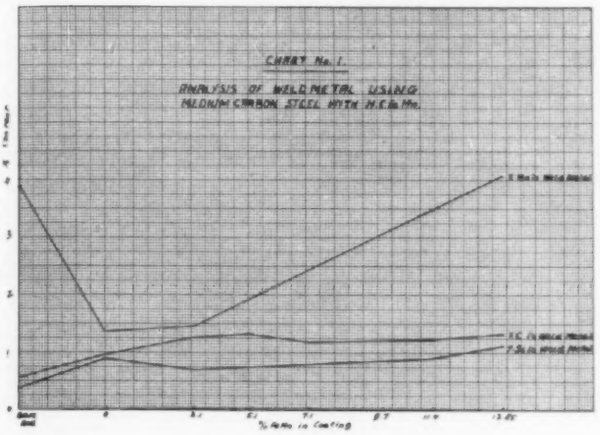
IN THE production of open-hearth steel, the operator adds ferromanganese to his melt in order to remove the oxides and gases in his melt. In addition to this, manganese plays a very important part to both the metal and slag. The presence of manganese increases the fluidity of the open-hearth slag, therefore, assisting in the working of the heat. The presence of sufficient manganese in the steel helps to flux any oxides in the bath. The greater the elimination of iron oxides, the higher the residual manganese. It has been stated that the residual manganese in steel varies with the percentage of manganese in the charge, and the ferrous oxide in the slag. It is further stated that if excessive high residual manganese is carried, trouble is likely to occur in the surface of the finished steel.

As the welding of steel is in reality a miniature open-hearth furnace, it would seem logical to assume that those conditions which exist in open-hearth practice would likewise exist in the welding of medium carbon steels.

In all mild steel electrode coatings, there is present varying percentages of ferromanganese. It has been as-

* Presented at Annual Meeting, A. W. S., Chicago, Oct. 23–27, 1939. Contribution to Fundamental Research Division, Welding Research Committee.

† Chemical Director, Foote Mineral Company, Philadelphia, Pa.



sumed that the addition of this alloy has as a primary function the maintenance of the proper manganese content of the deposited metal. We have felt that in addition to this, it helps in the control of what might be called "interfacial tension" between the slag and weld metal.

It was thought desirable to study the effect of various percentages of ferromanganese as well as other manganese compounds in the rutile type of coatings. In addition to this, we investigated quantitatively the distribution of the manganese from the coated rod after it has been welded. In our investigation, we used both high carbon and medium carbon ferromanganese as well as bare rods of two different manganese and carbon contents.

Analysis of Ferromanganese

	High Carbon, %	Med. Carbon, %
Manganese Metal	79.3	83.77
Silicon	0.45	1.18
Sulphur	0.018	0.04
Phosphorus	0.25	0.20
Carbon	6.9	1.19

Where we used the conventional mild steel electrode containing 0.13 to 0.18% carbon, they shall be referred to as "medium carbon steel" and the rods containing 0.06% carbon as "low carbon steel."

Analysis of Bare Rods

	Medium Carbon Steel, %	Low Carbon Steel, %
Carbon	0.14	0.06
Manganese	0.51	0.15
Silicon	0.04	0.04

All of the welding rods were coated in the same manner using a modified hydraulic type of extruder and were dried at 150° C. for 14 hours.

Coating Mixture

Rutile	Asbestos
Cellulose	Sodium Silicate
Clay	Ferromanganese

In depositing the metal, the beads were laid on $^1/_4$-inch and $^1/_2$-inch plates of boiler steel stock having a manganese content of 0.439%. The beads were deposited in such a manner as to secure a minimum amount of penetration. In our tests for quantitative recovery of manganese, the deposits were laid on the $^1/_2$-inch plates, 1 inch wide and 12 inches long. These plates rested on a large copper plate. In this manner, all of the splatter fell on the copper plate to which there was no adherence, so that it was easily removed for weighing. After removing the slag, drillings were taken and analyzed. All of the reports reported are the average of many individual tests.

Rod and Welding Characteristics

Size of bare rod	$^5/_{16}$ inch x 14 inches
Diameter of coated rod	0.230 inch
Current in amp. and volts	180—25
Type of weld	Flat bead

The first part of this paper will deal qualitatively on the effect of manganese; while the second part will give quantitative figures on the recovery of manganese.

EFFECT OF HIGH CARBON FERROMANGANESE

In coated rods containing high carbon ferromanganese, it was observed that those containing low percentages of ferromanganese produced a very rough bead. As the amount of ferromanganese increased the smoothness of the deposited bead improved until a certain point was reached, beyond this optimum percentage the bead again became rough, but of an entirely different nature than when using too small a percentage of manganese. In the case of medium carbon steel, the best shaped bead was obtained when using 10 parts ferromanganese; while in the case of the low carbon steel, 7.5 parts of ferromanganese produced the best shaped bead and smoothest operating rod. Table 1 shows the percentages of manganese, silicon and carbon found in the deposited metal when using medium carbon steel rods. It will be noted that as the ferromanganese content of the coating increased, the percentage of manganese in the deposit likewise increased. However, there were very little differences in the percentages of silicon and carbon. The results given in Table 1 are shown graphically in Chart 1.

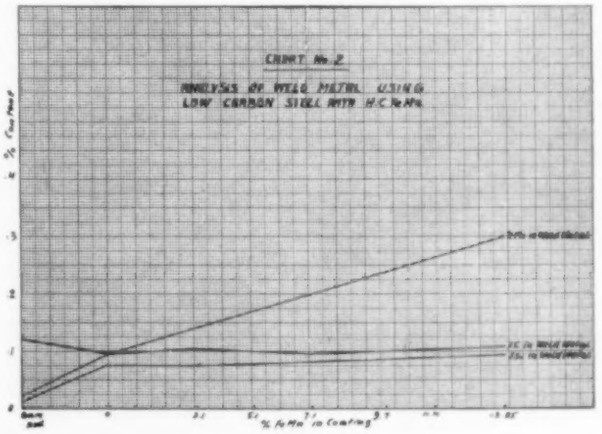


Table 1—Analysis of Weld Metal Using Medium Carbon Steel with High Carbon Ferromanganese

Parts FeMn in Coating	% Mn in Deposit	% Si in Deposit	% C in Deposit
Bare rod	0.390	0.037	0.055
None	0.135	0.090	0.094
3	0.143	0.070	0.124
5	0.201	0.075	0.132
7.5	0.252	0.098	0.177
10	0.305	0.088	0.116
12	0.327	0.090	0.124
15	0.409	0.112	0.133

The results when using a low carbon steel are given in Table 2. Likewise Chart 2 shows graphically these results.

Table 2—Analysis of Weld Metal Using Low Carbon Steel with High Carbon Ferromanganese

Parts FeMn in Coating	% Mn in Deposit	% Si in Deposit	% C in Deposit
Bare rod	0.121	0.012	0.223
None	0.092	0.075	0.092
3	0.133	0.075	0.102
5	0.152	0.077	0.096
7.5	0.187	0.089	0.097
10	0.240	0.086	0.106
12	0.255	0.096	0.100
15	0.320	0.093	0.106

EFFECT OF MEDIUM CARBON FERROMANGANESE

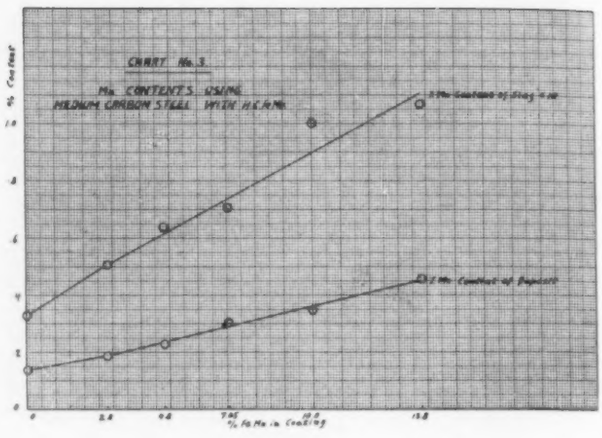
Another series of tests were made substituting medium carbon ferromanganese for high carbon ferromanganese in the coatings. The results of these tests are shown in Tables 3 and 4. When comparing these results with those obtained using high carbon ferromanganese, very little difference can be noted as far as chemical analysis of the weld metal is concerned. However, it was the opinion of the welder that the rods containing medium carbon ferromanganese produced an arc that was somewhat wild with a tendency to undercutting. Those observations held true in both types of core wire and all percentages of ferromanganese.

Table 3—Analysis of Weld Metal Using Medium Carbon Steel with Medium Carbon Ferromanganese

Parts FeMn in Coating	% Mn in Deposit	% Si in Deposit	% C in Deposit
3	0.164	0.075	0.172
7.5	0.231	0.084	0.121
12	0.345	0.082	0.119

Table 4—Analysis of Weld Metal Using Low Carbon Steel and Medium Carbon Ferromanganese

Parts FeMn in Coating	% Mn in Deposit	% Si in Deposit	% C in Deposit
3	0.123	0.054	0.103
7.5	0.154	0.063	0.095
12	0.220	0.075	0.135



SHAPE OF FILLET BEAD

Some very interesting observations were made comparing the shape of the various beads when deposited in a fillet weld. With low manganese, low carbon core wire, the best shaped bead was secured with 7.5 parts ferromanganese. Strangely enough a larger percentage of ferromanganese was required in the coating when a high manganese core wire was used. With the latter core wire, the best shaped bead was obtained with 10 to 12 parts ferromanganese. These experiments seem to indicate that the shape of the weld bead is not governed by the analysis of the weld metal. The shape of the bead is apparently determined either by the surface tension of the slag or by the "interfacial tension" between the slag and weld metal. In both series of tests, rods having a low percentage of ferromanganese in the coating gave convex shaped beads. As the percentage of ferromanganese was increased the beads flattened out and ultimately an almost flat bead was produced. Increasing the percentage of ferromanganese beyond this point produced a concave bead.

EFFECT OF POTASSIUM SILICATE

Rods were made substituting potassium silicate, 27° Bé, in the coating mixture for sodium silicate and using 12 parts of high carbon ferromanganese. A comparison of the manganese and silicon content of the weld metal was as follows:

Sodium Silicate		Potassium Silicate	
Mn	0.329%	Mn	0.375%
Si	0.088%	Si	0.122%

These tests seem to indicate that a certain percentage of potassium silicate, either in the form of the soluble silicate or as feldspar, might be used advantageously to increase the manganese content of the weld metal without increasing the percentage of ferromanganese in the coating. However, at the same time, there seems to be an increase in the silicon content when using potassium silicate.

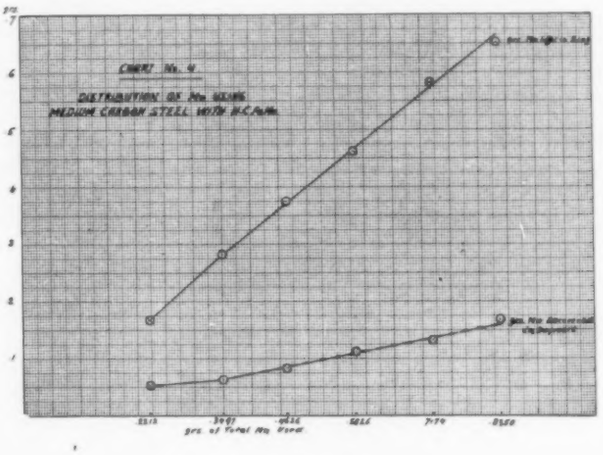
MANGANESE AND CARBON CONTENT OF SINGLE PASS AND MULTIPLE PASS WELD METAL

The analysis of a built up deposit of ten superimposed beads using medium carbon steel coated rods compared with that of a single pass bead gave the following:

Single Pass		Multiple Pass	
Mn	0.305%	Mn	0.313%
C	0.116%	C	0.136%

The coating in both cases contained 10 parts of high carbon ferromanganese.

These results seem to indicate there is very little difference in the manganese content, but that the carbon content is slightly higher in a built-up bead.



QUANTITATIVE RECOVERY OF MANGANESE

A series of welds were made with coated rods containing various percentages of ferromanganese. Each rod was deposited on a separate plate in the manner described in the early part of this paper. Care was taken in recovering all of the splatter and slag. All of the rods were of medium carbon steel core with high carbon ferromanganese in the coating. The results given represent the average of a number of individual tests. Table 5 gives the average weight in grams of all steps involved as well as percentages of metal recovery. It will be noted that the efficiency of the metal recovered averaged 85.2%. Table 6 shows the average weights in grams of the manganese both in the original rods as well as the deposit and slag. Table 7 gives the percentage of manganese used in the various rods as well as manganese found in the deposited metal, slag and splatter.

The percentage of ferromanganese in the coating had very little effect on the deposit efficiency. When examining the manganese recovery figures, it will be seen that the distribution of the manganese between the deposit, slag and splatter are rather constant. It would, therefore, seem that some factor was playing an important rôle in controlling the percentage of manganese recovered in the deposit. Charts 3 and 4 show diagrammatically the results just described.

Table 5—Weights* and Percentages of Metal and Slag

Rod Number	1	2	3	4	5	6
Rod used	42.3	42.6	44.5	44.3	44.7	43.6
Coating used	5.8	5.8	6.1	6.3	6.1	5.55
Deposit (metal)	37.4	35.2	38.0	37.3	38.1	36.9
Slag — non-magnetic	4.76	5.2	5.6	6.0	5.4	5.67
Slag—magnetic	0.30	0.37	0.31	0.55	0.42	0.43
Splatters	3.53	4.10	4.10	5.53	5.0	5.55
Deposit efficiency	88.50%	83.5%	85.5%	84.3%	85.0%	84.9%
% Metal in splatter	8.35	9.60	9.20	12.5	11.20	12.7
% Metal in slag	0.71	0.87	0.69	1.24	0.94	0.98
% Metal lost by slag reaction	2.44	6.03	4.61	1.96	2.86	1.42

* Weights shown are in grams.

Table 6—Weights of Manganese in Grams

Rod Number	1	2	3	4	5	6
Rod used	0.2200	0.2215	0.2314	0.2304	0.2324	0.2267
Coating used	0.0012	0.1282	0.2312	0.3522	0.4850	0.6083
Total manganese used	0.2212	0.3497	0.4626	0.5826	0.7174	0.8350
Deposit (metal)	0.0520	0.0644	0.0847	0.1120	0.1318	0.1686
Slag — non-magnetic	0.1646	0.2750	0.3651	0.4542	0.5670	0.6497
Slag—magnetic	0.0012	0.0064	0.0099	0.0069	0.0140	0.0023
Splatter	0.0032	0.0017	0.0024	0.0038	0.0038	0.0055
Total manganese recovered	0.2210	0.3475	0.4621	0.5769	0.7166	0.8261

Table 7—Percentage of Manganese Used and Recovered

Rod Number	1	2	3	4	5	6
Bare rod	0.520	0.520	0.520	0.520	0.520	0.520
Coating	0.02	2.21	3.79	5.59	7.95	10.96
Coated rod	0.46	0.722	0.913	1.15	1.415	1.70
Deposit (metal)	0.139	0.183	0.223	0.30	0.346	0.457
Slag — non-magnetic	3.46	5.29	6.52	7.57	10.5	11.46
Slag—magnetic	0.405	1.74	3.20	1.16	3.35	5.15
Splatter	0.91	0.425	0.591	0.691	0.75	1.00
% FeMn in coating	0	2.8	4.8	7.05	10.0	13.8
% Mn recovered in deposit	23.5	18.0	18.3	19.2	18.4	20.2
% Mn lost in slag	74.95	80.46	81.06	79.15	80.98	78.08
% Mn lost in splatter	1.44	0.49	0.51	0.65	0.52	0.65
% Mn unaccounted for	0.12	0.65	0.13	1.00	0.10	1.07

Herty, in discussing the chemical aspects of open-hearth practice, reported that in charges with the FeO content remaining constant, and the amount of manganese increasing, the percentage of manganese recovered in the metal was likewise constant. It was only by decreasing the FeO content that an increase in percentage of manganese recovered was noted.

We attempted to determine the percentage of ferrous oxide in the several slags. Due to the presence of complex titanium compounds, the findings were not as accurate as one would like. However, based on sufficient number of analyses, it is safe to say there was little difference in the FeO content of the different slags. This would then account for the rather constant percentage of manganese recovered in the series.

EFFECT OF CELLULOSE ON MANGANESE RECOVERY

The series of tests on quantitative recovery quite definitely show that, in order to increase the percentage of manganese in the deposited metal, it is necessary to increase the manganese content or decrease the FeO content of the slag. One way to reduce the FeO formation during welding is to increase the cellulose content of the coatings.

We took our regular coating mixture, substituted ferromanganese with manganox and varied the cellulose content. It was found on welding that as the cellulose content increased, a sharp increase in the manganese of the deposit. The results were:

8.5 parts cellulose	0.131% Mn
10.0 parts cellulose	0.158% Mn
11.5 parts cellulose	0.180% Mn

Qualitative tests indicate that as the cellulose content increased, the FeO content in the slag decreased.

EFFECT OF MANGANOX AND MANGANESE CARBONATE IN COATINGS

Manganox is a trade name for specially prepared hausmannite or Mn_3O_4. This material is brown in color and has a manganese metal content of approximately 64%. It has a number of advantages over ferromanganese. Due to its inertness, it will not cause gassing in sodium silicate solutions. Likewise, ferromanganese will oxidize progressively after the electrodes are made, whereas, manganox will remain stable. A series of tests were made using varying percentages of manganox as a substitute for ferromanganese in a coated medium carbon steel rod. The results seem to indicate that this material can be used as a partial substitute for ferromanganese. The following table shows the comparison of rods containing ferromanganese and those containing manganox with the percentage of manganese found in the deposited metal.

Table 8—Comparison of Weld Metal Using Medium Carbon Steel with Manganox and Ferromanganese

% Mn in Coated Electrode	% Mn in Deposit Using Manganox in Coating	% Mn in Deposit Using Ferro Mn in Coating
0.832	0.156	0.193
1.16	0.234	0.280
1.45	0.276	0.338

When testing the manganox series in fillet welding, it was found the coated rod containing 1.45% manganese gave the flattest bead. In this particular rod the manganox would be equivalent to a little more than 10% ferromanganese in the coating.

A series of rods were made substituting a portion of the ferromanganese in the coating with manganese carbonate or rhodochrosite. In all of the rods tested, we used our standard rutile type of formula varying the quantities of manganese carbonate and ferromanganese. The percentage of manganese found in the deposited metal, as well as parts of the several manganese compounds used are listed below.

Table 9—Comparison of Weld Metal Using Medium Carbon Steel with $MnCO_3$ and Ferromanganese

Parts in Coating	% Mn in Coated Rod	% Mn in Deposit	% Mn in Deposit Using All FeMn
7.5 $MnCO_3$	0.81	0.224	0.188
0.75 FeMn 3.80 $MnCO_3$	0.70	0.217	0.143
1.90 FeMn 9.50 $MnCO_3$	1.12	0.251	0.271
3.0 FeMn 15.0 $MnCO_3$	1.56	0.292	0.354

These tests seem further to indicate that a partial substitution of ferromanganese in the coating can be made with other manganese compounds. They again confirm that the shape of the weld bead is not entirely governed by the manganese content of the weld metal, but is influenced more by certain characteristics imparted to the slag by manganese.

SUMMARY

Increasing the amounts of ferromanganese in the coating results in the increase of manganese in the weld metal but has very little effect on the carbon and silicon content.

The shape of the weld bead is not governed by the analysis of the weld metal, but is probably controlled either by the surface tension of the slag or by the "interfacial tension" of the slag and weld metal. It is to be admitted that by altering the basic formula of a coating, say by increasing the organic content or by the addition of a certain percentage of potassium silicate, a weld metal of higher manganese content can be produced by using a smaller percentage of ferromanganese.

In the development of a welding rod coating, the factors involved are many. If we have a coating in which the percentage of ferromanganese and other ingredients give the proper slag characteristics, but with two small a residual manganese content in the deposit, increasing the cellulose content will give higher residual manganese in the deposit, with little change in the slag characteristics.

The partial substitution of manganese compounds for ferromanganese can be accomplished with little effect on the requisite chemical properties of the weld metal.

FUNDAMENTAL RESEARCH CONFERENCE

The part played by the Fundamental Research Division of the Welding Research Committee in the acquisition and spread of knowledge about welding was exemplified by its Annual Conference, which was held on Tuesday evening at the Stevens Hotel, Chicago. H. M. Hobart, Chairman of the Division, states that the Conference was attended by thirty-five researchers of the Division. There was an approximately equal number of visitors.

Chicago's interest in fundamental welding research was described by Drs. W. A. Pearl and J. L. Miller of Armour Institute of Technology. Dr. Pearl emphasized the remarkable collaboration of local industrial concerns with the research laboratories of the Institute and of Armour Research Foundation in solving problems ranging from welded ice cruisers to corrosion of welded pipe. A number of metallurgical research problems dealing with welding are being studied by Dr. Miller with the assistance of a graduate welding class.

Three short research papers were presented:

1. Austenitic Grain Size of Covered Electrode Deposits, by Herman Granberry, Texas Technological College. Some indication was given of the possible effects of submicroscopic inclusions derived from electrode coatings on austenitic grain size.

2. Welding Mine Rails, by Professor G. P. Boomsliter, West Virginia University. The results of bend-deflection tests and cost analyses of welded rail joints made by a large number of methods were correlated with metallographic studies.

3. High Frequency Resistance Welding, by Professor E. Bennett, University of Wisconsin. Professor Bennett has produced welds by his process in mild steel $^5/_{16}$ in. thick, 6 in. long, using 30,000 c/cles.

These papers aroused a spirited discussion of many "pet" welding subjects particularly micro-cracks and the unknown effects of shrinkage stresses.

Progress with welding research at their universities was related by nearly all the researchers who attended the Conference. The secretary of the Division G. E. Claussen, gave an extremely interesting address on "What is the Fundamental Research Division?" which will appear in the Minutes of the Conference. By the time the Conference was adjourned at 11 P. M. everyone was convinced of the truth of Dr. Adams' opening remarks on the subject, "Is Fundamental Research Worthwhile?"

THE ENGINEERING FOUNDATION

Welding Research Committee

Sponsored by the American Welding Society « « » » and American Institute of Electrical Engineers

Supplement to the Journal of the American Welding Society, December 1939

The WELDING RESEARCH COMMITTEE

PURPOSES AND ACTIVITIES

During 1935 the Welding Reserch Committee was organized by Engineering Foundation and sponsored jointly by the American Welding Society and the American Institute of Electrical Engineers, for the purpose of assembling, digesting and publishing all available information regarding welding research; stimulating and conducting needed research in the welding field; and correlating existing and future programs of welding research. Its activities have been divided into three parts: Literature, Fundamental Research and Industrial Research.

SCOPE AND VALUE OF WORK

Literature

The purpose of this division is to make available in convenient condensed form the welding knowledge contained in the literature. These digests of the literature serve many useful purposes among which may be listed:

1. They bring to the engineer the best information existing which can be used by him in his work thus enabling him to have at hand the combined experience of many people, and the results of thousands of dollars worth of investigational work.
2. They give the research man a basis of operation thus enabling him to profit by the experiences and mistakes of other investigators, and to build his research program on a sound foundation.
3. They give to students a condensed useful digest of information existing on a specific subject, together with a selected bibliography.
4. They indicate gaps in the knowledge where further research work is needed.

Some of the digests that have been made include Fatigue Strength and Corrosion of Welded Joints, Bend Tests and Impact Tests of Welds, Shrinkage Stresses Due to Welding, The Heat Effect in Welding, Welding Copper and Its Alloys, Coatings and Fluxes in the Welding of Steel, and Oxygen Cutting. In a series of a dozen reviews on the welding of every type of steel and all the properties of the welds, the literature on alloying elements, separately and in combination, was summarized.

Fundamental Research

This project is a carry-over from the American Bureau of Welding and has many accomplishments to its credit. The work now comprises more than 70 active research projects in many universities of this country. In addition to the direct results of these investigations, they serve as a means of educating professors and students of the institution in question, thus providing a supply of young engineers who are familiar with welding. However, a considerable number of these researches have developed data, methods of analysis, and information as to the nature of the phenomena involved, which, all told, constitute a major contribution to those industries concerned with welding. Altogether more than 100 important reports have been issued.

There are several meritorious and important fundamental research projects already in hand which badly need some financial support, and it is the hope of the Main Committee that we shall be able to provide such support to the extent of $8,000 or $10,000 a year.

Industrial Research

The purpose of this division is to coordinate industrial research without trespassing upon fields of a temporarily confidential nature. Its functions are carried out in three ways:

1. Stimulation of research on selected phases of welding, and the publication of the results in the form of papers and reports;
2. Arranging symposia on selected subjects as, for example, Impact Tests, and Weldability of Steels;
3. Conduct of large research projects of broad general importance, as, for example, Fatigue Research, Stresses in Welds, Weldability of Steels, Resistance Welding, and Structural Steel.

The work of this division is carried out through the organization of suitable committees. Close cooperation is maintained between other societies and organizations, such as, American Institute of Steel Construction, American Iron and Steel Institute, American Petroleum Institute, American Society of Mechanical Engineers—Boiler Code Committee, American Society for Testing Materials, American Transit Association, technical committees of the American Welding Society, Public Roads Administration, U. S. A., U. S. Navy—Bureau of Construction and Repair, and the like.

It can be safely stated that a large percentage of the outstanding leaders of industrial research in the welding field have been drawn into the picture to serve on one or more of these various committees.

REPORTS

The Committee issues its reports and findings in the form of a monthly research bulletin printed in standard (9 × 12) journal size. Approximately 450 pages are printed each year. Copies of these reports are distributed to cooperators, and to research workers in universities, industries and governmental departments.

FUNDS

Engineering Foundation acts as treasurer for the funds of the Welding Research Committee.

The Annual Budget of the Committee is of the order of $85,000. By distributing the cost among a number of companies, the burden on any one company is small and insignificant as compared with the value of the results received. Contributions may be made to the funds of the Main Committee, or earmarked for specific investigations. Checks may be sent to the Engineering Foundation—Welding Research Committee, 29 West 39th Street, New York, N. Y.

WELDING RESEARCH COMMITTEE

Chairman

COMFORT A. ADAMS

E. G. Budd Manufacturing Company, Philadelphia, Pa.

H. C. Boardman, *Vice-Chairman;* Director of Research, Chicago Bridge and Iron Company, Chicago, Ill.

Everett Chapman, President, Lukenweld, Inc., Coatesville, Pennsylvania

J. H. Critchett, Vice-President, Union Carbide & Carbon Research Laboratories, 30 East 42nd Street, New York

J. J. Crowe, Engineer-in-Charge of Apparatus Research and Development Department, Air Reduction Sales Company, 181 Pacific Avenue, Jersey City, New Jersey

Arthur S. Douglass, Construction Engineer, Detroit Edison Co., Detroit, Michigan

C. L. Eksergian, Chief Engineer, Budd Wheel Company, 12141 Charlevoix Avenue, Detroit, Michigan

A. J. Ely, Mechanical Engineer, Standard Oil Development Company, Elizabeth, New Jersey

F. H. Frankland, Chief Engineer, American Institute of Steel Construction, Inc., 101 Park Avenue, New York

H. M. Hobart, Consulting Engineer, General Electric Company, Schenectady, New York

D. S. Jacobus, Advisory Engineer, The Babcock and Wilcox Company, 85 Liberty Street, New York

G. F. Jenks, Colonel, Ordnance Department, U. S. A., Army Munitions Building, Washington, D. C.

P. G. Lang, Jr., Engineer of Bridges, Baltimore & Ohio Railroad, Baltimore, Maryland

Arthur E. Pew, Vice-President, Sun Oil Company, 1608 Walnut Street, Philadelphia, Pa.

R. E. Zimmerman, Vice-President, United States Steel Corporation of Delaware, 436 Seventh Avenue, Pittsburgh, Pa.

W. Spraragen, *Executive Secretary,* Technical Secretary and Editor, American Welding Society, 29 West 39th Street, New York.

Division Chairmen

Literature—J. H. Critchett

Fundamental Research—H. M. Hobart

Industrial Research—G. F. Jenks

BRIEF PROGRESS REPORT for the Year 1939

FORTY-SEVEN reports were issued by the Welding Research Committee during the past year. Of these 17 related to Fundamental Research investigations. Seven resulted from the activities of the Industrial Research Division. Twelve were critical digests of the world's literature prepared under the auspices of the Literature Division. The remaining reports were of a general nature.

These reports covered a wide variety of fields including resistance welding electrodes, stress distribution in welds subject to bending, welding arcs, spot welding low-carbon and stainless steels, crater formations in arc welding, residual stresses in pipe welding, photoelastic studies of stress distribution, miscellaneous investigations in the structural field, welds at low temperatures, control of distortion, welding of silicon bronze, weldability of medium-carbon steels, copper welding, spot, arc and gas welding of aluminum alloys, nickel-clad steels, and effect of carbon and manganese on weldability.

The literature reviews covered such timely and important subjects as molybdenum steels, copper steels, effect of oxygen, effect of aluminum on the welding of steel, internal stresses in castings, welding coated steel, effect of sulphur and phosphorus on steel, welding chromium steels, coatings and fluxes in the welding of steel, oxygen cutting of steel, effect of carbon in plain carbon steels, and the welding of copper steels.

In addition 39 translations of important foreign research reports were made available either in published or mimeographed form.

These numerous reports represent the cooperative effort of some 250 research workers, engineers and scientists of this country affiliated in one way or another with the various divisions and committees of the Welding Research Committee. In volume these reports represent 456 printed pages. They are distributed all over the world in the form of a monthly Supplement to THE WELDING JOURNAL, and are reprinted separately in bulletin form.

Cooperation continues with the British Welding Research Committee through the regular exchange of publications and correspondence.

Contacts exist with several other research committees in European countries.

Testimonial letters have been received in the central office from all parts of the world. These bear heavily on the value of summarizing and making available the welding knowledge of the world in the form of critical digests of the literature on specific subjects. Some of these reviews involve the condensation and digesting of infor-

mation contained in more than 500 articles written in several languages and published in many journals. Many of these important reports and critical digests have been reprinted by the leading engineering periodicals of foreign countries.

Close contact is maintained with 70 universities. The committee through its Fundamental Research Division supplies machined specimens for welding researchers when needed, assists in the formulation of problems, in small grants in aid, and in the establishment of fellowships. Important fellowships have been established at Rensselaer Polytechnic Institute, Union College and Lehigh University. This Division also makes research results, such as theses and laboratory reports, available to the engineering public.

Outstanding conferences are held annually in connection with the Annual Meeting of the AMERICAN WELDING SOCIETY and the National Metal Congress Exposition. This year the conferences were held in Chicago. The AMERICAN WELDING SOCIETY provided for 17 different reports of the Welding Research Committee on its annual program.

Of material importance to the Welding Research Committee is the cooperation rendered by its sponsors, namely, the AMERICAN WELDING SOCIETY and the American Institute of Electrical Engineers. Other national organizations and societies, such as the American Society of Mechanical Engineers—Boiler Code Committee, the American Institute of Steel Construction, American Society for Testing Materials, American Petroleum Institute and the American Iron and Steel Institute are actively cooperating.

Although our contributions from industry have considerably exceeded the quota assigned by Engineering Foundation, we are still far short of what could be very efficiently used in the way of grants to the Fundamental Research Division, as well as in connection with some important projects of the Industrial Research Division.

However, it should be noted in this connection that the real contribution of Universities to this work is on the whole many times the cash grants and other assistance rendered by the Welding Research Committee. Even in the case of a large investigation, such as the Fatigue of Welded Structures now under way at the University of Illinois, the real contribution of the University is approximately equal to the cash grant of the Committee, namely, about $8000 a year.

The problem of selling our services to industry is not an easy one even under favorable business conditions, as it involves in many cases a rather long period of education, correspondence and in most cases personal contact.

The Welding Research Committee

ITS SERVICE TO INDUSTRY

WHEN an industrial executive is approached for the financial support of an organization, his first queston is apt to be "What is the dollar value of your activities to us?" or "What important problems have you already solved?"

Instead of trying to answer these questions we shall try to paint a picture which will show in its proper perspective the less specific, but more fundamental, and more important service rendered by the organization in question.

Introduction

The evolution of physical science is briefly as follows: The observation of phenomena; giving them names; devising methods of measurement; defining units; discovery of laws (i.e., consistent quantitative relationships between the several factors or variables involved); creating devices, or hypotheses, to help visualize phenomena which are not visible; making logical deductions.

At first the laws are superficial and deal with the periphery of knowledge, a separate set for each part of the periphery. But as we dig deeper we usually find that these apparently independent laws in apparently separate fields are interrelated, often through more fundamental laws.

Theory

The structure made up of such laws, definitions, deductions and hypotheses, may be called the *Theory* of a field of knowledge, and may be thought of as a skeleton which is gradually filled out and confirmed by years of successful application under a wide range of conditions, or of combinations of the several variables involved. One finally comes to respect these laws, to lean on them, and to know that they will always work whether the results are pleasing to us or otherwise. If we do not understand them thoroughly, or do not apply them logically with due regard to all of the significant variables involved, the results will surely be a painful surprise.

Every theoretical or analytical solution of an engineering problem is, without exception, an approximation. Sometimes the geometry of the problem is such as to demand simplifying approximations, and sometimes variables, of minor importance under the surrounding conditions, are neglected. But in every case the resulting solution is limited in its range of safe application.

Theoretical Solution of Engineering or Industrial Problems

For our present purpose these problems may be classified as follows:

(*a*) Problems readily subject to theoretical solution with sufficient accuracy for all practical purposes, provided the approximations are adapted to the particular set of surrounding conditions. Because of the lack of sound understanding on the part of many engineers, some of these problems often fail of solution.

This group is actually much larger than ordinarily assumed.

(*b*) Problems of such a complex nature as to be hopeless of theoretical solution.

(*c*) The intermediate zone includes many important practical problems which, although so complicated as to scare off all but a very few with that rare combination of unusual analytical instinct and unusual training in analytical methods, are nevertheless subject to rational attack. Although in many such cases very crude approximations must be made, it is often possible with the aid of carefully planned experimental research to determine constants which, when employed in the rationally determined formulas, yield a general solution sufficiently accurate for all practical purposes.

Such a solution has three great advantages over the more common method of collecting a great mass of experimental data covering the full range of combinations of all the variables, to be tabulated or plotted, particularly when several variables are involved:

1. The ability to extend the application of the rational method well beyond the range of experimental work involved in the determination of the empirical constants. This may mean the reduction of expensive experimental work to a fraction of that otherwise necessary.
2. It develops a knowledge of the quantitative rela-

tions between the several variables, the result of which it invaluably and often renders unnecessary expensive experimental work, or at least reduces it to a minimum.

3. It develops the habit of thinking rationally and quantitatively about this and similar problems, as well as about problems not so similar. This is the most important asset of all, particularly when considered in connection with cooperative research where a considerable number of men are involved and thus exposed to the influence. Moreover, the problems and methods are fully discussed at the numerous committee meetings, as well as in the various reports of progress.

The long run value of this educational process to the employers of these men is often far greater than that of the solution of the specific problem. The influence extends to a much wider circle when the report is published and discussed in large society meetings.

Unfortunately the employer does not know when the solution of some important problem by one of his engineers was made possible by the information obtained or the habit of analysis acquired through this educational process. The result is a clarification of perspective, and a filling in and consolidation of the foundation without which some of the most important problems cannot be solved, except, perhaps, by laborious and expensive "cut and try," and then for only a very narrow range of conditions.

It happens that some of the most vital problems in welding are in this (*c*) group. They involve problems in transient heat flow, elasticity, plasticity, metallurgy, multiaxial stresses, and the relations of the latter to fatigue endurance. Some of these are: the weldability of steels; the magnitude and effect of residual stresses, and when stress relieving is necessary.

None of these problems can be solved without thorough-going research, rationalized as far as possible, and utilizing the best available skeleton of theory for which the research data is the filling.

It should be noted, however, that in addition to the type of approximations above mentioned, there usually enters another element of doubt, namely, the properties and homogeneity of the material or materials involved. These properties often vary over a wide range, with temperature and other fundamental variables. Full information concerning them is often not available, particularly when the range of temperature is as large as it is in many welding problems. The filling of these gaps often involves research work of the highest order.

The services rendered through cooperative research, in order of importance are:

1. Education.
2. Filling out and consolidation of our fundamental knowledge.
3. Solving of important fundamental industrial problems which can best be handled by the cooperative method.
4. The privilege of drawing on the services of the best experts in each field.

C. A. ADAMS, Chairman

ENGINEERING FOUNDATION—WELDING RESEARCH COMMITTEE

Estimated Budget for Year October 1, 1939 to September 30, 1940

Fundamental Research Investigations	
Work now under way by 70 investigators in some 50 educational institutions	$ 8,475
Literature Research	5,625
Industrial Research activities other than large specific projects	4,000
Expenses of central office, including rent, stenographic services, publications, and miscellaneous	6,750
Publication costs contributed by AMERICAN WELDING SOCIETY	8,500
*Fatigue Research Program	16,000
*Weldability of Steels	20,000
*Structural Welding Research	4,500
*Study of Weld Stresses	5,000
Specimens	4,000
Mimeographing translations and other contributed services	3,000
	$85,850

* Funds contributed for Special Projects and administered by Project Committees. One of these already financed; the other 3 partially financed.

The above sum does not include contributed services and traveling expenses of committee Members and Professors estimated at $30,000.

LITERATURE DIVISION

ONE of the most important undertakings of the Welding Research Committee is to make available in convenient condensed form the welding knowledge contained in the literature of the world. These digests of the literature serve many useful purposes, among which may be listed:

1. They make available to the engineer the best existing information which can be used by him in his work, thus enabling him to have at hand the combined experience of many people, and the results of literally thousands of dollars worth of investigational work.

2. They give the research man a basis of operation which enables him to profit by the experiences and mistakes of other investigators, and to build his research program on a sound foundation.

3. They give to students a condensed useful digest of information existing on a specific subject, together with a selected bibliography.

4. They indicate gaps in the knowledge where further research work is needed.

Probably in no other way could industry have available more quickly and more economically the information which lies buried in the literature scattered among many journals, and written in several languages. Topics selected for study by the committee are made at the re-

quest of industry, technical organizations, committees and research workers.

An additional purpose of the Literature Division is to make available translations of important foreign articles which are published in the Research Supplements or distributed in mimeographed form from the central office of the Committee. Altogether 12 critical digests of the literature were published during the year, 18 translations or extended abstracts printed and 14 mimeographed translations were made available.

The Committee believes that through these efforts and the work of previous years, there has been offered to industry the substance of the best information contained in the foreign literature. Many important English publications of other countries have also been made available in one form or another.

That this work is appreciated by industry and by our European friends is almost daily attested to by letters and comments from leading experts here and abroad. Each of these critical digests represents careful reading and condensation of all the important articles published on a particular subject. Each digest contains a summary of the facts and findings concerning which there appears to be considerable agreement. In each review the bibliographical references alone range from 150 to 350 of the most important articles reviewed. A list of problems needing further research is also made a part of each digest. The digests are first mimeographed and sent to the experts throughout the world for comments and criticism. The revised digests are then published in printed form.

A list of the published reports of the Literature Division for the year 1939 follows:

January 1939

The Effect of Oxygen on the Welding of Steel[1]
The Effect of Aluminum on the Welding of Steel[1]
Internal Stresses in Castings

February 1939

Welding Coated Steel[1]
The Effect of Sulphur on the Welding of Steel[1]

March 1939

Welding Chromium and Chromium-Nickel Corrosion and Heat Resisting Steels[1]

April 1939

The Effect of Phosphorus on the Welding of Steel[1]

May 1939

Coatings and Fluxes in the Welding of Steel[2]

July 1939

Oxygen Cutting of Steel—Part I—Mechanical Properties and Metallurgy[2]

August 1939

Effects of Carbon on the Welding of Plain Carbon Steels[1]

September 1939

Welding Copper Steels[1]

November 1939

Welding Tungsten Steels[1]

In addition to these reports which have been published, the following reviews have been prepared in mimeographed form and sent out for criticism to the experts:

Oxygen Cutting of Steel—Part II—Oxygen Cutting Procedure[2]
The Effect of Hydrogen, Arsenic, Titanium and Miscellaneous Elements on the Welding of Steel[2]
Underwater Cutting, Arc Cutting, The Oxygen Lance, and Oxygen Deseaming and Machining[2]
Flame Cutting Non-Ferrous Metals and Non-Metallic Materials[2]

In connection with the translations, the Literature Division is indebted to a number of university workers connected with the Fundamental Research Division, and to industry for their cooperation in rendering translations, and in providing mineographing facilities, and the like.

The following translations were printed during the year:

January 1939

Welding Low-Alloy Structural Steel, by G. Schaper
Some Present-Day Problems of Strength in Structural Welding, by Dr. Ing. G. Bierett
The Failure of Girders Under Repeated Stresses, by Professor Frederick Charles Lea and John Gwynne Whitman

February 1939

Static and Fatigue Strength of Welded Reinforcing Bars, by Dr. Ing. Bühler
Residual Stress Tests of All-Welded Plate Girders, by H. E Lance Martin
Temperatures in Flash Welding, by H. Kilger

April 1939

Cracks Caused by Corrosion of a Welded Aluminum Tank, by M. Bosshard
A New Spot Welder for Shipyards, by O. Wundram
Elastic Behavior and Strength of Side Fillet Welds, by D. Rosenthal and P. Levray
German Boiler Code
Weldability and Mechanical Properties of High-Tensile Steel Sheet, by H. Cornelius and F. Bollenrath
Tentative German Specifications for Welding Rods for the Gas Welding of Copper

May 1939

Strengthening of the Austerlitz Bridge (Paris) by Electric Arc Welding, by M. Fauconnier
Spot Welding Light Metals in a German Aircraft Works, by Dr. K. Reichel
Effect of Normalizing Fine Grained Low-Alloy Steel (St 52) before Welding, by Roland Wasmuht

August 1939

Efficient Utilization of Aircraft Tubing, by J. Müller
Fatigue Strength of Oxyacetylene Welded Boiler Plate, by K. H. Bussmann

September 1939

"Firecracker" Welding, by Georg Hafergut

November 1939

The Strength of Spot Welds, by E. Warnant, L. Vandeperre, A. Joukoff and P. Lucas

PERSONNEL

Critchett, J. H., *Chairman;* Union Carbide and Carbon Research Laboratories, Inc., New York
Adams, C. A., *Chairman,* Welding Research Committee; E. G. Budd Manufacturing Company, Philadelphia, Pa.
Boardman, H. C., Chicago Bridge and Iron Company
Hobart, H. M., General Electric Company, Schenectady, New York
Jacobus, D. S., Babcock and Wilcox Company, New York
Spraragen, W., *Secretary*

[1] A Review of the Literature to July 1, 1937.
[2] A Review of the Literature to Jan. 1, 1938.
[3] A Review of the Literature to July 1, 1938.

FUNDAMENTAL RESEARCH DIVISION

THE Fundamental Research Division directs its research efforts toward discovering "reasons why." The operation of the Division is in several respects unique and has already been described in the Welding Research Supplement for June 1939. Briefly it may be said that a list of current research problems needing solution is compiled, published and distributed to the researchers of the Division and to others interested. When a problem is undertaken for research, the Division is prepared to assist in the formulation and in supplying bibliographic references. When desired and if practicable, the Division assists in supplying specimens, materials and apparatus. In some instances, grants-in-aid, and research fellowships have been established at the recommendation of the Division.

There has been instituted a practice of holding every year a conference of the Division on the occasion of the annual meeting of the AMERICAN WELDING SOCIETY. A proportion of the railroad and pullman expenses of some researchers to the Conference has been defrayed. At the annual conference, the research workers meet with one another and compare their research experiences. In the course of the conference, valuable acquaintanceships are made with other people in the welding professions.

At the annual evening conference of the researchers of the Fundamental Research Division held during the 1939 Meeting of the AMERICAN WELDING SOCIETY in Chicago, thirty-five researchers were present, which with an approximately equal number of visitors, raised the total attendance to over seventy. After an introductory address by Dr. C. A. Adams, Chairman of the Welding Research Committee, the researchers briefly described their projects. Among those who spoke was Professor Edward Bennett of the University of Wisconsin, who described the process of welding he has developed, utilizing high frequency current, up to 30,000 cycles. Describing bend-deflection tests of welded mine rails made by arc, torch and thermit processes, Professor C. P. Boomsliter of West Virginia University correlated strength with microstructure of heat-affected zone and with design of joint. A research student of Texas Technological College, Herman Granberry, gave some indication of the possible effects of submicroscopic inclusions derived from electrode coatings on the McQuaid-Ehn grain size of weld metal. The papers were followed by a spirited discussion of microcracks and shrinkage stresses in welding. Progress with welding research was related by nearly all the researchers who attended the Conference.

LIST OF PROBLEMS

At intervals the division compiles a list of Fundamental Research problems for the assistance of the researchers in the Division and others. The last revision was published in June 1939 and is available in pamphlet form. The list emphasizes the importance of fundamental welding research in engineering and science and outlines the fundamental researches believed to be particularly needed in the welding field.

DIRECT CONTACTS

The Division keeps in touch with the researchers not only through correspondence and research conferences, but by visits from the Division's staff. About 20 laboratories were visited during the past year. These visits serve many useful purposes, including (1) explanation of outstanding Division objectives; (2) securing of first-hand information of facilities available at each laboratory including equipment and personnel; (3) guidance in solving problems and in overcoming obstacles; and (4) securing reports of researches completed, but not ordinarily published.

PUBLISHED REPORTS

During the year the Division published fifteen reports.

Effect of Eccentricity on the Strength of Welded Joints. William W. Stecker, thesis prepared in the College of Engineering, Department of Civil Engineering, University of California; *Welding Research Supplement*, November 1938, pp. 8–11.

Column Tests of Reinforced Channels. R. J. Roark, Professor of Mechanics, University of Wisconsin, C. B. Voldrich and E. Sollid; *Welding Research Supplement*, November 1938, pp. 31–42.

Stresses in Single Angles. J. Merle, Jr., and W. W. Smith, thesis prepared at Washington University, St. Louis; *Welding Research Supplement*, January 1939, pp. 12–14.

Deflection Tests on the Welded Steel Frame of Lagunita Court, R. A. Hattrup and H. A. Russell, thesis supervised by Professor A. S. Niles in the Department of Civil Engineering, Stanford University; *Welding Research Supplement*, February 1939, pp. 51–55.

Data on the Spot Welding of Low-Carbon Steel. Wendell F. Hess, Associate Professor of Metallurgical Engineering and Head of the Welding Laboratory, Renssalaer Polytechnic Institute, and Robert L. Ringer, Research Fellow, Department of Metallurgical Engineering, Rensselaer Polytechnic Institute; *Welding Research Supplement*, April 1939, pp. 113–119.

The Use of Light Steel Trusses. Leslie W. Graham, Clarkson H. Oglesby and Earle S. Sloan, résumé of a thesis, supervised by Professor A. S. Niles, submitted to the Dept. of Civil Engineering of Stanford University in partial fulfilment of the requirements for the Degree of Engineer, June 1936; *Welding Research Supplement*, April 1939, pp. 119–122.

Using "Controlled" Predistortion to Produce Straight Headers. Gerard Pesman, Institute of Mechanical Engineering, Montana State College, Bozeman, Montana; *Welding Research Supplement*, April 1939, pp. 132–138.

An investigation of Unsymmetrical Loading on Lightweight Steel Studs. Herbert L. Lyell, abstract of a thesis supervised by Prof. J. B. Wells, submitted to the Department of Civil Engineering of Stanford University in partial fulfilment of the requirements for the Degree of Engineer, December 1937; Welding Research Supplement, May 1939, pp. 167–175.

Tests of Plug and Slot Welds. G. E. Troxell, Associate Professor of Civil Engineering, University of California; *Welding Research Supplement*, September 1939, pp. 302–311.

Photoelastic Analysis of Stress in the Lap Plates of Fillet-Welded Joints. Arshag G. Solakian, Polarized Light and Photoelastic Co.; *Welding Research Supplement*, September 1939, pp. 318–323.

Tensile and Tensile Impact Tests of Stainless Steel Welds at Low Temperatures. O. H. Henry, Associate Professor of Metallurgical Engineering, Polytechnic Institute of Brooklyn, J. J. Cordiano, and M. S. Umanoff. Abstracted from B. S. theses in the Department of Mechanical Engineering, Polytechnic Institute of Brooklyn, by Joseph J. Cordiano (tensile tests), and Morton S. Umanoff (tensile impact tests); *Welding Research Supplement*, September 1939, pp. 326–328.

Reactions in Arc Welding. R. David Thomas, Jr., McMullen Fellow, Cornell University, now with the Arcos Corporation, Philadelphia, Pa. and F. H. Rhodes, Director, School of Engineering, Cornell University, Ithaca, New York; *Welding Research Supplement*, October 1939, pp. 335–338.

Further Studies of the Spot Welding of Low Carbon and Stainless Steels. Wendell F. Hess, Associate Professor of Metallurgical Engineering, Head of Welding Laboratory, and Robert A. Wyant, Research Fellow, Department of Metallurgical Engineering, Rensselaer Polytechnic Institute, Troy, New York; *Welding Research Supplement*, October 1939, pp. 348–354.

Contact Resistance in Spot Welding. Frank J. Studer, Union College, Schenectady, New York; *Welding Research Supplement*, October 1939, pp. 374–380.

The Effect of Alloying in Metallic Arc Welding. R. W. Emerson, Research Engineer, Westinghouse Research Laboratories, East Pittsburgh, Pa.; *Welding Research Supplement*, October 1939, pp. 381–392.

PERSONNEL

Hobart, H. M., Chairman, General Electric Co., Schenectady, N. Y.

Executive Committee:

Hobart, H. M., Chairman; General Electric Company, Schenectady, New York.
Adams, C. A., Chairman, Welding Research Committee; E. G. Budd Manufacturing Company, Philadelphia, Pa.
Crawford, W. E., A. O. Smith Corporation, Milwaukee, Wisconsin.
Critchett, J. H., Union Carbide and Carbon Research Laboratories, Inc., 30 East 42nd Street, New York.
Hess, W. F., Rensselaer Polytechnic Institute, Troy, New York.
Jacobus, D. S., Babcock & Wilcox Company, New York.
Jenks, G. F., Army Munitions Building, Washington, D. C.
Studer, F. J., Union College, Schenectady, New York.
Claussen, G. E., Associate Chairman and Secretary.

LIST OF RESEARCHERS

Their Affiliations and Research Subjects, revised November 1939

Name	Affiliation	Subject of Research
Dr. R. H. Aborn	Research Laboratories, United States Steel Corporation, Kearny, New Jersey	
A. Amatulli	Brooklyn Technical High School, Brooklyn, New York	
Prof. N. H. Barnard	Department of Mechanical Engineering, University of Nebraska, Lincoln, Nebraska	
Prof. M. L. Begeman	Department of Mechanical Engineering, University of Texas, Austin, Texas	Hard facing (*a*) Adherence of facing metal. (*b*) Relation between ductility and hardness. (*c*) Hardness as a function of both thickness of deposit and kind of base metal.
Prof. E. Bennett	Department of Electrical Engineering, University of Wisconsin, Madison, Wisconsin	Studies relating to the selective heating of large conducting bodies in strips by utilizing the "proximity effect" to control the distribution of heating currents of the audio frequency range.
Prof. H. Beresford	Department of Agricultural Engineering, University of Idaho, Moscow, Idaho	Application of wear resistant surfaces to agricultural equipment and tools.
Prof. R. K. Bernhard	Department of Mechanics, Pennsylvania State College, State College, Pennsylvania	
Prof. John L. Bray, Dr. J. F. Eckel	Department of Metallurgical Engineering, Purdue University, Lafayette, Indiana.	Viscosity and surface tension of welding slag.
Prof. J. W. Breneman	Department of Mechanics, Pennsylvania State College, State College, Pennsylvania	Shear tests on butt welds in mild steel bars.
Prof. W. H. Bruckner	Department of Metallurgy, University of Illinois, Urbana, Illinois	Weld quench test.
G. H. Chambers	Foote Mineral Company, Philadelphia, Pennsylvania	Electrode coatings, fluxes and slags.
Prof. D. Clark	Department of Mechanical Engineering, California Institute of Technology, Pasadena, California	Stress-strain relations under tension impact.
Prof. G. M. Cover	Department of Metallurgy, Case School of Applied Sciences, Cleveland, Ohio	
J. J. Crowe	Research and Development Department, Air Reduction Sales Company, Jersey City, New Jersey	Oxygen cutting.
Prof. T. M. Curran	Webb Institute of Naval Architecture, Bronx, New York	

Prof. H. L. Daasch	Department of Mechanical Engineering, University of Vermont, Burlington, Vermont	Oxygen cutting.
Dean R. P. Davis, Prof. G. P. Boomsliter and Asst. Prof. C. H. Cather	College of Engineering, West Virginia University, Morgantown, West Virginia	1. Welding mine rails. 2. Ductility of fillet welds.
Prof. A. V. DeForest	Department of Mechanical Engineering, Massachusetts Institute of Technology, Cambridge, Massachusetts	Vibration studies of welded structures.
Prof. D. J. Demorest	Department of Metallurgical Engineering, Ohio State University, Columbus, Ohio	Coatings for welding rods.
Dr. A. DiGiulio	Department of Chemical Engineering, University of Detroit, Detroit, Michigan	
Prof. G. E. Doan	Department of Physical Metallurgy, Lehigh University, Bethlehem, Pennsylvania	1. Causes and conditions of crater formation in arc welding. 2. A study of the basic nature of the arc discharge. 3. The properties of welds made with pure iron in argon and other atmospheres. 4. The use of gamma rays for weld examination. 5. Acceleration of rate of welding by additions to the welding wire.
Prof. W. G. Dow	Department of Electrical Engineering, University of Michigan, Ann Arbor, Michigan	Welding with high frequency currents.
Prof. R. L. Dowdell	Department of Metallurgy, University of Minnesota, Minneapolis, Minnesota	Cold rolling tests for welds.
R. W. Emerson	Westinghouse Electric and Manufacturing Company, Trafford, Pa.	Metallurgy, heat treating, etc.
Prof. D. S. Eppelsheimer	Engineering Experiment Station, University of New Hampshire, Durham, New Hampshire	
Prof. S. L. Goodale	Department of Metallurgy, University of Pittsburgh, Pittsburgh, Pennsylvania	1. Annealing of welded joints. 2. Strain annealing of medium carbon steel (0.25 to 0.30%).
Prof. S. H. Graf	School of Engineering, Oregon State Agricultural College, Corvallis, Oregon	1. X-ray diffraction studies on stresses in welds. 2. Heat effects in weld metal.
H. F. Gray	Department of Mechanical Engineering, Massachusetts Institute of Technology, Cambridge, Massachusetts	Spot welding temperatures.
Prof. P. R. Hall	Department of Industrial Engineering, Pennsylvania State College, State College, Pennsylvania	Welding of cast iron.
Prof. R. H. Harcourt	Department of Mechanical Engineering, Stanford University, Stanford University, California	Flame cutting steel.
Prof. J. C. Hardgrave and H. Granberry	Department of Mechanical Engineering, Texas Technological College, Lubbock, Texas	The grain size problem in the welding of steel.
Dr. R. H. Harrington	Research Laboratory, General Electric Company, Schenectady, New York	Metallurgical aspects of resistance welding electrodes.
Prof. O. H. Henry	Department of Mechanical Engineering, Polytechnic Institute of Brooklyn, Brooklyn, New York	1. Effect of low temperature on the static and dynamic tensile strength of welded joints in some ferrous and nonferrous metals. 2. Fatigue tests of welded joints at low temperatures.
Prof. W. F. Hess and R. A. Wyant	Department of Metallurgy, Rensselaer Polytechnic Institute, Troy, New York	1. Spot welding. 2. Properties of gas and arc welds of fine and coarse grained steels. 3. Non-destructive testing of welds.
I. T. Hook	American Brass Company, Ansonia, Connecticut	
Prof. S. C. Hollister	Department of Civil Engineering, Cornell University, Ithaca, New York	1. Locked up stresses in welds. 2. Repeated stresses in welded connections.
Prof. T. P. Hughes	Department of Mechanical Engineering, University of Minnesota, Minneapolis, Minnesota	1. Relation of current and voltage to speed of welding. 2. Ductility values as obtained by the tensile and cold bend tests.
Prof. R. E. Jamieson	Department of Civil Engineering, McGill University, Montreal, Canada	Initial stresses caused by welding.
Prof. C. D. Jensen	Department of Civil Engineering, Lehigh University, Bethlehem, Pennsylvania	Welded girders with inclined stiffeners.
W. C. Johnson	Department of Electrical Engineering, Princeton University, Princeton, New Jersey	Spot welding.
Prof. Bruce G. Johnston and H. J. Godfrey	Fritz Engineering Laboratory, Lehigh University, Bethlehem, Pennsylvania	1. Fracture characteristics. 2. Welded building frames. 3. Allowable unit stresses in coated electrode welds.
Prof. J. F. Kahles	Department of Metallurgy, University of Cincinnati, Cincinnati, Ohio	
Prof. J. B. Kommers	Department of Mechanics, University of Wisconsin, Madison, Wisconsin	1. Fatigue tests on notched specimens.
Prof. W. B. Kouwenhoven	School of Engineering, Johns Hopkins University, Baltimore, Maryland	1. Flux penetration tests. 2. Non-destructive tests for welds. 3. A study of contact resistance in resistance welding.
Prof. W. J. Krefeld	Department of Civil Engineering, Columbia University, New York, New York	Welded beam connections.
Dean A. S. Langsdorf	School of Engineering and Architecture, Washington University, St. Louis, Missouri	
Dr. H. H. Lester	Watertown Arsenal, Watertown, Massachusetts	X-ray specifications for castings and welded structures.

Prof. Joseph Marin	Department of Civil Engineering, Armour Institute of Technology, Chicago, Illinois	1. Deformations and stresses at yield and fracture of materials subjected to bi-axial and tri-axial stresses. 2. Experimental study of tubes loaded to fracture in tension and torsion. 3. Experimental study of materials subjected to torsion and bending fatigue.
Dr. J. W. Miller	Reid-Avery Company, Dundalk, Maryland	
Prof. J. L. Miller and Prof. Arthur H. Carpenter	Armour Institute of Technology, Chicago, Illinois	
Prof. H. F. Moore	Talbot Testing Laboratory, University of Illinois, Urbana, Illinois	Welded railroad rails.
Dean J. A. Needy	College of Engineering, Ohio Northern University, Ada, Ohio	Welding malleable cast iron to steel.
Prof. J. T. Norton	Department of Mining and Metallurgy, Massachusetts Institute of Technology, Cambridge, Massachusetts	1. Radiographic technic of examining welds. 2. X-ray diffraction method for investigating stresses in welds. 3. X-ray studies of age hardening.
Dr. W. A. Pearl	Department of Mechanical Engineering, Armour Institute of Technology, Chicago, Illinois	1. Corrosion of weld zone of wrought iron, structural steel, and low-alloy, corrosion-resisting steels. 2. Heat affected zone of arc-welded cast iron. 3. Fusion zone of high carbon steel and 18–8. 4. Weld zone of 14 Mn steel.
C. L. Pfeiffer	Western Electric Company, Hawthorne Station, Chicago, Illinois	Resistance welding.
Prof. F. H. Rhodes	Department of Chemistry, Cornell University, Ithaca, New York	Chemical and metallurgical reactions in the electric arc.
Prof. R. J. Roark	Department of Mechanics, University of Wisconsin, Madison, Wisconsin	Study of plaster models of welded joints.
Prof. G. Sachs	Department of Metallurgy, Case School of Applied Science, Cleveland, Ohio	Residual stresses and stress relief in welded tubing.
Prof. M. F. Sayre	Department of Civil Engineering, Union College, Schenectady, New York	1. Development of methods of testing welds. 2. Study of cracks in welds.
Prof. Gilbert S. Schaller	Department of Mechanical Engineering, University of Washington, Seattle, Washington	1. Development of an arc with a gaseous atmosphere. 2. Oxyacetylene welding of cast iron.
Prof. A. H. Sluss and V. M. Smith	Department of Mechanical Engineering, University of Kansas, Lawrence, Kansas	Fatigue testing of fusion welded butt joints.
Prof. E. D. Soderstrom and W. H. Rice	Department of Industrial Arts, Oklahoma A. & M. College, Stillwater, Oklahoma	
Prof. A. Solakian	New York	Photoelastic studies.
Prof. W. A. Spindler	Department of Metal Processing, University of Michigan, Ann Arbor, Michigan	Welding cast iron.
Dean S. S. Steinberg	College of Engineering, University of Maryland, College Park, Maryland	
Dr. S. T. Stephenson	State College of Washington, Pullman, Washington	
Profs. J. R. Stitt and J. A. Foust	Department of Industrial Engineering, Ohio State University, Columbus, Ohio	1. Warpage of mild steel caused by arc welding. 2. Shrinkage of mild steel caused by arc welding. 3. Study of procedures to overcome both warpage and shrinkage in arc welding mild steel. 4. Development of methods of testing welds.
Dr. F. J. Studer	Department of Physics, Union College, Schenectady, New York	Contact resistance in spot welding.
Dr. C. G. Suits	Research Laboratory, General Electric Company, Schenectady, New York	Physics of the arc.
Dr. Eric Therkelsen and G. J. Pesman	Department of Mechanical Engineering, Montana State College, Bozeman, Montana	Distortion of welded pipe structures.
R. D. Thomas, Jr.	Arcos Corporation, Philadelphia, Pennsylvania	
Malcolm Thomson	Welding Department, General Electric Company, West Lynn, Massachusetts	
Dr. William T. Thomson	Kansas State College of Agricultural and Applied Science, Manhattan, Kansas	
Prof. G. E. Thornton	Department of Mechanical Engineering, State College of Washington, Pullman, Washington	Fatigue tests.
Prof. W. T Tiffin	School of Mechanical Engineering, University of Oklahoma, Norman, Oklahoma	Inspection by means of trepanning.
Prof. G. E. Troxell	Engineering Materials Laboratory, University of California, Berkeley, California	
Dr. A. E. Vivell	Department of Electrical Engineering, Princeton University, Princeton, New Jersey	Non-destructive tests.
Dr. R. W. Vose	Harvard University, Cambridge, Mass.	Influence of the welding heat on the structure of the adjacent metal.
Prof. N. F. Ward	Department of Mechanical Engineering, University of California, Berkeley, California	1. Creep tests of arc welds in steel. 2. Resistance welding thin-gage duralumin. 3. Stress distribution in welded Vierendeel trusses.
W. L. Warner	Watertown Arsenal, Watertown, Massachusetts	1. Heat effect of arc welding various alloy steels. 2. Effect of preheating on weldability of these steels.
Prof. K. F. Wendt	College of Engineering, University of Wisconsin, Madison, Wisconsin	Tests of low-alloy steels.
Dean A. E. White	Division of Engineering Research, University of Michigan, Ann Arbor, Michigan	1. Creep of metals and welded structures at high temperature. 2. Resistance to oxidation, embrittlement, and fatigue. 3. Stresses in annealed and unannealed welded pipe.

Prof. W. M. Wilson	Department of Civil Engineering, University of Illinois, Urbana, Illinois	Fatigue tests on large welded specimens.
Prof. S. F. Yasines	Department of Civil Engineering, New York University, New York, New York	Notch-impact tests of welds in low-alloy steel.
Dr. M. Yatsevitch	Watertown Arsenal, Watertown, Massachusetts	Coefficient of expansion of weld metal.
Prof. A. P. Young and R. R. Seeber	Department of Mechanical Engineering, Michigan College of Mining and Technology, Houghton, Michigan	1. Copper welding. 2. Methods of testing welds.

INDUSTRIAL RESEARCH DIVISION

THE Executive Committee of the Division has held three meetings during the year. A meeting of the Division was held during the annual meeting of the AMERICAN WELDING SOCIETY on Monday evening, October 23rd, in Chicago. The total membership of the Division and committees is now about 125. There have been no changes in the organization of the Division.

This Division was organized about three years ago as a cooperative effort and differs from other research committees in that the scope of its activities covers a rather broad field and in that when organized it took over no specific project under which it might appeal to industry for technical and financial assistance.

The activities of the various committees of the Division corresponds to the various stages of the development of cooperative research. These stages may be classified as roundtable discussions, cooperative research, preparation of programs for project research, and project research.

The personnel of committees of the Division have been selected to include the foremost experts in various fields and to represent the various phases and interests of the welding industry. Meetings of these committees develop into roundtable discussions of problems within their scope. There is a comparison of experience and a disclosure of work carried on or known to the various committee members. Lack of information in respect to some phases of the problem becomes apparent. There follows an agreement between committee members to carry out an investigation within their own organization or a plan to interest others in such work. Were no other results to follow from committee discussions industry would be well repaid for the cost of these meetings by the analysis of problems and the stimulation of investigations to solve them and the elimination of duplication of effort.

The second stage mentioned is cooperative research. This results logically from the discussion at roundtable committee meetings. A program within a certain scope of work is drawn up and there is formed an agreement between organizations represented on the committee to undertake certain phases of work and to report it for general information. A great deal of the activity of committees is carried on in this manner.

There are many investigations which require the utilization of special laboratory facilities and trained personnel. The work can best be conducted as a project under cooperative financial assistance. Outside of the organization for financing the project, which is a function of the Welding Research Committee, the problem of the technical committee is outlining the scope of the investigation and the preparation of the program. This stage of committee activity presupposes a knowledge of research work carried on here and abroad and involves the summarizing of existing knowledge.

The objective of the program is the execution of the project under adequate financial assistance. A research committee charged with the execution of a project is in a fortunate position of supplying fundamental and authoritative knowledge. There is no question as to the value of service rendered by such an organization. But the successful operation of this Division does not depend upon the organization and financing of research projects to all its various committees.

The work of the Division is in all these stages.

COMMITTEE II—CARBON STEEL

J. C. Hodge, Chairman

The original program of the Carbon Steel Committee was published early in 1938. During the annual meeting a year ago, the committee modified its program to provide preliminary tests on three analyses of steel to be made in three heats, one electric arc furnace, one open hearth large grain size, and one open hearth fine grain size. The program was further modified in a meeting of the Committee held on June 27th last to include the study of rimmed steel as well as killed steel. Otherwise the program was considered satisfactory.

This program was developed from request for information received from A. S. T M. The committees of that organization had experienced difficulty in fixing carbon limitations in weldable steel and it was felt that exact information should be available for specification purposes. The Committee therefore planned a program to study the effect of both carbon and manganese on the properties of the welded joint and upon the weldability of material. There has been no consensus of opinion in industry as to what tests are best to study the weldability of any material.

In the meantime there is a definite lack of knowledge as to the behavior of certain elements in steel, both in the weld metal but especially in the heat-affected zone. Considerable work has been done in outside laboratories in studying these problems and we may hope to have available from such sources considerable information as to the behavior of carbides in the presence of various elements during the heating and cooling cycles of welding operation.

Some funds are available subject to the raising of the complete amount required. The Steel Founders' Society is definitely interested in the carrying out of this program for the development of weldable steels in casting and have set aside $1000 for the preliminary program. One of the bureaus of the Navy Department and a number of fabricators have also indicated a financial interest in the program.

COMMITTEE III—LOW-ALLOY STEELS

J. H. Critchett, Chairman

At a meeting held last October in Detroit this Committee studied the possibilities of a questionnaire to low-alloy steel producers and users for the purpose of obtaining on standardized forms data concerning material and its welded joint. It was later decided that it was not advisable to send out such a questionnaire at the present time.

Because of the divergent views held in various industries as to what constitutes a test for weldability some of the most useful work done is the preliminary conference on this subject and also the clarification of ideas. There are special problems in some industries and their ideas as to weldability are formed in relation to those problems. In other industries a slightly different phase of the problem is emphasized. That has resulted in a different conception of tests needed. There is an opportunity of studying the fundamentals of the phenomena of welding and the accompanying modification of structure of the plate material which may yield some grounds of approach. After such an analysis the Committee will be in better condition to plan work needed in this field.

A contribution entitled "Weld Hardening of Carbon and Alloy Steels" in this field was made by H. J. French and T. N. Armstrong in a paper presented to the recent annual meeting of the AMERICAN WELDING SOCIETY.

COMMITTEE IV—HIGH-ALLOY STEELS

S. L. Hoyt, Chairman

This Committee met December 2, 1938, and made a comprehensive survey of problems requiring solution within its scope. The demand for cooperative efforts seems to be in the study of heat treatment, both for purposes of stress relieving and for purposes of protection against corrosion. Preliminary library research work was organized to obtain knowledge of work done in the past. Since that time the chairman, T. H. Nelson, found it necessary to resign the chairmanship on account of ill health. The Committee is being reorganized under the chairmanship of Dr. S. L. Hoyt, of Battelle Memorial Institute.

The reorganized committee has held a preliminary meeting and may be expected to organize promptly cooperative research work within the scope indicated.

COMMITTEE V—ALUMINUM ALLOYS

G. O. Hoglund, Chairman

This Committee initiated the work which led to the presentation of a paper on "An Investigation of Arc and Gas Welded Joints in Aluminum and Aluminum Alloys," by Lieutenant Commander R. K. Wells and A. G. Bissell, of the Navy Department, and a paper on resistance welding, "The Effect of Current, Pressure and Time on the Shear Strength and Structure of Spot Welds in the Aluminum Alloys," by G. O. Hoglund and G. S. Bernard, Jr.

The committee has made available information on the strength of welds in aluminum-manganese alloy plate and has been active in the development of information which is being used to establish specifications, codes and testing procedures, as applied to welded aluminum alloy parts.

The committee is concerned during the immediate future with the carrying out of an investigation on the strength of fusion welds in material of lighter gage than that reported on by Lieutenant Commander R. K. Wells and A. G. Bissell in the paper mentioned above. The committee is also concerned with the compilation of certain design details which are applied particularly to welded aluminum alloy vessels.

COMMITTEE VI—COPPER ALLOYS

D. K. Crampton, Chairman

No meeting of this Committee has been held during the past year. It has cooperated in the preparation of report on non-ferrous pressure vessels.

COMMITTEE VII—NICKEL ALLOYS

O. B. J. Fraser, Chairman

Work has proceeded during the year on two cooperative projects which were organized the previous year. The preliminary work on one project on the effect of wire temper on the physical properties of welds was carried on in the Westinghouse Research Laboratories in cooperation with the International Nickel Company. The final work on this program is being carried on in the International Nickel Company's research laboratories.

This Committee has a second project on the effect of arc length on the physical properties of welds of Monel and Inconel metals. The preliminary work of this project is being carried on at the U. S. Naval Experiment Station at Annapolis.

COMMITTEE A—METHODS OF TEST

M. F. Sayre, Chairman

During the preceding year this committee cooperated successfully with the American Society for Testing Materials in holding a symposium on impact testing. During the current year the committee organized the round-table discussion on weldability which formed a part of the annual program of this Division during the annual meeting of the AMERICAN WELDING SOCIETY. The primary objectives of this program were to ascertain from the various industries the problems experienced in welding, the tests developed to determine weldability and their opinions as to unsolved problems and promising tests.

COMMITTEE C—WELD STRESSES

Everett Chapman, Chairman

This Committee has held three meetings during the past year to consider various proposals for the study of weld stresses and the behavior of materials under multi-axial stresses. Last February a program was set up for the general study of biaxial stresses in the elastic and plastic ranges under the direction of Professor Joseph Marin at Rutgers University. This program involved particularly the study of torsion and axial tension stresses, and also of tension and tension stresses in an ellipsoidal shell. An allocation of $950.00 was made for this work. The approved program was not started as Professor Marin severed his connection with Rutgers

University and is now located at the Armour Institute at Chicago.

This program has not been satisfactory to the Committee as it involved the repetition of some work done. The need for information is in behavior of materials under repeated multi-axial stresses. The Committee now has under study the possibilities of carrying out investigations involving the fatigue of metal in hollow cylinders under repeated stresses from longitudinal tension and radial pressure. Such information is lacking not only on welds but also on the behavior of the base metal. Consideration is being given to obtaining the pulsating tension stresses by a machine similar to that developed at the University of Illinois for fatigue investigations and pulsating internal pressures in synchronism with the tension loading. The program involves the construction of the machine and the application of laboratory equipment for the measurement of internal pressures as well as the measurement of stress and strain in the test specimen. The project is under study at Armour Institute to determine whether facilities and equipment as well as operating personnel can be provided with the limited financial assistance available.

During the Chicago meeting Prof. Joseph Marin and R. C. Stanley presented a paper entitled "Failure of Aluminum Subjected to Combined Stresses."

COMMITTEE D—NON-DESTRUCTIVE TESTS

H. H. Lester, Chairman

This Committee met in February and after a study of various non-destructive tests initiated investigations of the possibilities of developing electrical resistance methods for the test of welds. The Committee took note of work being done in the development of methods and standards for non-destructive inspection under way at industrial laboratories. It is also following the development of magnaflux tests.

The Committee has under way a small project at the Massachusetts Institute of Technology under Dr. J. T. Norton in a study in secondary radiation.

This Committee is organized under the same chairman as Committee E-7 of A. S. T. M. which deals with radiographic tests. The research activities carried on by E-7 are available, therefore, to Committee D. Work in progress or contemplated include: studies to determine relationships between porosity as shown in X-ray pictures and tension and impact properties of weld metal; preparation of a recommended practice for radiographic testing of castings and weldments; preparation of recommended safety regulations for radiography; preparation of specifications for the radiographic inspection of structural welds and weldments; preparation of correlated abstracts in non-destructive testing for 1939.

COMMITTEE E—RESISTANCE WELDING

G. S. Mikhalapov, Chairman

Committee E has held two meetings and a subcommittee meeting during the past year.

In the preceding year the Committee had planned a comprehensive program of elementary research, reported last year. During the past year actual research has been started in Rensselaer Polytechnic Institute under the direction of Dr. W. F. Hess, some of the results of which have been presented at the AMERICAN WELDING SOCIETY's Fall Meeting in a paper by Dr. Hess and R. A. Wyant entitled "An Investigation of the Spot Welding of Automobile Grade Mild Steel." This is in addition to work being reported by Dr. Hess on the fundamental aspects of spot welding which the Fundamental Research Division is sponsoring.

The Committee has continued its investigation of the methods of testing of spot welds and expects to make a recommendation covering all static tests this Fall.

An attempt has been made to determine the needs of aircraft industries in so far as resistance welding research is concerned to which purpose a meeting was held between some of the Committee members and the representatives of several aircraft concerns and government agencies interested in aeronautics. A tentative program of most pressing research was drafted. It is planned to begin preliminary research at the factories of several of the aircraft manufacturers and with the cooperation of The Aluminum Company of America.

COMMITTEE F—FATIGUE TESTING (STRUCTURAL)

Jonathan Jones, Chairman

This Committee has held one meeting since last October and met again during October of this year to prepare an extension of its current program.

Committee F was organized to formulate and administer a program, and in accordance with the stipulations of the two principal contributors the research was planned for execution under Professor Wilson at Illinois. The general program, first stage, was formulated at a Committee meeting held in February 1938, and details have been adjusted at Committee or Subcommittee meetings from time to time.

The work accomplished to date has been summarized in a paper presented by Professor Wilson before the AMERICAN WELDING SOCIETY, October 23, 1939. Pronouncements of Committee conclusions have not yet been made, but will be formally made in due course.

The United States Navy, Bureau of Construction and Repair, has joined Committee F, and has introduced two relatively small programs of its own; one a comparison of hull splices in riveting and in butt welding, which program has been completed; the other a test of the joints, as between hull and bulkhead plates. In each instance the Navy has made a substantial contribution to pay its way, and its programs have been welcomed as bearing on the Committee's general field.

No program other than at Illinois is now contemplated. It will some day be desirable to organize collateral and confirming work elsewhere; but the funds now pledged cannot be so used, and it is probably not a great disadvantage to continue studying the elements of the problem in one place for a time.

The Committee has tried to tie its research directly into bridge design as it exists and may be expected to exist. It is paying a minimum of attention to matters which have to do with the philosophy of fatigue but cannot be applied to, or modify, practice. Thus, it has not studied the effect of annealing upon fatigue strength because it does not conceive that an annealed bridge can yet be manufactured and sold. Eventually all of this may change, but for the present the Committee is trying to produce statistical information regarding constructions that have been used or can be seriously proposed as of today's industrial situation.

The Committee has, by its "Rest *vs.* No-Rest" program, proved that the technique of its research is suitable to reveal what may be expected of bridge structures in service; and even more so, therefore, of structures

which undergo still more rapid or frequent reciprocating stresses.

It has produced statistical fatigue values for butt-welded joints in carbon steel, with and without extra treatments such as leveling off of reinforcement; it has thereby enabled the designing engineer to study the economics of this or that type of butt-welded joint. It has not yet investigated other grades of steel, but expects to do so.

It has only scratched the surface of fillet weld fatigue strengths. Its work to date has shown the extreme danger that lies in that joint as it frequently has been, and normally might be expected to be, designed. It must continue in the hope of devising fillet weld joint designs that will come very much closer to developing the fatigue strength of the parent material.

There is a place for research in this field that will continue to call for support from industry for several years to come. The results of the research may or may not expand the structural use of welding; but they are essential in order that what is done may be honestly based on better knowledge than we now have.

COMMITTEE ON NONFERROUS PRESSURE VESSELS

C. W. Obert, Chairman

The Committee, after an extended study, made final report and found that work on aluminum and its alloys to determine strength characteristics, and the investigation of behavior of aluminum and aluminum alloys pressure vessels in comparison with steel, is being carried on by the Aluminum Company of America.

In case of copper alloys there was found little need for further research work except possibly in connection with copper-silicon alloys.

No special research program appeared to be needed for the application of the nickel alloys in the construction of pressure vessels.

PRINCIPAL FINANCIAL NEEDS OF THE DIVISION

The status of the weldability project has been given in the report of Committee II.

Preliminary indications are that a total $20,000 will be needed for this work.

The weld stress project which is now engaging the attention of Committee C is tentatively financed. For more than a development of the method of study additional funds will be required. The project under study covers but one element of the complicated problems of the cause and effect of weld stresses.

Work of this nature requires precision and special laboratory equipment and the concentrated effort of specially trained personnel. Some of our university laboratories are equipped for such work. A few industrial laboratories can undertake it. Investigations of this nature are ideal for cooperative financing.

It is believed that industry would be well recompensed to spend $5000 a year on well organized projects in this field.

Committee E (Resistance Welding) programs involve work far beyond the capacity of personnel for a reasonable period of time. The amount now allocated to the project at Rensselaer Polytechnic Institute is sufficient for one research associate. Facilities there are adequate for the assignment of additional personnel. It is believed that because of the large amount of work involved it would be highly advantageous to the resistance welding industry if $5000 were made available annually for this project.

The amount of work which should be done under the fatigue project exceeds the capacity of the equipment available at the University of Illinois. Additional machines could be operated without increase of personnel. Under the type of loading required the existing machines cannot be expected to remain in operation indefinitely without services being delayed due to fatigue failures. Committee F is desirous of speeding up their program because of demands for definite information on the behavior of welded joints in fatigue. Eight thousand dollars in addition to present available funds should be made available to that committee for additional equipment. With additional equipment operating costs per test specimen would be correspondingly decreased. Funds are available for operating expenses of this committee during the coming year.

PERSONNEL INDUSTRIAL RESEARCH DIVISION

Membership

* Jenks, Colonel, G. F., *Chairman;* Office of the Chief of Ordnance, War Department, Washington, D. C.
* Adams, C. A., Edward G. Budd Manufacturing Company, Philadelphia, Pa.
Bibber, L. C., Welding Engineer, Carnegie-Illinois Steel Corporation, Carnegie Bldg., Pittsburgh, Pa.
Boardman, H. C., Director of Research, Chicago Bridge and Iron Company, 1305 West 105th Street, Chicago, Ill.
*Chapman, Everett, President, Lukenweld, Inc., Coatesville, Pa.
Clark, R. W., Works Laboratory Bldg. 7, General Electric Company, Schenectady, N. Y.
*Crampton, Dr. D. K., Research Director, Chase Brass and Copper Company, Waterbury, Conn.
*Critchett, J. H., Vice-President, Union Carbide and Carbon Research Laboratories, Inc., 30 East 42nd St., New York, N. Y.
*Crowe, J. J., Engineer in Charge of Apparatus, Research and Development Department, Air Reduction Sales Company, 181 Pacific Ave., Jersey City, N. J.
Deppeler, J. H., Chief Engineer and Works Manager, Metal & Thermit Corporation, 120 Broadway, New York, N. Y.
Eksergian, C. L., Chief Engineer, Budd Wheel Company, 12141 Charlevoix Ave., Detroit, Mich.
*Fraser, O. B. J., Superintendent, Technical Service, The International Nickel Company, Inc., 67 Wall Street, New York, N. Y.
Freeman, John R., Jr., Technical Manager, The American Brass Co., Waterbury, Conn.
French, Dr. H. J., In Charge, Alloy Steel and Iron Development, Development and Research Department, The International Nickel Company, Inc., 67 Wall Street, New York, N. Y.
Gibson, A. E., President, The Wellman Engineering Company, Cleveland, Ohio.
Halsey, W. D., Assistant Chief Engineer, Boiler Division, The Hartford Steam Boiler Inspection and Insurance Co., 56 Prospect St., Hartford, Conn.
*Hobart, H. M., Chairman, Fundamental Research Division; Consulting Engineer, General Electric Company, Schenectady, N. Y.
*Hodge, Dr. J. C., Chief Metallurgist, The Babcock and Wilcox Company, Barberton, Ohio.
*Hoglund, G. O., Welding Engineer, Aluminum Company of America, P. O. Box 772, New Kensington, Pa.
Hoyt, Dr. S. L., Battelle Memorial Institute, Columbus, Ohio.
Jasper, T. M., Director of Research, A. O. Smith Corporation, Milwaukee, Wis.
Jennings, C. H., Engineer in Charge of Welding Research, Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.
Johnson, J. B., Chief, Material Branch, Wright Field, Dayton, Ohio.
*Jones, Jonathan, Chief Engineer, Bethlehem Steel Company, 701 East Third Street, Bethlehem, Pa.
Jones, S. O., Welding Engineer, Newport News Shipbuilding and Dry Dock Company, Newport News, Va.
Kelley, E. F., Public Roads Administration, Washington, D. C.
Kidd, A., Assistant Works Manager, The M. W. Kellogg Company, Foot of Danforth Ave., Jersey City, N. J.
Lang, P. G., Jr., Engineer of Bridges, Baltimore and Ohio Railroad Company, Baltimore, Md.
*Lester, Dr. H. H., Senior Physicist, Watertown Arsenal, Watertown, Mass.

Lincoln, J. F., President, The Lincoln Electric Company, Cleveland, Ohio.
Loos, C. E., Carnegie-Illinois Steel Co., Frick Annex Bldg., E. Pittsburgh, Pa.
Madsen, Lieut. H. V. B., Bureau of Construction and Repair, Navy Department, Washington, D. C.
*Mikhalapov, G. S. Baldwin-Southwark Co., Philadelphia, Pa.
Miller, H. L., Metallurgical Department, Republic Steel Corp., Massillon, Ohio.
*Mochel, N. L., Metallurgical Engineer, Westinghouse Electric and Manufacturing Company, So. Philadelphia Works, Lester Station, Philadelphia, Pa.
Nelson, T. H., Consulting Metallurgist, The Midvale Steel Company, Spring Mill Road, Villanova, Pa.
Norton, J. T., Associate Professor of Physical Metallurgy, Massachusetts Institute of Technology, Cambridge, Mass.
Officer-in-Charge, Specifications Section, Design Division, Bureau of Engineering, Navy Department, Washington, D. C.
*Sayre, M. F., Professor, Department of Applied Mechanics, Union College, Schenectady, N. Y.
Seabloom, Eric, Crane Company, 4100 So. Kedzie Avenue, Chicago, Ill.
Schenck, Charles, Engineer of Development, Bethlehem Steel Company, Bethlehem, Pa.
*Spraragen, W., Executive Secretary, Welding Research Committee, Engineering Foundation, 29 West 39th Street, New York, N. Y.
Underwood, C. M., Senior Welding Engineer, U. S. Naval Gun Factory, Washington, D. C.
Director, American Railway Research Advisory Committee, 59 East Van Buren Street, Chicago, Ill.
Weigel, A. C., Vice-President, Combustion Engineering Corporation, 200 Madison Ave., New York, N. Y.
Wilson, James W., Senior Marine Engineer, Bureau of Marine Inspection and Navigation, Department of Commerce, Washington, D. C.

* Executive Committee Member.

COMMITTEE ORGANIZATION

Material Committees:

I. Cast Iron (Not yet organized).

II. Carbon Steels, J. C. Hodge, *Chairman;* The Babcock and Wilcox Company, Barberton, Ohio.

Members:

Aborn, R. H., United States Steel Corporation, Kearny, N. J.
Bagsar, A. B., Sun Oil Company, Marcus Hook, Pa.
Briggs, Dr. C. W., Steel Founders' Society of America, 920 Midland Bldg., Cleveland, Ohio.
Chapman, E. C., Hedges-Walsh-Weidner Company, Chattanooga, Tenn.
Critchett, J. H., Union Carbide and Carbon Research Laboratories, Inc., New York.
Deppeler, J. H., Metal and Thermit Corporation, New York.
Douglass, A. S., The Detroit Edison Company, Detroit, Mich.
Ffield, Paul, Bethlehem Shipbuilding Corporation, Quincy, Mass.
Fyke, F. C., Standard Oil Development Corporation, Elizabeth, N. J.
Madsen, H. V. B., Bureau of Construction and Repair, Navy Dept., Washington, D. C.
Mochel, N. L., Westinghouse Electric and Manufacturing Company, Philadelphia, Pa.
Navy Department, Officer-in-Charge, Specifications Section, Design Division, Bureau of Engineering, Washington, D. C.
Schenck, Charles, Bethlehem Steel Company, Bethlehem, Pa.
Theisinger, W. G., Lukens Steel Company, Coatesville, Pa.

Subcommittee II

Effect of Carbon and Manganese on the Weldability of Steel, W. G. Theisinger, *Chairman;* Lukens Steel Company, Coatesville, Pa.

Members:

Bagsar, A. B., Sun Oil Company, Marcus Hook, Pa.
Navy Department, Officer-in-Charge, Specifications Section, Design Division, Bureau of Engineering, Washington, D. C.

III. Low-Alloy Steels, J. H. Critchett, *Chairman;* Union Carbide and Carbon Research Laboratories, Inc., New York.

Members:

Bibber, L. C., Carnegie-Illinois Steel Corporation, Pittsburgh, Pa.
Chapman, Everett, Lukenweld, Inc., Coatesville, Pa.
Crafts, Walter, Secretary; Union Carbide and Carbon Research Lab., Niagara Falls, N. Y.
Crawford, W. E., A. O. Smith Corporation, Milwaukee, Wis.
Deppeler, J. H., Metal and Thermit Corporation, New York.
Gibson, A. E., The Wellman Engineering Company, Cleveland, Ohio.
Hodge, J. C., The Babcock and Wilcox Company, Barberton, Ohio.
Hopkins, R. K., The M. W. Kellogg Company, Jersey City, N. J.
Jennings, C. H., Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.
Johnson, J. B., Air Corps, Wright Field, Dayton, Ohio.
Miller, H. L., Republic Steel Corporation, Massillon, Ohio.
Mochel, N. L., Westinghouse Electric and Manufacturing Co., Lester Branch Post Office, Philadelphia, Pa.
Roop, W. P., Lieut. Comdr., U. S. Experimental Model Basin, Navy Yard, Washington, D. C.
Bureau of Construction & Repair, U. S. Navy
Lieut. P. W. Snyder (C. C.)—Representative
A. G. Bissell, Alternate
Theisinger, W. G., Lukens Steel Company, Coatesville, Pa.
Thum, E. E., Editor, Metal Progress, Cleveland, Ohio.
Warner, W. L., Watertown Arsenal, Watertown, Mass.

IV. High-Alloy Steels, Dr. S. L. Hoyt, *Chairman;* Battelle Memorial Inst., Columbus, Ohio.

Members:

Aborn, R. H., U. S. Steel Corp., Kearny, N. J.
Bibber, L. C., Carnegie-Illinois Steel Corporation, Pittsburgh, Pa.
Chapman, E. C., Hedges-Walsh-Weidner Company, Chattanooga, Tenn.
Forker, E., Blaw-Knox Company, Pittsburgh, Pa.
Franks, Russell, Union Carbide & Carbon Lab., Niagara Falls, N. Y.
Johnson, A. C., Downington Iron Works, Downington, Pa.
Krivobok, V. N., Allegheny Steel Company, Pittsburgh, Pa.
Lichtenwalter, T. R., Republic Steel Corporation, Massillon, Ohio.
Mathias, D. L., Metal and Thermit Corporation, New York.
Maurauth, G. A., Maurauth, Inc., Cleveland, Ohio.
Maxwell, H. L., E. I. du Pont de Nemours and Company, Wilmington, Del.
Mochel, N. L., Westinghouse Electric and Manufacturing Company, Philadelphia, Pa.
Nelson, T. H., Midvale Steel Co., Villanova, Pa.
Thomas, R. D., Arcos Corporation, Philadelphia, Pa.
U. S. Navy, Bur. of Constr. & Repair, H. V. Madsen, Rep.
Vincent, C. R., Jr., Alloy Fabricators, Inc., Newark, N. J.
Winlock, Joseph, Edward G. Budd Manufacturing Company, Philadelphia, Pa.

V. Aluminum Alloys, G. O. Hoglund, *Chairman;* Aluminum Company of America, New Kensington, Pa.

Members:

Hibert, C. L., Consolidated Aircraft Corporation, San Diego, Calif.
Jones, S. O., Newport News Shipbuilding and Dry Dock Company, Newport News, Va.
Navy Department, Bureau of Construction and Repair, Represented by Lieut. P. W. Snyder (C. C.)—A. G. Bissell, Alternate, Washington, D. C.
Rockefeller, H. E., The Linde Air Products Company, New York, N. Y.
Smith, E. W. P., The Lincoln Electric Company, Cleveland, Ohio.

VI. Copper Alloys, D. K. Crampton, *Chairman;* Chase Brass and Copper Company, Waterbury, Conn.

Members:

Cecil, R. E., Wm. B. Scaife & Sons Company, Oakmont, Pa.
Freeman, John R., Jr., The American Brass Company, Waterbury, Conn.
Gallo, F. A., J. L. White Welding Company, Waterbury, Conn.
Jennings, C. H., Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.
Lincoln, J. F., The Lincoln Electric Company, Cleveland, Ohio.
Reed, W. C., General Electric Company, Pittsfield, Mass.
Wilkins, R. A., Revere Copper and Brass, Inc., Rome, N. Y.
Wood, M. L., Secretary; Plant Research Section, Chase Brass & Copper Co., Waterbury, Conn.

VII. Nickel Alloys, O. B. J. Fraser, *Chairman;* The International Nickel Company, New York, N. Y.

Members:

Bash, F. E., Driver-Harris Company, Harrison, N. J.
Deppeler, J. H., Metal and Thermit Corporation, New York, N. Y.
Johnson, J. B., Air Corps, Wright Field, Dayton, Ohio.
Kosting, Dr. P. R., Watertown Arsenal, Watertown, Mass.

Maxwell, H. L., E. I. du Pont de Nemours and Company, Wilmington, Del.
Mochel, N. L., Westinghouse Electric and Manufacturing Company, Philadelphia, Pa.
Jones, S. O., Newport News Shipbuilding and Dry Dock Company, Newport News, Va.
Navy Department, Officer-in-Charge, Specifications Section, Design Division, Bureau of Engineering, Washington, D. C.
Theisinger, Dr. W. G., Lukens Steel Company, Coatesville, Pa.
Vincent, C. R., Jr., Alloy Fabricators, Inc., Newark, N. J.

Functional Committees:

A. Methods of Testing, M. F. Sayre, *Chairman;* Union College, Schenectady, New York.

Members:

deForest, A. V., Massachusetts Institute of Technology, Cambridge, Mass.
Freeman, John R., Jr., The American Brass Company, Waterbury, Conn.
Hopkins, R. K., M. W. Kellogg Co., Jersey City, N. J.
Howell, F. M., Aluminum Company of America, New Kensington, Pa.
Jennings, C. H., Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.
Kinzel, A. B., Union Carbide and Carbon Research Laboratories, New York, N. Y.
Loos, C. E., Carnegie-Illinois Steel Co., Pittsburgh, Pa.
Madsen, H. V. B., Bureau of Construction & Repair, Navy Dept., Washington, D. C.
Warner, W. L., Watertown Arsenal, Watertown, Mass.

B. Analysis of Weld Failures (Not yet organized).

C. Weld Stresses—Causes and Effects—Everett Chapman, *Chairman;* Lukenweld, Inc., Coatesville, Pa.

Members:

Eksergian, Rupen, Edward G. Budd Manufacturing Co., Philadelphia, Pa.
Eriksen, E. L., University of Michigan, Ann Arbor, Mich.
Fairchild, F. P., Public Service Co., Newark, N. J.
Den Hartog, J., Assoc. Prof. of Applied Mechanics, Graduate School of Engineering, Harvard University, Cambridge, Mass.
Hollister, Prof. S. C., Cornell Univ., Ithaca, N. Y.
Kinzel, A. B., Union Carbide and Carbon Research Laboratories, Inc., New York, N. Y.
MacGregor, Prof. Charles W., Massachusetts Institute of Technology, Cambridge, Mass.
Marchant, Dr. J. H., State College, Storrs, Conn.
Marin, Prof. Joseph, Armour Inst. of Tech., Chicago, Ill.
Nadai, A., Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.
Officer-in-Charge, Specification Section, Design Division, Bureau of Engineering, Navy Department, Washington, D. C.
Sayre, Prof. M. F., Union College, Schenectady, N. Y.
Winlock, Joseph, Edward G. Budd Manufacturing Co., Philadelphia, Pa.

D. Non-Destructive Tests, H. H. Lester, *Chairman;* Watertown Arsenal, Watertown, Mass.

Members:

Adams, C. A., Budd Manufacturing Company, Philadelphia, Pa. (Advisory).
Carpenter, O. R., The Babcock and Wilcox Company, Barberton, Ohio.
Doan, Gilbert, Lehigh University, Bethlehem, Pa.
deForest, A. V., *Vice-Chairman;* Massachusetts Institute of Technology, Cambridge, Mass.
Jackson, T. M., Sun Shipbuilding & Dry Dock Co., Chester, Pa.
Kouwenhoven, W. B., Johns Hopkins University, Baltimore, Md.
Lucas, Dr. F. F., Bell Telephone Laboratories, New York.
Mochel, N. L., Westinghouse Electric and Manufacturing Company, Philadelphia, Pa.
Norton, Prof. J. T., Massachusetts Institute of Technology, Cambridge, Mass.
Vivell, A. E., *Secretary;* Princeton University, Princeton, N. J.
Warner, W. L., Watertown Arsenal, Watertown, Mass.

E. Resistance Welding, George S. Mikhalapov, *Chairman;* Baldwin-Southwark Co., Philadelphia, Pa.

Members:

Bibber, Leon C., Carnegie-Illinois Steel Corporation, Pittsburgh, Pa.
Bowman, Robert, Wright Field, Dayton, Ohio.
Crawford, W. E., A. O. Smith Corporation, Milwaukee, Wis.
Eksergian, C. L., Budd Wheel Company, Detroit, Mich.
Fraser, O. B. J., The International Nickel Co., Inc., 67 Wall Street, New York.
Gathman, M. B., Fisher Body Corporation, Detroit, Mich.
Gillette, R. T., General Electric Company, Schenectady, N. Y.
Heitman, Charles, E. G. Budd Manufacturing Company, Philadelphia, Pa.
Hensel, Dr. F. R., P. O. Box 1361, Indianapolis, Ind.
Hess, Dr. Wendell F., Rensselaer Polytechnic Institute, Troy, N. Y.
Hoglund, G. O., Aluminum Company of America, New Kensington, Pa.
Humphrey, S. M., Taylor-Winfield Corporation, Warren, Ohio.
Hutchins, W. C., General Electric Company, Schenectady, N. Y.
Jennings, C. H., Westinghouse Electric and Manufacturing Co., East Pittsburgh, Pa.
Merriam, Paul, Glenn L. Martin Company, Baltimore, Md.
Navy Department, Bureau of Aeronautics, Washington, D. C.—represented by Harry J. Huester.
Navy Department, Bureau of Construction and Repair—represented by A. J. Raymo, Philadelphia Navy Yard.
Navy Department, Officer-in-Charge, Specifications Section, Design Division, Bureau of Engineering, Washington, D. C.
Pfeiffer, C. L., Western Electric Co., Hawthorne Works, Chicago, Ill.
Priest, H. M., U. S. Steel Subsidiary, Railroad Research Bureau, Pittsburgh, Pa.
Roby, F. H., Square D Company, Milwaukee, Wis.
Unger, A. M., Pullman-Standard Car Mfg. Co., Chicago, Ill.
Wood, M. L., Plant Research Section, Chase Brass & Copper Co., Waterbury, Conn.

F. Fatigue Testing (Structural), Jonathan Jones, *Chairman;* Bethlehem Steel Company, Bethlehem, Pa.

Members:

Bibber, L. C., Carnegie-Illinois Steel Corporation, Pittsburgh, Pa.
Boardman, H. C., Chicago Bridge and Iron Company, Chicago, Ill.
Bruckner, Prof. W. H., University of Illinois, Urbana, Illinois.
Carpenter, A. W., New York Central Railroad, New York, N. Y.
Chapman, Everett, Lukenweld, Inc., Coatesville, Pa.
Frankland, F. H., American Institute of Steel Construction, New York, N. Y.
Gemeny, A. L., Public Roads Adminstration, Washington, D. C.
Goodrich, C. F., American Bridge Company, Pittsburgh, Pa.
Grover, LaMotte, Air Reduction Sales Company, New York, N. Y.
Grover, O. L., Public Roads Administration, Washington, D. C.
Hollister, S. C., Dean, College of Engineering, Cornell University, Ithaca, New York
Hopkins, Walter C., State Roads Commission, Baltimore, Maryland.
Hovey, Otis E., Consulting Engineer, 11 West 42nd Street, New York, N. Y.
Jenks, G. F., Colonel, Ordnance Department, U. S. Army, Washington, D. C.
Moisseiff, Leon S., Consulting Engineer, 99 Wall St., New York, N. Y.
Moss, H. H., The Linde Air Products Company, Newark, N. J.
Navy Department, Bureau of Construction and Repair, Washington, D. C.—Represented by Lieut. P. W. Snyder (C.C.).
Tinker, G. H., New York, Chicago and St. Louis Railroad, Cleveland, Ohio.
Wilson, Prof. W. M., University of Illinois, Urbana, Ill.
Spraragen, W., Executive Secretary, Welding Research Committee.

THE ENGINEERING FOUNDATION
WELDING RESEARCH COMMITTEE

★

CURRENT SUBSCRIBERS TO ACTIVITIES

Air Reduction Sales Company
American Bureau of Shipping
American Institute of Electrical Engineers
American Institute of Steel Construction, Inc.
American Iron and Steel Institute
American Petroleum Institute
American Transit Association
American Welding Society
Babcock and Wilcox Company
S. Blickman, Inc.
E. G. Budd Manufacturing Company
Champion Rivet Company
Chattanooga Boiler & Tank Co.
Chicago Bridge & Iron Company
Climax Molybdenum Company
Combustion Engineering Company
Consolidated Edison Company of New York, Inc.
Detroit Edison Company
Downington Iron Works
Duquesne Light Company
Electric Arc Cutting & Welding Company
Engineering Foundation
General Electric Company
Handy & Harman Company
International Nickel Company, Inc.
M. W. Kellogg Company
Lukenweld, Inc.
Metal and Thermit Corporation
National Cylinder Gas Company
Public Roads Administration, Federal Works Agency
Public Service Gas and Electric Company
Resistance Welder Manufacturers' Association
Shawinigan Products Corporation
Sun Shipbuilding & Dry Dock Company
Union Carbide and Carbon Company
U. S. Navy—Bureau of Construction and Repair
Utilities Research Commission
Henry Vogt Machine Company
Westinghouse Electric & Mfg. Company
Wyatt Metal & Boiler Works

★

COOPERATION INVITED

By distributing the cost among a number of companies, the burden on any one company is small and insignificant as compared with the value of the results received. Contributions may be made to the funds of the Main Committee, or earmarked for specific investigations.

The Engineering Foundation acts as Treasurer for the funds of the Committee. Checks may be made payable to The Engineering Foundation for the Welding Research Committee, and sent to 29 West 39th Street, New York.

WELDING RESEARCH COMMITTEE
29 WEST 39th STREET
NEW YORK, N. Y.

TENTATIVE PROGRAM

TWENTIETH ANNUAL MEETING
AMERICAN WELDING SOCIETY

WEEK OF OCTOBER 22, 1939

STEVENS HOTEL—CHICAGO, ILL.

National Metal Exposition, International Amphitheater—Open Afternoons and Evenings Throughout Entire Week

---

PRESIDENT'S RECEPTION—SUNDAY, OCTOBER 22nd, 5 to 7 P.M.

MONDAY, OCTOBER 23rd, Morning—9:30 A.M.

KEYNOTE ADDRESS

PRESENTATION OF MEDALS

TECHNICAL SESSION

Chairman, H. C. BOARDMAN
Chicago Bridge and Iron Company

Vice-Chairman, C. A. McCUNE
Magnaflux Corporation

GOVERNMENT CONSTRUCTION WORK

Welded Structures Under the Bureau of Yards and Docks, U. S. Navy,
by Commander C. A. TREXEL, Bureau of Yards and Docks, Navy Department

Bonneville Dam Presents Varied Use of Welding,
by PAUL L. HESLOP and FRED T. DOWNING, Corps of Engineers, U. S. Army

Motion Picture on Grand Coulee Dam

Afternoon—2:00 P.M.

SIMULTANEOUS TECHNICAL SESSIONS

INDUSTRIAL RESEARCH

Chairman, G. F. JENKS, Col.,
Ordnance Dept., U. S. Army

Vice-Chairman, C. H. JENNINGS,
Westinghouse Electric & Mfg. Co.

Some Aspects of the Welding of Alloy Steels
by H. J. FRENCH and T. N. ARMSTRONG,
International Nickel Co.

PRODUCTION WELDING IN LIGHT GAGE MATERIAL

Chairman, R. E. McFARLAND,
Western Elec. Co.

Vice-Chairman, E. A. BALSLEY,
Link Belt Co.

Industrial Research Continued

An Investigation of the Spot Welding of Automobile Grade Mild Steel
by W. F. HESS and R. A. WYANT,
Rensselaer Polytechnic Institute

Failure of Aluminum Subjected to Combined Stresses
by JOSEPH MARIN, Rutgers University

Fatigue Tests of Welded Joints in Structural Plates
by W. M. WILSON and A. B. WILDER,
University of Illinois

Production Welding Continued

Welding Tubing for the Automotive Industry
by G. C. GRIDLEY, Universal Joint Co.

Machine Welded Sheet Metal Parts for Enameling
by JOSEPH C. LEWIS, Associated Engineers, Inc.

Automatic Oxyacetylene Welding of Oil Floats
by H. E. LERNER, Taylor Sales Engineering Company

Evening—7:30 P.M.

Industrial Research Conference—Col. G. F. JENKS, **Chairman,** presiding

TUESDAY, OCTOBER 24th, Morning—9:30 A.M.
SIMULTANEOUS TECHNICAL SESSIONS

FUNDAMENTAL RESEARCH

Chairman, J. J. CROWE,
Air Reduction Sales Co.

Vice-Chairman, MILTON MALE,
U. S. Steel Corp. of Delaware

Application of Proximity Effect to Welding
by E. BENNETT, Univ. of Wisconsin

Arc Welding in Controlled Atmospheres
by G. E. DOAN, Lehigh University

The Effect of Alloying in Metallic Arc Welding
by R. W. EMERSON, Westinghouse Elec. & Mfg. Co.

The Effect of Flux Upon Grain Size in Welding Mild Steel
by HERMAN GRANBERRY,
Texas Technological College

Creep Tests of Arc Welded Low Carbon Steel
by N. F. WARD, University of California.

MACHINERY CONSTRUCTION

Chairman, A. E. GIBSON,
The Wellman Eng. Co.

Vice-Chairman, C. M. UNDERWOOD,
U. S. Naval Gun Factory

Use of Steel Castings and Rolled Steel Plate in Welded Fabrication
by H. J. SHIFFLI and EMIL KRECJI,
American Steel Foundries

Paper on Plate Edge Preparation for Welding

Flow of Materials in Production Welding
by W. W. PETRY, Cincinnati Milling Machine Co.

Welding Heavy Presses
by L. J. McDONOUGH and J. R. HENRY,
Clearing Machine Corp.

Afternoon—2:00 P.M.

SIMULTANEOUS TECHNICAL SESSIONS

FUNDAMENTAL RESEARCH

Chairman, K. L. HANSEN,
Harnischfeger Corp.

Vice-Chairman, A. M. CANDY,
Hollup Corporation

Further Studies of the Spot Welding of Low Carbon and Stainless Steels
by W. F. HESS and R. A. WYANT,
Rensselaer Polytechnic Institute

Reactions in Arc Welding
by R. DAVID THOMAS, JR. and F. H. RHODES,
Cornell University

Contact Resistance
by F. J. STUDER and MR. WINZENBURGER,
Union College

WELDING IN EQUIPMENT CONSTRUCTION

Chairman, J. W. OWENS,
Fairbanks-Morse Co.

Vice-Chairman, O. T. BARNETT,
Black, Sivalls & Bryson, Inc.

The Fabrication of Road Building Equipment
by L. L. BURGER, LaPlant-Choate Mfg. Co. Inc.

Designing Construction Machinery for Production by Welding
by H. C. HETTELSATER, Harnischfeger Corp.

Fundamental Research
Continued

Weld Inspection by Means of Trepanned Plugs
by W. T. TIFFIN, University of Oklahoma

Welding of Copper
by A. P. YOUNG,
Michigan College of Mining & Technology

Welding in Equipment Construction
Continued

Cutting and Welding Stainless Clad Steel
By W. B. KEELOR,
Ingersoll Steel & Disc Div., Borg-Warner Corp.

Large Equipment Fabrication
by LESLIE McPHEE, Whiting Corp.

Evening—7:30 P.M.

Fundamental Research Conference—H. M. HOBART, **Chairman,** presiding

---

WEDNESDAY, OCTOBER 25th, Morning—9:30 A.M.

SIMULTANEOUS TECHNICAL SESSIONS

FLAME HARDENING & HARD SURFACING

Chairman, M. M. WEIST,
Air Reduction Sales Co.

Vice-Chairman, R. S. KENRICK,
The Welding Engineer

Flame Hardening the Way Surfaces of Machine Tools
by FRED C. DULL,
The Monarch Machine Tool Company

Hard Facing by Fusion Welding
by T. B. JEFFERSON, U. S. Engineers

Flame Treating
by J. H. ZIMMERMAN,
The Linde Air Products Company

Flame Hardening from the Standpoint of the Commercial Heat Treater
by W. G. HAMILTON,
Accurate Steel Treating Co., Inc.

RESISTANCE WELDING

Chairman, G. S. MIKHALAPOV,
Heintz Mfg. Co.

Vice-Chairman, D. H. COREY,
Detroit Edison Company

Some Applications of the Resistance Welding Process
by C. L. PFEIFFER, Western Electric Company

Precision Welding by the Resistance Method
by E. J. BATES and G. J. OSWALD,
The National Cash Register Co.

Spot Welding of Aluminum
by R. KAUFFMANN, Aluminum Français

Some Studies of Flash Welding
by M. G. Yatsevitch, Watertown Arsenal

Afternoon—2:00 P.M.

SIMULTANEOUS TECHNICAL SESSIONS

WELDED PIPING, PRESSURE VESSELS AND BOILERS

Chairman, D. S. JACOBUS,
The Babcock & Wilcox Co.

Vice-Chairman, E. VOM STEEG, Jr.,
General Electric Company

Welding of Boilers and Pressure Vessels
by R. T. KERNOLL, Edge Moor Iron Works

Oxyacetylene Welding of Red Brass Pipe
by A. N. KUGLER, Air Reduction Sales Co.

90° Welded Nozzles, Piping—Calculations, Design and Fabrication
by F. C. FANTZ and W. G. HOOPER,
Midwest Piping & Supply Co. Inc.

Extensions of Fusion Welding in Special Pipe and Pipe Fitting Applications
by E. HALL TAYLOR, Taylor Forge and Pipe Works

RESISTANCE WELDING

Chairman, J. D. GORDON,
The Taylor Winfield Corp.

Vice-Chairman, M. S. CLARK,
Federal Machine & Welder Co.

Engineering Fundamentals of Resistance Welding
by S. M. HUMPHREY, The Taylor Winfield Corp.

High Speed Welding Assemblies
by E. W. FORKNER, Federal Machine & Welder Co.

Application and Use of Portable Spot Welders
by C. E. HEITMAN, Edward G. Budd Mfg. Co.

Latest Developments in Resistance Welding
by R. T. GILLETTE, General Electric Company

THURSDAY, OCTOBER 26th, Morning—9:30 A.M.

SIMULTANEOUS TECHNICAL SESSIONS

CUTTING & WELDING IN STEEL MILLS

Chairman, L. C. BIBBER
Carnegie-Illinois Steel Corp.

Vice-Chairman, H. E. ROCKEFELLER
The Linde Air Products Company

Repairs to Castings and Other Steel Mill Parts
by S. S. BAUMER, Bethlehem Steel Co.

Welding in a Steel Mill
by R. P. PALMER, Wheeling Steel Corp.

Flame Descaling and Flame Cleaning and Dehydrating
by J. G. MAGRATH, Air Reduction Sales Co.

WELDING IN STRUCTURAL & SHIP WORK

Chairman, EVERETT CHAPMAN
Lukenweld, Inc.

Vice-Chairman, A. G. BISSELL
Bureau of Construction & Repair, Navy Dept.

High Spots in Ship Welding
by E. D. DEBES, Bethlehem Shipbuilding Corp.

Designing Welded Frames for Continuity
by BRUCE JOHNSTON and E. H. MOUNT,
Lehigh University

Design and Fabrication of Welded Structures
by C. E. LOOS & F. H. DILL, American Bridge Co.

Thirteen Years of Welding Structural Steel
by A. L. WILSON,
Mississippi Valley Structural Steel Co.

Afternoon, 2:00 P.M.—**Committee Meetings; Visit Show**

Evening—**Annual Banquet**

FRIDAY, OCTOBER 27th, Morning—9:30 A.M.

SIMULTANEOUS TECHNICAL SESSIONS

RAILROAD SESSION

Chairman, P. G. LANG, Jr.
The Baltimore & Ohio R. R. Co.

Vice-Chairman, E. WANAMAKER,
Chicago, Rock Island & Pacific Ry. Co.

Maintenance of Way Welding
by C. E. MORGAN,
Chicago, Milwaukee, St. Paul & Pacific R. R.

Automatic Welding in the Design and Construction of Railroad Rolling Stock
by F. C. HASSE, Oxweld Railroad Service Co.

Production Spot Welding in the Manufacture of Freight and Passenger Cars
by J. W. SHEFFER,
American Car and Foundry Company

MISCELLANEOUS SESSION

Chairman, K. V. KING
Standard Oil Co.

Vice-Chairman, F. L. PLUMMER
Consulting Engineer

A Study of Welding Heat Effects
by Dr. W. A. PEARL, Armour Institute of Technology

Welding in the Manufacture of Valves for High Pressures and Temperatures
by W. F. CRAWFORD, Edward Valve & Mfg. Co.

Shrinkage of Steel Due to Welding
by SAMUEL T. SMETTERS

BUSINESS MEETING—11:30 A.M.

Chairman, H. C. BOARDMAN
President, American Welding Society

Annual Report of the President

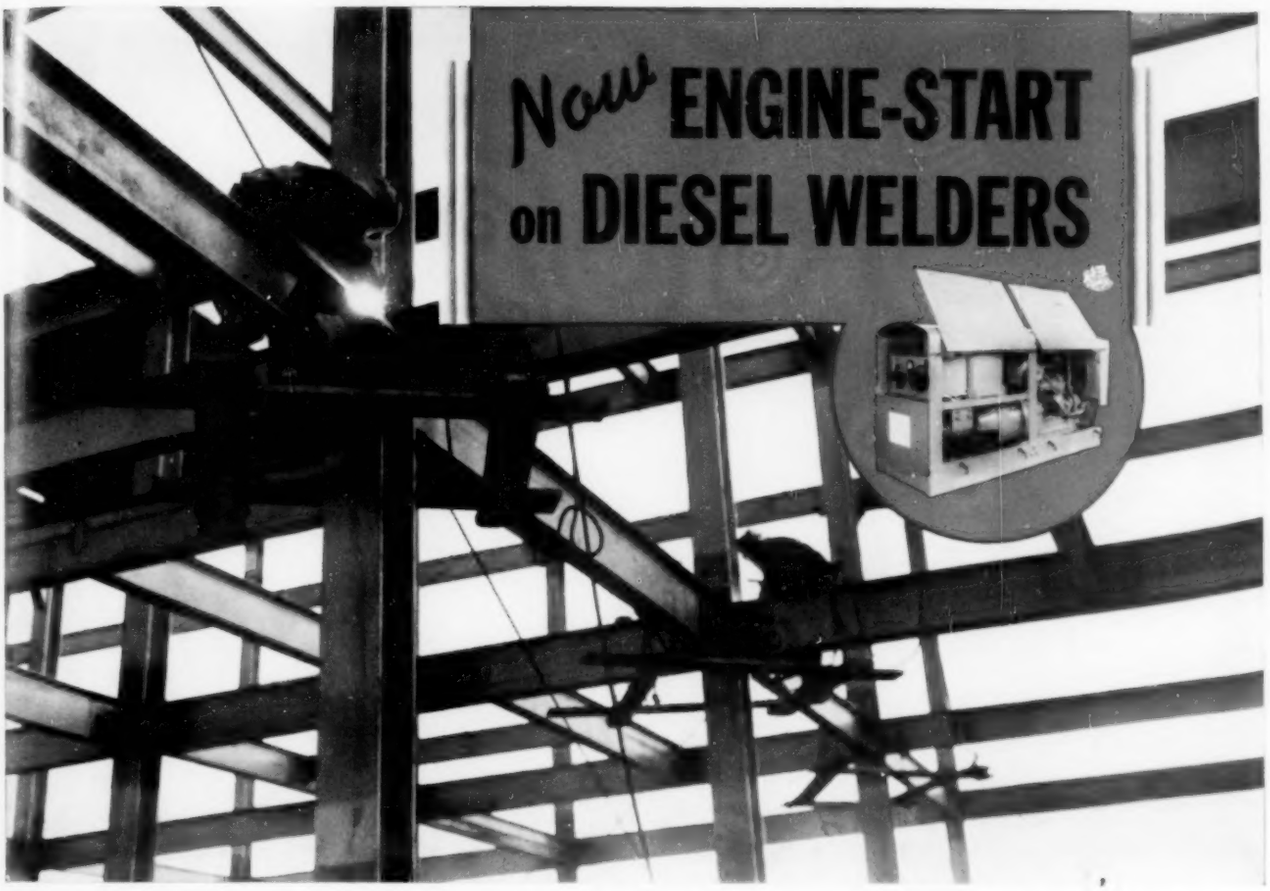
Vice-Chairman, G. F. JENKS
1st Vice-President, American Welding Society

Election and Installation of New Officers

Presentation of Past-President's Pin

Board of Directors Luncheon Meeting

Afternoon—**Inspection Trip,** Electro-Motive Corporation Plant, La Grange, Ill.



"Shield-Arc" welding the framework of new 14-story state building in Kentucky.

The engine-start diesel driven *"Shield-Arc"* welder now gives you—

1. EASY STARTING IN ANY WEATHER:—Faster starts in cold weather than with conventional engine driven welders!

2. SIMPLE STARTING EQUIPMENT:—Compact easily hand-cranked auxiliary engine mounted above the diesel. No worry about batteries.

3. LIGHT, COMPACT POWER:—A diesel comparable in weight and size to gasoline power.

4. INHERENT OPERATING ECONOMY:—Savings on fuel up to 40 cents per hour.

5. LOW FIRST COST:—Only slightly higher than gasoline engines of equal power and operating speed.

—These decisive advances in diesel design make the *"Shield-Arc"* the biggest money-saver in the welding field today.

Find out about the diesel driven *"Shield-Arc"*. Consult your nearest Lincoln office or mail the coupon.

Largest Manufacturers of Arc Welding Equipment in the World

THE LINCOLN ELECTRIC CO.

CLEVELAND, OHIO

THE LINCOLN ELECTRIC COMPANY, Dept. DD-650, Cleveland, O.
Send full particulars about the Diesel *"Shield-Arc"* (Bulletin 327-A.)

Name _____ Position _____

Company _____

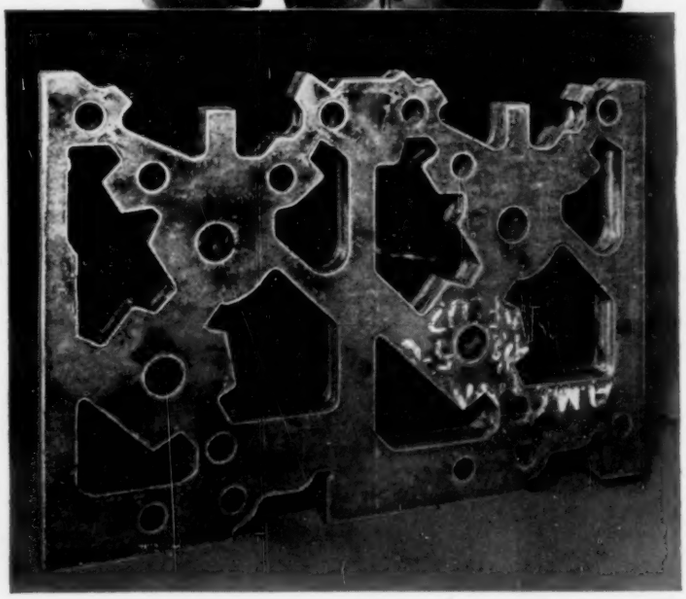
Address _____

City _____ State _____

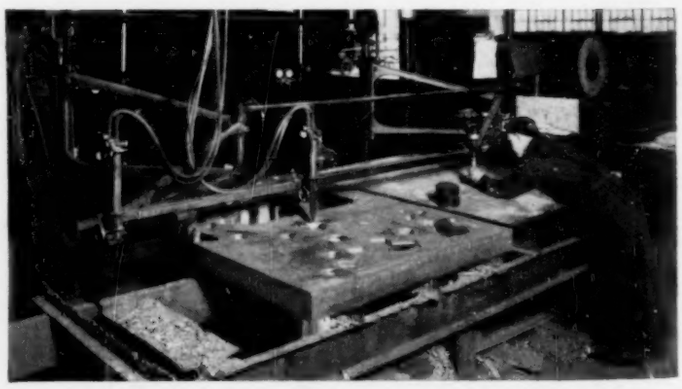
RESULTS WERE LIKE *fingerprints* IN THIS CASE!

The finished cuts on this difficult job were extremely clean and accurate — a typical Airco machine gas cutting job. Specifications were rigid and demanded that the inside cuts in this 4½-inch thick steel printing press frame be to close tolerances. The big difficulty was in piercing the starting holes while still maintaining the required accuracy. The A. M. Castle Company of Chicago was able to maintain the specified tolerances with an Airco Oxygraph Cutting Machine.

Cutting to close tolerances, multiple cutting, flame-hardening or flame-cleaning can be done quickly and economically with Airco High-Purity Oxygen, Airco Acetylene, apparatus and engineering assistance. A new bulletin illustrating some of the many designs which are being successfully flame cut with Airco machines is on the press. Advance copies will be ready for distribution soon. Write for your copy now.



Printing Press Side Frame Having 26 "Inside Cuts."



Oxygraph Cutting Printing Press Frame from 4½-inch steel slab.

AIR REDUCTION

SALES COMPANY

General Offices: 60 EAST 42nd ST., NEW YORK, N.Y.

DISTRICT OFFICES IN PRINCIPAL CITIES

AIRCO *Anything and Everything for* GAS WELDING or CUTTING and ARC WELDING WILSON